TRANSACTIONS

of the

American Foundrymen's Association

Proceedings of the

Fifty-First Annual Meeting

Detroit, Mich.

April 28 to May 1, 1947

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of the

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(INCORPORATED)

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Summary of Proceedings of the 51st Annual Meeting

THE 51ST ANNUAL CONVENTION of the American Foundrymen's Association was held in Detroit, Michigan, April 28 to May 1, 1947 without exhibits. It set a new record for attendance at a non-exhibit convention, approximately 5,000 attending.

There were 60 technical papers and committee reports, 8 gray iron and sand shop course sessions, and 5 round table discussion sessions. In all, there were 44 sessions of technical interest. Meetings were held at the Horace H. Rackham Educational Memorial, Hotel Statler and the Book-Cadillac Hotel. The first Charles Edgar Hoyt Annual Lecture, formerly known as the A.F.A. Foundation Lecture was delivered by Dr. James T. MacKenzie, Director, American Cast Iron Pipe Co., Birmingham, Ala., and A.F.A. Gold Medalist of 1937. His subject was *The Cupola Furnace.*

Formal opening of the convention took place on schedule, April 28, in Detroit's Rackham Memorial building with A. H. Allen, Penton Publishing Co., welcoming the visiting foundrymen in behalf of the Detroit chapter of A.F.A. Mayor Edward J. Jeffries, Jr., extended the city's official welcome, assuring the foundrymen they were assembled in an industrial area where a revolution in mechanized operation is in progress and where the problems of the foundry industry are understood.

A summary of the various sessions follows:

ALUMINUM AND MAGNESIUM SESSION

Monday, April 28, 10:00 A.M.

Presiding—W. E. Sicha, Aluminum Co. of America, Cleveland.

Co-Chairman—J. C. DeHaven, Battelle Memorial Institute, Columbus.

Influence of Inclusions on Properties of Sand Cast Aluminum Base Alloys—A. W. Dana, L. J. Ebert, and G. Sachs, Case Institute of Technology, Cleveland.

Some Causes of Pinhole Blows in Magnesium Alloy Castings—H. H. Fairfield and A. E. Murton, Bureau of Mines, Ottawa, Canada.

MALLEABLE SESSION

Monday, April 28, 10:00 A.M.

Presiding—C. F. Joseph, Central Foundry Div., General Motors Corp., Saginaw, Mich.

Co-Chairman—W. D. McMillan, International Harvester Co., McCormick Works, Chicago.

Malleable Iron Finishing—E. M. Strick, Erie Malleable Iron Co., Erie, Pa.

Malleable Foundry Finishing and Inspection—T. E. Poulson, Belle City Malleable Iron Co., Racine, Wis.

GENERAL MEETING

Monday, April 28, 2:00 P.M.

Presiding—S. V. Wood, President, American Foundrymen's Association.

Greetings of Host Chapter—A. H. Allen, Chairman, Detroit Chapter A.F.A.

Official Welcome—Hon. Edward J. Jeffries, Jr., Mayor of the City of Detroit.

Speaker—George T. Christopher, President, Packard Motor Car Co., Detroit.

Subject—*Why the Importance of the Foundry Industry.*

ALUMINUM AND MAGNESIUM SESSION

Monday, April 28, 4:00 P.M.

Presiding—R. E. Ward, Eclipse-Pioneer Div., Bendix Aviation Corp., Teterboro, N.J.

Co-Chairman—W. Mader, Oberdorfer Foundries, Syracuse, N.Y.

Effect of Room Temperature Intervals Between Quenching and Aging of Aluminum Sand Casting Alloys—R. A. Quadt, Federated Metals Div., American Smelting & Refining Co., Perth Amboy, N.J.

Methods of Impregnation to Leakproof Aluminum and Magnesium Alloy Castings—E. V. Blackmun, Aluminum Co. of America, Cleveland.

EDUCATIONAL SESSION

Monday, April 28, 4:00 P.M.

Presiding—F. C. Cech, Cleveland Trade School, Cleveland.

Co-Chairman—B. D. Claffey, General Malleable Corp., Waukesha, Wis.

Management Looks at Training—R. S. Falk, Falk Corp., Milwaukee.

Foundry Training Course for College Graduates—A. W. Gregg, Whiting Corp., Harvey, Ill.

Foreman Training—S. G. Garry, Caterpillar Tractor Co., Peoria.

MALLEABLE SESSION

Monday, April 28, 4:00 P.M.

Presiding—R. J. Anderson, Belle City Malleable Iron Co., Racine, Wis.

Co-Chairman—J. A. Durr, Albion Malleable Iron Co., Albion, Mich.

Material Handling in a Malleable Foundry Processing Department—N. J. Henke, Saginaw Malleable Div., General Motors Corp., Saginaw.

Mechanized Malleable Foundry Finishing and Inspection—D. F. Sawtelle, Malleable Iron Fittings Co., Branford, Conn.

GRAY IRON SHOP COURSE (SESSION 1)

Monday, April 28, 4:00 P.M.

Presiding—E. J. Burke, Hanna Furnace Corp., Buffalo.

Co-Chairman—J. E. Coon, Packard Motor Car Co., Detroit.

Subject—*Variables Affecting Carbon Control in Cupola Operation.*

Discussion Leader—W. W. Levi, Lynchburg Foundry & Machine Co., Radford, Va.

EDUCATIONAL SESSION AND DINNER

Monday, April 28, 7:00 P.M.

Presiding—A. W. Gregg, Whiting Corp., Harvey, Ill.

Co-Chairman—F. G. Sefing, International Nickel Co., New York.

Speaker—Dr. R. L. Lee, General Motors Corp., Detroit.

Subject—*Management's Stake in Personnel Training.*

SAND SHOP COURSE (SESSION 1—MALLEABLE)

Monday, April 28, 8:00 P.M.

Presiding—D. F. Sawtelle, Malleable Iron Fittings Co., Branford, Conn.

Co-Chairman—E. C. Zirzow, National Malleable & Steel Castings Co., Cleveland.

Subject—*Malleable Sand Problems.*

Discussion Leader—D. Tamor, American Chain & Cable Co., Inc., York, Pa.

ALUMINUM AND MAGNESIUM SESSION

Tuesday, April 29, 10:00 A.M.

Presiding—R. T. Wood, American Magnesium Corp., Cleveland.

Co-Chairman—Hiram Brown, Solar Aircraft Corp., Des Moines.

Simplification of Light Metal Castings Design and its Effect Upon Serviceability—W. T. Bean, Jr., Continental Aviation & Engineering Corp., Detroit.

A New Gating Technique for Magnesium Alloy Castings—H. E. Elliot and J. G. Mezoff, Dow Chemical Co., Bay City, Mich.

BRASS AND BRONZE SESSION

Tuesday, April 29, 10:00 A.M.

Presiding—H. M. St. John, Crane Co., Chicago.

Co-Chairman—A. K. Higgins, Allis-Chalmers Mfg. Co., Milwaukee.

Radiography of Gun Metal Castings—W. H. Baer, Non-Ferrous Section, Naval Research Lab., Washington, D.C.

Segregation in Manganese Bronze—G. E. Dalbey, Mare Island Naval Shipyard, Vallejo, Calif.

Spectrographic Analysis in the Manufacture of Brass and Bronze Ingots—G. E. Staahl and G. P. Halliwell, H. Kramer & Co., Chicago.

MALLEABLE SESSION

Tuesday, April 29, 10:00 A.M.

Presiding—V. A. Crosby, Climax Molybdenum Co., Detroit.

Co-Chairman—Milton Tilley, National Malleable & Steel Castings Co., Cleveland.

An Interpretation of the Constitution of Iron-Carbon-Silicon Alloys—J. E. Rehder, Bureau of Mines, Ottawa, Canada.

Effect of Section Size and Annealing Temperature on the Graphitization of White Cast Iron—D. J. Reese, International Nickel Co., New York, R. Schneidewind, and A. Tang, University of Michigan, Ann Arbor, Mich.

HEAT TRANSFER SESSION

Tuesday, April 29, 10:00 A.M.

Presiding—H. A. Schwartz, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman—E. C. Troy, Dodge Steel Co., Philadelphia.

Heat Transfer Committee Report.

Studies on Solidification of Castings—V. Paschkis, Columbia University, New York.

Solidification Rates of Aluminum in Dry Sand Molds—H. Y. Hunsicker, Aluminum Co. of America, Cleveland.

Calculating the Size of Gates and Risers—N. Janco, Centrifugal Casting Machine Co., Tulsa.

ALUMINUM AND MAGNESIUM ROUND TABLE LUNCHEON

Tuesday, April 29, 12:00 P.M.

Presiding—A. T. Ruppe, Bendix Products Div., Bendix Aviation Corp., South Bend.

Co-Chairman—C. E. Nelson, Dow Chemical Co., Midland, Mich.

Subject—*Recommended Practices for Aluminum and Magnesium Casting.*

Discussion Leader—W. Bonsack, National Smelting Co., Cleveland.

MALLEABLE ROUND TABLE LUNCHEON

Tuesday, April 29, 12:00 P.M.

Presiding—A. M. Fulton, Northern Malleable Iron Co., St. Paul.

Co-Chairman—J. H. Lansing, Malleable Founders' Society, Cleveland.

Subject—*Chromium Determinations, Grinding of Malleable Gates, Exothermic Ladle Additions.*

Discussion Leader—C. F. Lauenstein, Link-Belt Co., Indianapolis.

BRASS AND BRONZE ROUND TABLE LUNCHEON

Tuesday, April 29, 12:00 P.M.

Presiding—B. A. Miller, Baldwin Locomotive Works, Philadelphia.

Co-Chairman—R. J. Keeley, Ajax Metal Co., Philadelphia.

Subject—*Melting of Brass and Bronze.*

Discussion Leader—H. Smith, Federated Metals Div., American Smelting & Refining Co., Pittsburgh.

STEEL SESSION

Tuesday, April 29, 2:00 P.M.

Presiding—C. W. Briggs, Steel Founders' Society of America, Cleveland.

Co-Chairman—L. H. Hahn, Sivyer Steel Castings Co., Chicago.

Institute of British Foundrymen Exchange Paper.

Side-Blown Converter Practice—F. Cousans, Hadfields, Ltd., Sheffield, England.

Effect of Melting Practice on Properties of Medium-Carbon Low Alloy Cast Steels—J. G. Kura and N. H. Keyser, Battelle Memorial Institute, Columbus.

Influence of Selenium on the Sulphide Form and Ductility of Cast Steel—A. P. Gagnebin, International Nickel Co., Bayonne, N.J.

SAND SESSION

Tuesday, April 29, 2:00 P.M.

Presiding—E. C. Zirzow, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman—H. J. Williams, New Jersey Silica Sand Co., Millville, N.J.

Elevated Temperature Properties of Steel Foundry Sands—*D. C. Williams, Ohio State University, Columbus, Ohio.....* ..

Preparation of Foundry Sands for Market—F. P. Goettman, G. F. Pettinos, Inc., Philadelphia.

Evaluation of Core Knockout—Committee Report—H. W. Dietert Co., Detroit.

GRAY IRON SESSION

Tuesday, April 29, 2:00 P.M.

Presiding—R. G. McElwee, Vanadium Corp. of America, Detroit.

Co-Chairman—W. W. Levi, Lynchburg Foundry & Machine Co., Radford, Va.

Cupola Melting Phenomena—D. W. Gunther and E. V. Somers, Westinghouse Electric Corp., Trafford, Pa.

Arc Welding of Cast Iron with Nickel Electrodes—T. E. Kihlgren and L. C. Minard, International Nickel Co., Bayonne, N.J.

REFRACTORIES SESSION

Tuesday, April 29, 4:00 P.M.

Presiding—R. H. Stone, Vesuvius Crucible Co., Pittsburgh.

Co-Chairman—A. S. Klopf, Western Foundry Co., Chicago.

Special Refractories in Metal Melting—W. H. Henson, Norton Co., Worcester, Mass.

Information Please—Question and Answer Panel.

BRASS AND BRONZE SESSION

Tuesday, April 29, 4:00 P.M.

Presiding—G. P. Halliwell, H. Kramer & Co., Chicago.

Co-Chairman—B. M. Loring, Naval Research Laboratory, Washington, D.C.

Correlation of Structure and Properties 85-5-5-5 Alloy Test Bars—J. G. Kura and L. W. Eastwood, Battelle Memorial Institute, Columbus.

Gas Absorption Phenomena and Degasification of Cast Monel—B. N. Ames and N. A. Kahn, New York Naval Shipyard, New York.

PATTERNMAKING SESSION

Tuesday, April 29, 4:00 P.M.

Presiding—V. J. Sedlon, Master Pattern Co., Cleveland.

Co-Chairman—A. F. Pfeiffer, Allis-Chalmers Mfg. Co., Milwaukee.

Pattern Purchase Considerations—W. G. Schuller, Caterpillar Tractor Co., Peoria, Ill.

Liquid Phenolic Casting Resins for Foundry Patterns—C. R. Simmons, Durez Plastics and Chemicals, Inc., North Tonawanda, N.Y.

CHAPTER OFFICERS' AND DIRECTORS' DINNER

Tuesday, April 29, 7:00 P.M.

Presiding—Max Kuniansky, Vice-President, American Foundrymen's Association.

SAND SHOP COURSE (SESSION 2—NON-FERROUS)

Tuesday, April 29, 8:00 P.M.

Presiding—E. J. Bush, Navy Yard, Washington, D.C.

Co-Chairman—E. W. Horlebein, Gibson & Kirk Co., Baltimore.

Subject—*Your Sand Pile.*

Discussion Leader—W. M. Ball, Jr., Magnus Brass Div., National Lead Co., Cincinnati.

GRAY IRON SHOP COURSE (SESSION 2)

Tuesday, April 29, 8:00 P.M.

Presiding—W. W. Levi, Lynchburg Foundry & Machine Co., Radford, Va.

Co-Chairman—C. J. Rittenger, American Car & Foundry Co., Detroit.

Subject—*Effect of Coke Quality on Cupola Melting.*

Discussion Leader—D. E. Krause, Battelle Memorial Institue, Columbus.

INSPECTION OF CASTINGS SESSION

Tuesday, April 29, 8:00 P.M.

Presiding—H. R. Youngkrantz, Apex Smelting Co., Chicago.

Co-Chairman—H. C. Stone, Belle City Malleable Iron Co., Racine.

Importance of Radiography to Inspection—E. L. LaGrelius, American Steel Foundries, East Chicago, Ind.

Magnetic Particle Inspection in the Foundry—W. E. Thomas, Magnaflux Corp., Chicago.

HEAT TRANSFER SESSION

Tuesday, April 29, 8:00 P.M.

Presiding—H. F. Taylor, Massachusetts Institute of Technology, Cambridge, Mass.

Co-Chairman—W. J. Klayer, Aluminum Industries Inc., Cincinnati.

Freezing Rate of White Cast Iron in Dry Sand Molds—H. A. Schwartz, National Malleable & Steel Castings Co., Cleveland.

Thermal Conductivity of Three Sands—C. F. Lucks, O. L. Linebrink, and K. L. Johnson, Battelle Memorial Institute, Columbus.

Influence of Properties on Solidification of Metals—V. Paschkis, Columbia University, New York.

Feeding of Metal Castings—A. F. Faber, Jr., H. B. Salter Mfg. Co., Marysville, Ohio, and D. T. Doll, Case Institute of Technology, Cleveland.

GRAY IRON SESSION

Wednesday, April 30, 10:00 A.M.

Presiding—B. P. Mulcahy, Consultant, Indianapolis.

Co-Chairman—H. W. Stuart, U. S. Pipe & Foundry Co., Burlington, N.J.

Survey of Foundry Coke Characteristics—D. E. Krause and H. W. Lownie, Jr., Battelle Memorial Institute, Columbus.

Thermochemical Analysis of Combustion in a Cupola—H. E. Flanders, University of Utah, Salt Lake City.

STEEL SESSION

Wednesday, April 30, 10:00 A.M.

Presiding—C. H. Lorig, Battelle Memorial Institute, Columbus.

Co-Chairman—E. C. Troy, Dodge Steel Co., Philadelphia.

Slag Control in the Acid Electric Furnace—H. H. Johnson, M. T. McDonough, and D. L. Radford, National Malleable & Steel Castings Co., Sharon, Pa.

Application of a Single Slag Process to Basic Electric Steel—M. V. Healey and R. W. Thomas, General Electric Co., Schenectady.

SAND SESSION

Wednesday, April 30, 10:00 A.M.

Presiding—G. R. Gardner, Aluminum Co. of America, Cleveland.

Co-Chairman—K. J. Jacobson, Griffin Wheel Co., Chicago.

New Tentative Standards for Grading and Fineness of Sands—Committee Report—R. E. Morey, Naval Research Lab., Washington, D.C.

The Foundry Sand Laboratory—O. J. Myers, Werner G. Smith Co., Minneapolis.

A Study of the Precision of Sand Test Data—R. E. Morey and C. G. Ackerlind, Naval Research Lab., Washington, D.C.

CANADIAN LUNCHEON

Wednesday, April 30, 12:00 P.M.

Presiding—J. Sully, Sully Foundry Div., Neptune Meters Ltd., Toronto.

ENGINEERING SCHOOL GRADUATES LUNCHEON

Wednesday, April 30, 12:00 P.M.

Presiding—F. J. Walls, International Nickel Co., Detroit.

Co-Chairman—G. K. Dreher, Olds Alloys Co., Southgate, Cal.

ANNUAL BUSINESS MEETING AND CHARLES EDGAR HOYT ANNUAL LECTURE

Presiding—S. V. Wood, President, American Foundrymen's Association.

President Wood called the meeting to order at the Annual Business Meeting of the American Foundrymen's Association. He then read a cablegram from the Institute of British Foundrymen signed by D. H. Wood, *President*, and T. Makemson, *Secretary*, which read as follows:

"The Institute of British Foundrymen send cordial greetings to the President and the members of the American Foundrymen's Association and the best wishes for success in their 51st Annual Convention."

President Wood then read the President's Annual Address (see p. xiii). Following this address he called on Secretary W. W. Maloney who announced the 1947 Apprentice Contest winners as follows:

Gray Iron Molding Division

1st—Floyd G. Matthews, Caterpillar Tractor Co., Peoria, Ill.

2nd—Robert J. Miller, Hill Acme Co., Cleveland, Ohio.

3rd—Leo Prushinski, Sheffield Foundry Co., Chicago, Ill.

Steel Molding Division

1st—Harold Gobeille, Crucible Steel Casting Co., Cleveland, Ohio.

2nd—John Colombo, Crucible Steel Casting Co., Cleveland, Ohio.

3rd—Walter G. Bach, Indiana Harbor Works, American Steel Foundries, East Chicago, Indiana.

Non-Ferrous Molding Division

1st—John Hronek, Wisconsin Aluminum Foundry Co., Inc., Manitowoc, Wis.

2nd—David Gregory, Empire Brass Foundry, Ltd., Montreal, Quebec, Canada.

3rd—Wilson Bruce, Buckeye Brass Co., Cleveland, Ohio.

Patternmaking Division

1st—William Waddicor, Jr., Brown & Sharpe Mfg. Co., Providence, R.I.

2nd—Howard J. Rand, Aluminum Co. of America, Cleveland, Ohio.

3rd—Laurent Messier, Acme Pattern & Woodwork Co., Ltd., Montreal, Quebec, Canada.

The Association arranged to have the four first prize winners present at the convention and to receive their awards in person. The first prize winners were called to the platform and President Wood presented the awards to each of them individually. The first prize award consisted of $100 and an engraved Certificate of Award.

Following this presentation President Wood introduced Fred G. Sefing, Chairman of the Educational Division of A.F.A., who spoke briefly about the Foundry Educational Foundation. He then called on Mr. B. J. Claffey, one of the Trustees of the Foundation, who explained the aims and purposes of the Foundation.

Following this discussion Secretary Maloney reported on nominations of Officers and Directors for the coming year and stated that no additional nominees had been received in accordance with the procedure prescribed in the Association By-Laws. He therefore cast the unanimous ballot of the membership of A.F.A. for election of the following:

President (to serve one year):

Max Kuniansky, Lynchburg Foundry Co., Lynchburg, Va.

Vice President (to serve one year):

W. B. Wallis, Pittsburgh Lectromelt Furnace Corp., Pittsburgh, Pa.

Directors (to serve three years):

E. N. Delahunt, Warden King, Ltd., Montreal, Quebec, Canada.

W. J. MacNeill, G. H. R. Foundry Div., Dayton Malleable Iron Co., Dayton, Ohio.

John M. Robb, Jr., Hickman, Williams & Co., Philadelphia, Pa.

A. C. Ziebell, Universal Foundry Co., Oshkosh, Wis.

R. H. McCarroll Ford Motor Co., Dearborn, Mich.

Director (to serve one year):

S. V. Wood, Minneapolis Electric Steel Castings Co., Minneapolis, Minn.

Following announcement of elections, President Wood introduced the newly elected Officers and Directors present.

Following this brief introduction, President Wood introduced Dr. James T. MacKenzie, Director, American Cast Iron Pipe Co.,

Birmingham, Ala., who presented the first Charles Edgar Hoyt Lecture on the subject "The Cupola Furnace." (Dr. MacKenzie's lecture will be found beginning on page 1 of this volume.)

The meeting was adjourned upon completion of this lecture.

A.F.A. ALUMNI DINNER

Wednesday, April 30, 7:00 P.M.

Presiding—F. J. Walls, International Nickel Co., Detroit.

Speaker—John H. Van Deventer, Director of Information, Committee on Economic Development, New York.

Subject—*Broadening Our Economic Horizons.*

SAND SHOP COURSE (SESSION 3—STEEL)

Wednesday, April 30, 8:00 P.M.

Presiding—R. H. Jacoby, Key Co., St. Louis.

Co-Chairman—S. W. Brinson, Norfolk Navy Yard, Portsmouth, Va.

Subject—*The Role of Sand in Hot Tearing.*

Discussion Leader—J. B. Caine, Sawbrook Steel Castings Co., Cincinnati.

GRAY IRON SHOP COURSE (SESSION 3)

Wednesday, April 30, 8:00 P.M.

Presiding—J. H. Bernard, Eaton Mfg. Co., Vassar, Mich.

Co-Chairman—K. E. Davis, Cadillac Motor Car Co., Detroit.

Subject—*Factors Affecting Cost of Cupola Operation.*

Discussion Leader—L. L. Clark, Buick Motor Car Co., Flint, Mich.

PLANT AND PLANT EQUIPMENT SESSION

Wednesday, April 30, 8:00 P.M.

Presiding—J. Thomson, Continental Foundry and Machine Co., East Chicago, Ind.

Co-Chairman—E. W. Beach, Campbell Wyant and Cannon Co., Muskegon, Mich.

Design and Operating Phases of Mechanized Foundries—C. O. Bartlett, C. O. Bartlett and Snow Co., Cleveland.

GRAY IRON SESSION

Thursday, May 1, 10:00 A.M.

Presiding—V. A. Crosby, Climax Molybdenum Co., Detroit.

Co-Chairman—H. N. Myers, Perfect Circle Co., Hagerstown, Ind.

The Graphite Phase in Gray Cast Iron—R. W. Lindsay, Pennsylvania State College, State College, Pa.

Microstructure of Silvery Pig Iron—R. Schneidewind, University of Michigan, Ann Arbor, Mich., and C. A. Harmon, Hanna Furnace Corp., Buffalo.

Micro-Radiography of Gray Cast Iron—J. H. Schaum, E. T. Salkovitz, and F. W. Von Batchelder, Naval Research Lab., Washington, D.C.

STEEL SESSION

Thursday, May 1, 10:00 A.M.

Presiding—J. A. Rassenfoss, American Steel Foundries, Indiana Harbor, Ind.

Co-Chairman—G. Vennerholm, Ford Motor Co., Dearborn, Mich.

Occurrence of Intergranular Fracture in Cast Steels—C. H. Lorig and A. R. Elsea, Battelle Memorial Institute, Columbus.

Segregation in Small Steel Castings—H. F. Bishop and K. E. Fritz, Naval Research Lab., Washington, D.C.

JOB EVALUATION AND TIME STUDY SESSION

Thursday, May 1, 10:00 A.M.

Presiding—R. J. Fisher, The Falk Corp., Milwaukee.

Co-Chairman—J. A. Westover, Westover Engineers, Milwaukee.

Establishment and Use of Standard Data (I)—M. Annich, American Brake Shoe Co., Mahwah, N.J.

STEEL ROUND TABLE LUNCHEON

Thursday, May 1, 12:00 P.M.

Presiding—F. A. Melmoth, Glen Lake, Cedar, Mich.

Co-Chairman—John Howe Hall, Swarthmore, Pa.

PATTERN ROUND TABLE LUNCHEON

Thursday, May 1, 12:00 P.M.

Presiding—H. K. Swanson, Swanson Pattern & Model Works, East Chicago, Ind.

Co-Chairman—L. F. Tucker, City Pattern & Foundry Co., Inc., South Bend, Ind.

Subject—*Need for Close Relationship Among Patternmakers and Foundrymen.*

Discussion Leader—A. F. Pfeiffer, Allis-Chalmers Mfg. Co., Milwaukee.

FOUNDRY COST SESSION

Thursday, May 1, 2:00 P.M.

Presiding—R. L. Lee, Grede Foundries Inc., Milwaukee.

Co-Chairman—G. Tisdale, Zenith Foundry Co., Milwaukee.

Two Ways to Make Profit—W. E. George, Booz, Allen and Hamilton, Chicago.

Foundry Costs and Cost Controls—C. E. Westover, Westover Engineers, Milwaukee.

GRAY IRON SESSION

Thursday, May 1, 2:00 P.M.

Presiding—H. Bornstein, Deere & Co., Moline.

Co-Chairman—J. E. Bowen, Chevrolet Gray Iron Foundry, General Motors Corp., Saginaw, Mich.

Effects on Gray Iron of Minor Constituents Derived from the Additions of Copper Alloys—K. E. Rose and C. H. Lorig, Battelle Memorial Institute, Columbus.

Isothermal Transformation of Molybdenum Cast Iron—C. A. Nagler, Wayne University, Detroit, and R. L. Dowdell, University of Minnesota, Minneapolis.

Reduction in Chilling Tendency Through Silicon Carbide Inoculation of Gray Cast Iron—E. A. Loria, A. P. Thompson, Mellon Institute, Pittsburgh, and H. D. Shephard, Kerchner, Marshal & Co., Pittsburgh.

GRAY IRON SHOP COURSE (SESSION 4)

Thursday, May 1, 2:00 P.M.

Presiding—H. H. Wilder, Eaton Mfg. Co., Vassar, Mich.

Co-Chairman—G. A. Timmons, Climax Molybdenum Co. Detroit.

Subject—*Variables Affecting Electric Furnace Gray Iron.*

Discussion Leader—K. H. Priestley, Vassar Electroloy Products Inc., Vassar, Mich.

SAND SESSION

Thursday, May 1, 2:00 P.M.

Presiding—H. F. Taylor, Massachusetts Institute of Technology, Cambridge, Mass.

Co-Chairman—B. H. Booth, Carpenter Bros., Inc., Milwaukee.

Density of Molding Sands—H. W. Dietert, H. H. Fairfield and E. J. Hasty, H. W. Dietert Co., Detroit.

Physical Properties of Molding Sands—G. R. Gardner, Aluminum Co. of America, Cleveland.

STEEL SESSION

Thursday, May 1, 4:00 P.M.

Presiding—J. B. Caine, Sawbrook Steel Castings Co., Cincinnati.

Co-Chairman—C. K. Donoho, American Cast Iron Pipe Co., Birmingham.

Determination of Molten Metal Temperatures—G. Vennerholm and L. C. Tate, Ford Motor Co., Dearborn, Mich.

Temperature Distribution in Metal Molds—M. C. Udy and H. O. McIntire, Battelle Memorial Institute, Columbus.

SAND SHOP COURSE (SESSION 4—GRAY IRON)

Thursday, May 1, 4:00 P.M.

Presiding—E. L. Thomas, Cadillac Motor Car Co., Detroit.

Co-Chairman—F. R. Mason, Riley Stoker Co., Detroit.

Subject—*Variables in Gray Iron Sand Practice.*

Discussion Leader—C. B. Schofield, Chevrolet Gray Iron Foundry, General Motors Corp., Saginaw.

JOB EVALUATION AND TIME STUDY SESSION

Thursday, May 1, 4:00 P.M.

Presiding—R. J. Fisher, Falk Corp., Milwaukee.

Co-Chairman—J. A. Westover, Westover Engineers, Milwaukee.

Establishment and Use of Standard Data (II)—M. Annich, American Brake Shoe Co., Mahwah, N.J.

ANNUAL BANQUET

Thursday, May 1, 7:00 P.M.

Presiding—S. V. Wood, President, American Foundrymen's Association.

The Annual A.F.A. Dinner of the Association's 51st Annual Convention was called to order by President S. V. Wood, presiding. Following singing of the National Anthem by Miss Marion Gillette, President Wood introduced Past President F. J. Walls, Chairman of the Board of Awards. After brief introductory remarks Past President Walls introduced Dr. G. H. Clamer, Past President of A.F.A. and 1933 Joseph S. Seaman Medalist, who presented the Wm. E. McFadden Gold Medal to H. M. St. John, Supt. of Forge Shop and Brass Foundry, Crane Co., Chicago.

PRESENTATION OF MCFADDEN MEDAL

Dr. Clamer stated:

"I have been honored with the invitation to act as spokesman for our Association on this occasion because of my long friendship with Harry St. John. We have had mutual interest in the same technical fields; namely, electric furnaces and the brass foundry. I have always admired and valued his sound judgment—judgment based on a technical education of high order and tempered with broad experience.

"Harry Mark St. John was born November 29, 1888 in Canajoharie, N.Y. He graduated from Cornell University in 1910 with an A.B. degree and immediately became connected with National Carbon Co., Cleveland, as a Research Chemist. Three years later he moved to Chicago to become a Research Engineer for Commonwealth Edison Co., where he remained until the outbreak of World War I, during which he served with distinction as a captain in the Chemical Warfare Service. On leaving the Army he accepted a position as Service Engineer for the Detroit Electric Furnace Co., Detroit, and during his four years with this company became Vice-President. In 1923 he became connected with Detroit Lubricator Co., Detroit, as Chief Metallurgist, where he remained until 1938, at which time he accepted a position with the Crane Co., Chicago, as Superintendent of the Brass Foundry and Forge Shop, which position he still holds.

"Many papers by Mr. St. John have been published, not only in the TRANSACTIONS of the American Foundrymen's Association, but also in the *Proceedings* of the American Electrochemical Society, American Institute of Mining & Metallurgical Engineers and the American Institute of Chemical Engineers. He presented an A.F.A. Exchange Paper on Temperature Control to the French Foundrymen's Association. Articles by him have been published in the following trade papers: *Chemical & Metallurgical Engineering*, *Metals & Alloys* (now *Materials & Methods*), *The Foundry* and *Metal Industry*, *Electrical World* and *Electrical Engineering*. His articles cover a broad field; for example, Electric Furnaces for Brass Melting; Temperature Control; Analysis of Casting Defects in the Brass Foundry; Brass Foundry Metal Practice; Influence of Impurities in Brass Castings; Influence of Silicon and Aluminum on Structures of Brass Castings; Refractories used in Brass Melting Furnaces. He contributed the section on Red Brass Alloys for the *ASM Handbook* and the section on Non-Ferrous Foundry Practice for *Kent's Mechanical Engineering Handbook*. He has been granted United States Patents on Cooling Coils for Electrodes and a patent on an Electric Furnace Door.

"Mr. St. John has been one of the most active members of the Brass and Bronze Division of A.F.A. since its inception, serving as Chairman of the Division and participating in many Committee activities. He is today Chairman of the Annual Lecture Committee of A.F.A. He is a past Chairman of both the Chicago and Detroit sections of AIME and a member of ASM as well as A.F.A.

"Acting for the Board of Awards and the Board of the American Foundrymen's Association it is my pleasure to present to Mr. Harry M. St. John the Wm. H. McFadden Gold Medal with citation as follows:

> *"'For outstanding work in the field of non-ferrous casting research over a period of many years.'"*

RESPONSE BY H. M. ST. JOHN

Mr. St. John graciously accepted the Wm. H. McFadden Gold Medal from Dr. Clamer with the following statement:

"I wish to express to the American Foundrymen's Association and its Board of Awards my sincere appreciation of this award."

PRESENTATION OF PENTON MEDAL

Past President Walls then called on President-elect Max Kuniansky, Vice-President and General Manager, Lynchburg Foundry Co., Lynchburg, Va., who presented the John A. Penton Gold Medal to R. J. Allen, Worthington Pump & Machinery Co., Harrison, N.J. after commenting on the outstanding work which Russ Allen has done for the American Foundrymen's Association and on his many valuable contributions to the art of metal founding.

RESPONSE BY R. J. ALLEN

To this Mr. Allen responded as follows:

"I wish to thank the Board of Awards for having considered me worthy to receive the John A. Penton Gold Medal, and, also, to thank Max for the remarks he has made in justification of their action. Further, I wish to thank the numerous individuals—particularly those members of the National Office staff, who have cooperated with me in the various committee activities and such other work of the Association as has resulted in this award. Again, I thank you."

PRESENTATION OF SIMPSON MEDAL

Past President Walls then called on D. J. Reese, International Nickel Co., New York, who presented the Peter L. Simpson Memorial Medal to R. A. Flinn, American Brake Shoe Co., Mahwah, N.J., with the following introductory remarks:

"I am deeply appreciative of the privilege of representing the Board of Awards of the American Foundrymen's Association in the presentation of the Peter L. Simpson Gold Medal to the second recipient of this high honor.

"The first presentation of the Peter L. Simpson Gold Medal was made by his son, Herb Simpson, whose generosity, affection for his dad, and love for the foundry industry, created this award. I am sure that Herb and his son Bruce were quite happy the Board selected Howard F. Taylor as the first recipient of this award, and I know they must be equally happy to have Dick Flinn sharing the Peter L. Simpson honors with Howard Taylor. These two young men have set a high standard for the Board of Awards, in selecting future recipients, and as the years roll on the memory of Peter L. Simpson will become increasingly enriched by association with the names of these young men—young men who will experience a lifetime of achievement.

"Dick's great-great-grandfather, Richard O'Flinn, operated an iron foundry in Drogheda on the River Boyne, between Dublin and Belfast. The name-plate on this foundry is dated 1801.

"Dick's grandfather, Thomas Flinn, served his time as a molder in this foundry and also as a machinist in its adjacent machine shop. After selling his patent rights in a new steam pump for £500, he came to America in 1860 to study 'Modern American Machine Shop and Foundry Methods.' He subsequently was employed by the Henry R. Worthington Hydraulic Works, Brooklyn, New York, now known as the Worthington Pump & Machinery Corp., from which he retired as Works Manager after 37 years of service.

"The College of the City of New York graduated Dick with B.S. and C.E. degrees in 1936. Massachusetts Institute of Technology granted an M.S. degree in 1937. Now, he found himself at odds with the engineering material so familiar to his grandfather and great-great-grandfather for his thesis work at M.I.T. was concerned with the damping characteristics of wrought steels. He corrected this error by spending two years researching in the field of cast iron, and when he received a Doctor of Science degree from M.I.T. in 1941, his thesis work was concerned with Acicular Cast Irons. Since 1937, Dick's work in the field of metals has been primarily concerned with several members of the cast iron family. The Peter L. Simpson award is presented:

> *'For outstanding work during the past year in the field of chilled and white iron castings'*

"I believe everyone present here tonight should know that he received the Henry Marion Howe Medal of the American Society

for Metals in 1944, he is a charter member of the Metropolitan A.F.A. Chapter, and he is a member of the A.I.M.E., the A.S.M., the American Society of Experimental Stress Analysis, and of the honorary Sigma Xi Fraternity.

"During World War II years, Dick was continuously ill-at-ease that he did not get into uniform, but that was only because he was assigned by the U. S. Army to work on the top military secret of the war, known then as the Manhattan Project, known now as the Atomic Bomb.

"At the time he received his doctor's degree from M.I.T., his counselor persuaded him that to be known as Dick Flinn would be more compatible with the feelings of the graduates of the school of hard-knocks in the foundry industry, so together we held a private wake around a pile of heap sand, in a nearby foundry—when we solemnly buried this degree.

"Dick, I feel that Peter L. Simpson would be just as proud as I am, in honoring so grand and able a fellow as you are."

Medalist Flinn expressed his appreciation in the following words:

"Don, it's ten years since I poured arbitration bars with you for the first time at Bayonne. I realize that these ten years are a very short period compared with the records of many of those here tonight who have given a lifetime of service to the foundry industry. I can't therefore just receive this medal quietly and graciously as the reward for long service, I must outline the conditions under which my conscience will allow me to accept.

"This award is first of all a signal recognition of those who have been my teachers and colleagues both at the Institute and in industry. At times like tonight I realize how great my debt is to Professors Victor Homerberg, John Norton, Morris Cohen and John Chipman at M.I.T. and to Ray Schaefer at Mahwah.

"Secondly, this presentation should stand as an encouragement to the younger men of the industry, particularly those who are attempting to apply the newer findings of physical metallurgy and physical chemistry to the foundry. I know that management's appreciation of the fundamental approach often seems slight and the future appears difficult. It is at times like these, we should turn to this Peter L. Simpson award for confidence.

"For it is not particularly important that the Board of Awards chose me as this year's medalist—of course it gives me great personal pleasure—but what matters is that a group of *practical* men, leaders in the industry, has reached out and selected a theoretical man, a physical metallurgist, for honor.

"It is in this spirit then, Don, that I accept the Peter L. Simpson Memorial Gold Medal with deep appreciation and gratitude to you and to the Board of Awards. I promise that in the years to come I will work to be worthy of this honor."

PRESENTATION OF SEAMAN MEDAL

Past President Walls then called on H. S. Simpson, National Engineering Co., Chicago and 1941-42 A.F.A. President who presented the Joseph S. Seaman Gold Medal to Rodney Washburn, son of Henry S. Washburn, Plainville Casting Co., Plainville, Conn., for whom the medal was intended but who was unable to be present for the presentation because of illness.

Mr. Simpson made the presentation with the following statement:

"It is unfortunate that our medalist, Henry S. Washburn, is unable to be present tonight to receive personally the Gold Medal from the American Foundrymen's Association. His son, Rodney, is here to accept it for his father. Rodney is now associated with Mr. Washburn in the operation of the Plainville Casting Company, Plainville, Conn., which his father created and started in 1921, operating it for a quarter of a century as a successful, free enterprise foundry, specializing in gray iron castings.

"In all of the years that I have been active in the American Foundrymen's Association, I think the presentation of an award to Henry Washburn is most significant, not only from a personal standpoint but because of the realization that our medalist is particularly deserving to be honored by the foundry industry and the American Foundrymen's Association, both of which he has unselfishly and capably served for so many years.

"There is something in this world more important than making dollars although, of course, livelihood will always be an essential activity. It is the intelligent, hard-working individual who gets his real pleasure from service to others—that is, his family, the industry of which he is a part and particularly to the A.F.A., and finally, being a good, clean, progressive American citizen. Henry Washburn exemplifies a man for whom we have respect for all of these qualifications.

"Born in Brooklyn, New York, Mr. Washburn received his early education at St. Paul's School, Garden City, New York. Incidentally, being from Brooklyn he might have been a pitcher for the Brooklyn Dodgers; certainly he has always been in there pitching for the foundry industry. His first position was with a bank in New York City; later he was employed by the Lackawanna Railway as well as Turner and Seymour, Torrington, Conn.

"Since 1921 he has been president and treasurer of his own foundry, the Plainville Casting Company, Plainville, Conn. Mr. Washburn has also been associated with other foundries and manufacturing enterprises in the New England States. He served as a director of the A.F.A. from 1935 to 1938 and then in 1939 was elected the 38th president. Mr. Washburn made a fine record as head of this association. He is a member of the National Founders Association and a past director of the Gray Iron Founders Society, receiving from the latter organization, in 1946, the award for distinguished service to that industry. In his presidential address to the American Foundrymen's Association he said: "The challenge of the future calls for the very best that can be brought forward by every firm and every individual interested in the casting of the metals." Mr. Washburn has followed his own advice; he has done his very best for the industry of which he is a part. He firmly believes in free enterprise, in hard work, and in helping others, particularly in the business of making castings.

"Mr. Washburn has always been happy that he has been actively interested in the Gray Iron Foundry business because he thinks the quality and usability of each casting depend on so many personal factors that it gives an opportunity for rendering a service to the customer as compared to the manufacture of a product where many competitors can make the same article to the same specifications and yet the personal element is largely non-existent.

"After going to Connecticut and becoming closely in contact with the jobbing foundry trade, he believed, as he learned the history of the foundry business in that area, that during the depression periods foundry prices were reduced way below cost and that during periods of expansion the prices of castings went up beyond all reason, with the natural result that purchasers of castings were compelled to switch patterns from one foundry to another. It seemed to Mr. Washburn that his foundry could not furnish castings best suited to a customer's requirements unless his foundry could continue making that product over a long period of time, developing the best mixtures for the particular requirements of each casting, working out the pattern equipment for modern methods of production so that the foundry could play an important part in reducing costs in a machine shop if the operations performed on each casting were previously known. It is also Mr. Washburn's belief that the foundry customer, whose casting requirements really meant something to him, would be willing to pay a reasonable price during dull periods to receive this type of cooperation and with the understanding, of course, that the same margin of profit would apply in busy periods as in quiet periods. He also realizes that there is nothing unusual or new in this approach but when he started the foundry business twenty-six years ago these principles were not so generally accepted.

"Mr. Washburn has had a very broad interest in the welfare of foundry industry from all standpoints. His personality, thoughtfulness and kindliness, together with all his other fine characteristics and accomplishments are so outstanding that the Board of Awards of the American Foundrymen's Association, which I represent, were unanimously enthusiastic in their decision that he be given this honor at this time.

"It is, therefore, my pleasure, Rodney—representing your father, Henry S. Washburn—to present to you for him the Joseph S. Seaman Gold Medal for outstanding service to the American Foundrymen's Association and the casting industry as a whole and particularly for his valuable contributions to the Gray Iron Castings Industry."

R. R. Washburn accepted for H. S. Washburn with the following remarks:

"I think, Mr. Simpson, you can appreciate how keenly dis-

appointed my Father is not to be here and how proud he would have been to have accepted personally through you this award from the American Foundrymen's Association. For me to accept it for him is the greatest privilege I have ever had.

"In accepting this for my Father I would like to bring to you a thought he has expressed to me so many times—that is, that while each of us may make our own small individual contribution to A.F.A., each of us in return receives from the Association the combined thoughts, experience, and helpful suggestions of the entire membership. I know my Father sincerely feels that any small contribution he has been able to make could in no way equal benefits received.

"My Father has asked me to extend his deep appreciation for this honor which the Association has conferred upon him and sends his warmest regards to friends in A.F.A."

Then Past President Walls presented Certificates of Honorary Life Membership to John Grennan, University of Michigan, Ann Arbor, Mich., and to retiring President S. V. Wood.

GUEST SPEAKER'S ADDRESS

Following presentation of Awards, the guest speaker of the evening, Arthur H. Motley, President, Parade Publications, Inc., New York, presented his talk on "Tradition—An Asset or a Liability" in which he stressed the importance of constantly seeking new ways to improve services and production, citing instances in which the break away from tradition has brought phenomenal improvements in many fields. He urged the foundry industry to take stock constantly of its own products and operations to the end that the foundry industry may constantly progress and receive its full share of recognition as one of the vital basic occupations.

President's Annual Address

By S. V. WOOD

Minneapolis Electric Steel Castings Co.
Minneapolis

ON THIS, the occasion of the 51st Annual Convention of the A.F.A., and the first year of the second one-half century of its existence, I find myself confronted with the task, by custom long since established and an accepted obligation of your Executive Officer, of presenting the President's Annual Address.

With the knowledge that it was incumbent upon me to continue this custom, which has now become a tradition, I have recently reviewed the addresses of former presidents. I can assure you that it has indeed been pleasurable reading and has brought back to me memories of friends and acquaintances within this organization, memories which I cherish and friends whose advice and counsel I value most highly and for which I am most grateful.

Good Association Officers

This Association has been blessed with fine officers down through the years, and the fact that it has a long and unbroken history of successful service speaks well for the serious and unselfish efforts of these men.

In many of these past addresses I find the words: "This is a critical period." These words were not spoken because of fear for the future, but because these men were serious minded about their task, were anxious about their ability to successfully carry on in the service of their industry.

A review of the volumes of the A.F.A. TRANSACTIONS, impresses one with a realization that casting practices, techniques, and procedures are continually changing. It impresses one also that the basic concept, the general objectives of the American Foundrymen's Association have changed but little. The original by-laws set forth that the objectives of the new Association will be, namely, "To promote the arts and sciences applicable to metal castings manufacture, and to improve the methods of production and quality of castings."

Human endeavor cannot long survive in an association of men unless founded upon sound objectives and a burning desire to accomplish. Who is there that would care to challenge the statement that within these words from the early by-laws are set forth today's concept of our obligation. Within these areas we have operated successfully for fifty years, and within these areas we still can operate and yet broaden our activities.

That this Association is fortified with worthy objectives and sincerity of purpose is reflected in an ever expanding membership roll. As of April 1, 1946, membership totaled 8073, while the report of April 1, 1947, shows a total of 9364, an increase of 1291 members, and increasing recently at a rate of 200 new members per month. It is, therefore, fair to presume that the total should, on July 1 at the close of our fiscal year, be very close to 10,000 members.

This is a marvelous growth, particularly for a post-war period when adjustments of personnel are always particularly heavy. It is especially significant when one realizes that there has not been a campaign for new members; instead, interested men realize that this Association has much to offer, and desire to become a part of a living, moving group who have pride in their craft.

Increasing membership is a tribute to the continuing efforts of both chapter and national officers and directors and the national staff who constantly make available new foundry information, ideas and techniques. It also places upon these same workers the added responsibility of finding work for this increasing membership, for it must be borne in mind that this is a voluntary and cooperative organization.

National and Chapter Service

These opportunities for service are to be found at two levels—the National level and the Chapter level. At the National level we have over 110 committees with several hundred men as working members. At the Chapter level will be found hundreds of committees and thousands of men at work in chapter activities. These committeemen are not appointed by the National office; they are selected men. Committees accept definite and distinct responsibilities; they have signed their names to the committee cards whereon they have pledged their willingness to serve.

Top men of the foundry industry from all over the world—engineers, metallurgists, supervisors, operating men and technicians—are voluntarily giving of their time and effort for the betterment of their industry.

No company is large enough to command and no company has sufficient funds to buy the services of these men, yet you, as a member of the American Foundrymen's Association, have the services of these men in their speaking assignments, in their committee program, in their writings and research.

The A.F.A. is not an accident: it is a live, throbbing organization, established and built by men who have found, over the passing years, pride and profit in an industry that they love. This is pride of craft at work.

A.F.A. Chapters

Now a word about the A.F.A. chapters, the home-town working units, of the Association. Close to 95 per cent of our members are affiliated with chapters. Each chapter is a completely organized working unit with officers, boards of directors, committees appointed and elected by chapter membership. The chapter boards

and committees plan and carry out monthly meetings.

Within these groups is born the sparks of enthusiasm for the industry and are found and developed the men for future leadership of chapters. Herein are they trained and developed for future national officers.

These men are being trained and developed for association work, but they are also being trained for leadership in your plants and companies. Here you will find men of all levels—engineers, technicians, metallurgists, supervisors and salesmen all imbued with the idea of improving themselves and their industry.

At the beginning of this 51st year there were 33 of these chapters, and now, at the time of your Annual Meeting, there are 38 chapters. Five chapters have been organized this year:

Washington chapter, Seattle, No. 34
Rocky Mountain Empire chapter, Denver, No. 35
Tri-State chapter, Tulsa, Okla. No. 36
British Columbia chapter, Vancouver, B.C. No. 37
Central Michigan chapter, Marshall, No. 38

June 30-July 1-2, of this year, at Chicago, the Fourth Annual Chapter Chairman Conference where incoming Chapter Chairmen and Vice-Chairmen will meet in a three-day conference to discuss and study the work of the Association and their chapters.

To this meeting will come the chosen leaders of your chapter, gray iron men, steel men, brass men, malleable men, pattern men, and supply and equipment men, all vitally interested in the production of castings, all to learn of the other fellow's problems. Also to learn of the importance of the casting industry; its history; to dream of its future; and to learn how to improve your industry, plant and products.

How I wish all of you men here today could be at this meeting to see how eagerly these men will approach the activities of this three-day conference. These meetings are serious business to these chapter officials. Starting at an early hour their discussions continue throughout the three-day session.

Even an onlooker would be dull if he did not feel aroused and heartily stimulated by the sincerity of these men and the new ideas that they develop from their discussions and instructions. These chapter chairman conferences are the great meetings of our Association. Here are made the acquaintances, the contacts, the friends—here are found the sources of information —here is developed the know-how of leaders. My only wish is that all of you could sit in and observe this new bubbling enthusiasm; it speaks well for the future of the American Foundrymen's Association.

Now that I have pointed out the highlights of this annual meeting where men from all divisions of the industry are gathered, may I say just a few words about another meeting which has been carried on this year.

This is a group consisting of two top elected executives of each branch or division of the industry, the presidents and vice-presidents of the Malleable Founders' Society, Gray Iron Founders' Society, Non-Ferrous Founders' Society, Steel Founders' Society, Foundry Supply Manufacturers' Association, Foundry Equipment Manufacturers' Association, National Founders' Association, and our own Association.

The elected officers of these long established associations have had several meetings this year, not formal but very intimate meetings where we are each learning definitely about each organization, its hopes, its plans, its objectives, its resources, and its manpower.

We are coming together as a beginning; we are working together for progress; and we are determined to keep together for success.

We are meeting informally under the organization name of the Castings Council, without permanent officers, without definite plans, and without a budget. But, with growing confidence in each other and with an increasing belief that from our efforts will come an over-all casting group which can tie together all groups interested in casting of metals, we are forging ahead.

Tie The Associations Together

We have met thus far without fanfare or publicity, but with open minds and sincerity of purpose. I am not only hopeful, but truly believe that eventually we will come forth with an organization that will tie together all of our individual associations and make it possible to do a better job for the entire industry.

It has been my privilege this year to personally visit a number of the A.F.A. chapters. I have, in company with your Executive Secretary or other staff members, traveled close to 50,000 miles, and have already visited fourteen foundry or chapter centers and expect to visit an additional number before the year closes on July 1. These visits have covered a wide area, from Seattle and Portland in the Northwest to San Francisco and Los Angeles along the Pacific shores, to Birmingham, Alabama, in the Southeast section, to Boston in the Northeast, to Toronto, Canada in the north, to Houston and Lufkin, Texas in the southwest corner of the nation.

At each of these points I have held from one to six meetings with management people, with officers and Boards of Directors, various chapter committees, and general meetings with the membership. In all I have attended 50 or 60 meetings, and in addition thereto have made innumerable calls upon foundrymen in their own plants and offices wherever I have gone.

In addition to these meetings which I have attended, A.F.A. Vice-President Kuniansky has met with additional chapters as have several other directors and some members of the staff.

Everywhere I have gone I have found a warm and enthusiastic welcome from members anxious to do their part in carrying on the work of the Association. It has been a glorious experience for me and I am proud indeed of the spirit of this great Association.

I have been impressed particularly by the feeling of unity among our members. There is a feeling of cordiality between supply and equipment people and the men in the plants, between technicians and operating men and, as for management men, I am gratified and thrilled with the attendance that we have had at the management luncheons. I hope and really believe that management in these centers where I have had the pleasure of visiting have a new conception of what the A.F.A. is doing for their men.

Chapter Interest Increasing

Everywhere I have gone I have had encouraging reports of increasing interest and activity in all chapter areas. Letters that have reached me since our visitations convince me beyond question that we are moving to an improved position among the industries.

I am convinced from my observations this year that the American Foundrymen's Association is soundly organized and functioning well; that it is fulfilling its accepted obligation as outlined in our by-laws; and is, first, "Properly promoting the arts and sciences applicable to metal castings manufacture," and second, "Improving the methods of production and the quality of castings."

We are in a fine position financially. The spirit and enthusiasm of the membership is high. We are fortunate in having a Board of Directors of outstanding quality, sincere of purpose and with a burning zeal to render unselfish service.

The incoming officers and directors are outstanding men of long experience and leadership. Our staff at the National Office is of high quality, earnestly and harmoniously working for our welfare. Our new quarters at the Chicago office are a great improvement. We have many new things to do, plans are being made.

I am more convinced than ever, gentlemen, after almost a lifetime in the foundry industry, that "The FOUNDRY is a GOOD place to work" and, may I add with enthusiasm, it is getting better all the time.

In closing, may I take just a moment to express to my fellow officers and directors, to the committee chairmen and their committeemen, my sincere personal thanks and the gratitude of the membership for their cooperation and earnest help during this year.

And now, gentlemen, may I thank you all for your very great kindness to me and may I thank you, too, for the very great honor which you have given to me, the highest honor that can come to a foundryman, and my earnest hope is that I have not served you in vain and that from my efforts and those of my fellow workers have come benefits which will bear fruit for the betterment of this great basic American industry.

Report of the Secretary for the Fiscal Year 1946-47

Membership

The Association membership continued to show a healthy growth with a net increase of 1144 members during the fiscal year, ended June 30, 1947. The total membership on this date was 9683, as against 8539 on the same date a year ago.

Of the total of 9683 members on June 30, 8809 or 90.9 per cent were affiliated with Chapters, as against 90.8 per cent a year ago; 444 (772 a year ago) held no Chapter affiliation; 430 (392 a year ago) reside in countries outside North America. The chart of membership growth for the past 51 years illustrates the continued upward trend in membership since 1934, when the first A.F.A. Chapter was organized.

Membership Efforts in 1947–48. Our membership work for 1947-48 will be concentrated on the following efforts: (1) To obtain a greater percentage of membership renewals than a year ago, when approximately 7 per cent of our members were dropped as delinquents on December 30, 1946; (2) to seek an increase in our membership, especially among foundry firms where no memberships are held, and thus broaden the base of our entire membership; (3) to increase, conservatively, the number of Personal ($8.00 per year) Members in each Company or Sustaining Member organization; (4) to work more closely with the Chapter Membership Committee, by providing new membership aids.

Two steps have been taken to increase the number of membership renewals; (1) Offering a 20 per cent discount on A.F.A. Publications for renewals within 30 days of due date, an offer utilized by a fair percentage of members but which, as an accepted A.F.A. practice, should be used more widely from now on; (2) offering to all members in all classes a paper-bound copy of the 1947 TRANSACTIONS gratis, for the first time in a number of years. It is believed that these steps will materially help membership work.

Chapters

During the past year, five new A.F.A. Chapters were approved by the Board of Directors: the Washington Chapter, at Seattle; Timberline Chapter (formerly Rocky Mountain Empire Chapter), at Denver, Colo.; Tri-State Chapter, at Tulsa, Okla.; British Columbia Chapter, at Vancouver; and Central Michigan Chapter, at Marshall. These bring to a total of 38 the number of Chapters established since 1934. (One Chapter, the University of Minnesota Student Chapter, is still inactive but we have hopes of early reorganization).

Interest in the formation of additional Chapters has been expressed in recent months from New England; Williamsport, Pa.; Chattanooga, Tenn.; and Provo, Utah. A letter has been received from the New England Foundrymen's Association requesting information on the question of Chapter affiliation, and this inquiry will be followed up early this Fall.

Chapter Problems. Some concern has been expressed over the formation of Chapters in areas where the possibilities of a large membership simply do not exist. Several Chapters have also expressed the view the A.F.A. should take a more definite interest in Chapter financing, especially in the first three to five years of a Chapter's establishment. Inability to contact personally all Chapters at frequent intervals undoubtedly has given rise to a feeling that some Chapters are carrying on activities not too well directed. These thoughts indicate the desirability for more attention to establishment of two-way obligations between A.F.A. and its Chapters . . . an obligation on A.F.A. to give greater assistance to Chapters needing help, and an obligation on the Chapters to carry on effectively if they are to remain as Chapters representative of the Association.

We recommend that the Board of Directors give greater consideration to this problem in the coming year.

Chapter Visits. The question of visits of National Officers to the Chapters remains burdensome, and we doubt if even the appointment of a "Chapter Contact Man" will satisfy the desire of the Chapters for visits by the President and Vice-President of the Association. We believe, however, that the matter might be helped greatly if A.F.A. could provide a technical speaker, or even formulate and carry through an entire technical program, for Chapters during the year. Unless definite steps are taken to avoid the urgency of Presidential and/or Vice-Presidential visits to a large number of Chapters in a single year, the task of the A.F.A. Nominating Committee to select chief officers of A.F.A. will be increasingly difficult.

We recommend that the Board of Directors give serious consideration to this problem in 1947-48.

Chapter Chairman Conference. The Fourth Annual Chapter Chairman Conference was staged as a three-day meeting at the Palmer House in Chicago, on June 30-July 1-July 2. A total of 44 Chapter Officers attended, as against 37 a year ago, with every Chapter represented except Toledo, whose Chairman was forced to cancel at the last minute. For the first time, Mexico City Chapter was represented. More time was given this year for floor discussion, but the indications are that future conferences of this type must be worked up and promulgated to the attending officers well in advance of the Conference, if best results are to be achieved. Doing so is complicated by the fact that most Chapters elect new officers in May or even in June, but we intend to request that the Conference be made a subject for Chapter Board Meetings, well in advance of the event.

Since 1945 the Chapters have requested, increasingly, that both the Chairman and the Vice-Chairman be permitted to attend the Conference, and the 1947 Conference delegates again repeated this request. The A.F.A. Directors thus far have hesitated to comply out of a desire to keep the meeting more or less informal. However, since the attendance now exceeds 50, we recommend that the Board of Directors reconsider the question of inviting both the incoming Chairman and Vice-Chairman to future Conferences, with provision for the Chapters to share in the cost of bringing the Vice-Chairman.

Technical Department

A separate report of Technical Director S. C. Massari deals in detail with technical activities of the Association during the past year. In addition, several points involving our technical operations should be commented on in general. It has been suggested that the goal of A.F.A. should be a staff member heading up the technical activities for each seperate metals division, such as gray iron, steel, malleable, etc. . . . all under the overall jurisdiction of the Technical Director. We intend to add a Ferrous Technical Assistant as soon as a capable man is found.

However, the goal mentioned cannot be realized in full under our present revenue circumstances. It has been suggested that, in order to achieve this goal, A.F.A. might approach the various trade associations of the industry for partial solution of this problem and in the interests of greater industrial cooperation.

The reserve of $100,000, held in the name of the Technical Development Program, as stated in the Secretary's Report a year ago, is still intact. This reserve was set up as a cushion for maintenance of technical activities in the event of decreased revenue due to slackening of industrial activities.

The reserves for the Cupola Research Project diminished somewhat during the past year for the production of the CUPOLA HANDBOOK, but have since been restored through sales of this handbook. With the approval of the Cupola Research Committee, it is intended to reimburse the fund for any withdrawals on behalf of the Project, reserves beyond this point to be used to reimburse the General Fund for administration thereof.

Publications

The Report of Technical Director Massari outlines work during the past year on Special Publications, and the direction of our efforts during the coming year. It has become evident that more attention must be given to continuous preparation of new books, so as to keep our book lists entirely up to date. Consequently, steps are being taken to implement this need early in the new fiscal year.

It has been evident for some time that insufficient use has been made of our Technical Library, consisting of some 1200 volumes. It is felt that this Library, if kept in efficient and catalogued order and added to as worthy publications appear, can be of far greater usefulness to both the Staff and our members. Therefore, approval has been obtained to add a Librarian to the A.F.A. Staff. In addition to maintaining the Library, the Librarian will be required to prepare abstracts on various technical subjects and to maintain a "Who's Who" of the industry for use by the AMERICAN FOUNDRYMAN editorial department, the Board of Awards, and the Nominating Committee.

1947 Convention

The technical program of the 1947 Convention at Detroit, April 27-May 1, is adequately covered in the Report of the Technical Director, S. C. Massari.

Some 2914 registered at the Detroit Convention, with total attendance estimated between 4500 and 5000. No accurate count could be obtained since no control of attendance is possible at a non-exhibit convention.

It is probable that the success of the Detroit technical convention will induce other cities in Chapter areas to invite A.F.A. for similar annual meetings. The experience at Detroit indicates that between 1500 and 2000 guaranteed hotel rooms are necessary for such a non-exhibit convention, a fact which makes possible taking these technical meetings to a number of foundry centers not visited for many years.

Educational Activities

Progress in educational activities is covered in the separate report by Technical Director Massari.

To reinforce our educational program during the coming year, it is intended that H. F. Scobie be freed, as much as possible, from other Staff duties in order to concentrate on this program. A tremendous interest has been aroused in educational efforts throughout the foundry industry, and opportunity exists for A.F.A. to play a major part in this work—an opportunity that is immediate and pressing.

American Foundryman

With the experience of two years, the AMERICAN FOUNDRYMAN begins its third year in its present enlarged scope with an excellent record financially, but with need for additional editorial improvement. Under the direction of Terese Koeller, advertising revenue grossed $95,000 and $85,000 during 1945-46 and 1946-47, respectively. It is now our belief that the advertising program can be advanced still further to the point where advertising revenue can provide, as it originally was intended to provide, a steadier source of income than has been possible from exhibits held biennially. Accordingly, steps have been taken toward this end for the coming year.

During the past year, the Technical Department has cooperated splendidly in obtaining worthwhile articles for AMERICAN FOUNDRYMAN and in reviewing articles for possible publication. However, as the magazine advances editorially, it will become necessary to relieve the Technical department of responsibility for the major part of such procurement and review. As a result, the AMERICAN FOUNDRYMAN Staff will have to be increased editorially if it is to hold its own, and such increases must become a consideration of increased advertising revenue to the end that AMERICAN FOUNDRYMAN shall be entirely self-sustaining.

Staff Organization

During the past year, Staff organization plans have been discussed at length with the Executive Committee and, thanks to the wise counsel of the Committee Members, we are well on the way to more definite delineations of duties for a Staff capable of handling the constantly growing demands of an increasing membership and a progressive industry. The readjustments of the past two years, coupled with the desire for long-range planning, have brought about greater understanding as to the size and caliber of Staff required. It is now recognized that additional activities must be considered from the standpoint of their effects on present activities and the personnel required for their accomplishment.

New and enlarged quarters for the National Office were obtained, early in 1947, and have contributed to greater efficiency in carrying out our operations. Under present conditions, considerable expense was incurred in remodeling these quarters, and further contemplated additions to the Staff may make necessary consideration of additional office space.

Appreciation

The wholehearted and ready willingness of the Board of Directors and the Executive Committee to consult with and advise the National Office Staff has been of the greatest help and encouragement during the past year of our operations. From the standpoint of administration, we cannot stress too strongly the thought that such close cooperation between the National Office and the Directors is equally important to close cooperation between the National Office and our Chapters, if A.F.A. is to continue its present rate of progress. The opportunity to discuss our problems with the Directors, in terms of service to the foundry industry, is of inestimable value to the satisfactory performance of Staff obligations, and we hope that the policy of frequent meetings with the Board and Executive Committee will be continued.

Respectfully submitted,

WM. W. MALONEY, *Secretary*

Report of the Technical Director for the Fiscal Year 1946-47

During the course of the year, progress reports have been made to the Executive Committee and the Board of Directors, so that they might be fully apprised of important activities. Following is a brief consolidated statement of the entire year's work, grouped in accordance with the more important phases of our technical activities.

Technical Committees

A review of the technical structure at the beginning of the year gave a definite indication that it should undergo a reorganization for the purpose of streamlining the various committees, and establishing a uniform type of divisional organization to accomplish proper interrelation and appropriate representation within the division and between the several divisions. Through such an organization, we could hope to achieve a better understanding on the part of the several committees, attain greater efficiency of performance ,and avoid duplication of effort.

Following approval by the Board of Directors, the committee reorganization plan was submitted to the several divisions for their consideration, and fortunately was welcomed. In accordance with the dictates of the plan, a complete Committee Roster was prepared, only after the written acceptance by each member of a committee. This plan reactivated the Technical Correlation Committee, the policy-making group for all technical activities, which had its first meeting in several years in Chicago on June 20, 1947. This meeting proved to be most effective and served to lay the foundation for future policies relating to committee operation, convention planning, and the publication policies. This group approved the Rules for Committee Operation, thereby establishing well integrated mechanism for committee performance.

With few exceptions, the divisional activities are working effectively, this being especially true of the Program and Papers Committees, so essential for a convention program composed of papers of a high standard of quality.

A.F.A.-Sponsored Research

Following approval by the Board of Directors of the A.F.A.-sponsored research plan, several research projects are already actively under way. Sand Research and the Heat Transfer Research Projects are actively functioning, and a great deal of progress has been made. Definite contracts for the coming year have already been executed.

Of the remaining divisions, the Aluminum and Magnesium Division has planned its research project and a definite contract has been entered into with Battelle Memorial Institute. Work will be initiated on this project in early Fall.

The Brass and Bronze and Malleable Divisions have prepared their projects and are ready to proceed, following approval by the Board of Directors of the expenditure of funds. The Steel, Gray Iron, and Pattern Divisions are in process of organizing their research committees and deciding upon their initial project.

The Cupola Research Project has just employed a research worker for the purpose of making plant surveys of cupola operation, under the direction of the committee. In addition, we have been successful in obtaining the cooperation of the Canadian Bureau of Mines on this project, at no cost to the American Foundrymen's Association.

Educational Activities

The Educational Division was formed on October 18, 1946, to coordinate the activities of existing committees in the fields of apprentice training, foreman training, and engineering schools. The result has been unity of action with renewed vigor and interest in foundry educational work.

Major educational activities of the year are:

(a) Three outstanding educational sessions at the 1947 convention, including the Educational Division Business Meeting.

(b) Formation of a Youth Encouragement Committee and the Development of a good youth encouragement program.

(c) Publication and partial distribution of two booklets, THE FOUNDRY IS A GOOD PLACE TO WORK and MILITARY ASSIGNMENTS RELATED TO THE FOUNDRY INDUSTRY to employment agencies, educational publications, personnel directors, libraries, foundrymen and students.

(d) Publication in AMERICAN FOUNDRYMAN of one educational article or committee report each month, as well as miscellaneous news items of educational interest.

(e) Increased interest in the 1947 A.F.A. Apprentice Contest by wider publicity and more attractive prizes.

(f) Formation of a revised countrywide educational program and a chapter educational program, both of which were presented at the 1947 Chapter Chairman Conference.

Convention Program and Publication Policy

During the beginning of 1946, we attempted to establish a much needed publication policy, which among other things established a deadline date for papers which were to be accepted for the convention, the development of an Author's Offer Form, and a Reviewer's Form for the Program and Papers Committee. This policy likewise involved the preprinting of all convention papers for the 1947 Convention, and 62 technical papers were presented, including seven which were published in the Pre-Convention issue of AMERICAN FOUNDRYMAN.

The GUIDE TO AUTHORS was completely revised and made available to all prospective authors. This publication has met with general acceptance and we have received numerous requests for it.

Convention plans are well formulated and the Program and Papers Committees are actively working on the convention program for 1948.

Special Publications

The 1945 Annual TRANSACTIONS were available for distribution in October 1946. The 1946 TRANSACTIONS will be printed by this Fall, and we hope to have the 1947 TRANSACTIONS available about the first of the coming year. Paper bound copies of the latter TRANSACTIONS will be available gratis to members on request.

The following books were published and offered for sale during the year: MALLEABLE FOUNDRY SAND AND CORE PRACTICE SYMPOSIUM, FOURTH ANNUAL FOUNDATION LECTURE ("Test Bars for 85-5-5-5 Alloy, Their Design and Some Factors Which Affect Their Design") and reprints of the following: FOUNDRY COST METHODS; SYMPOSIUM ON MALLEABLE IRON MELTING; FUNDAMENTALS OF DESIGN, CONSTRUCTION, OPERATION AND MAINTENANCE OF EXHAUST SYSTEMS; TENTATIVE CODE OF RECOMMENDED PRACTICES FOR TESTING AND MEASURING AIR FLOW IN EXHAUST SYSTEMS; SYMPOSIUM ON GRAPHITIZATION OF WHITE CAST IRON.

The book on CASTING DEFECTS should be available approximately October 1st, 1947; the SYMPOSIUM ON DUST CONTROL about August 15, 1947; and the HISTORY OF THE CASTINGS INDUSTRY by the end of the year. The following publications likewise are in process of editing, revision or preparation by a committee: SYMPOSIUM ON PROPERTIES OF GRAY CAST IRON, THE MICROSCOPE IN ELEMENTARY METALLURGY, CORE BOOK, FOUNDRY SAND TESTING HANDBOOK, and SYMPOSIUM ON MAGNESIUM FOUNDRY ALLOYS.

The Committee on Precision Investment is progressing rapidly on the preparation of a book on this subject. A committee of the Malleable Division is actively engaged in the preparation of a book on "Recommended Practices for the Malleable Foundry."

Cooperation With Other Societies

Upon request of the American Society of Tool Engineers, A.F.A. prepared a chapter on Cast Metals for inclusion in their Handbook; similarly, a very comprehensive manuscript was prepared for the American Society of Mechanical Engineers for their Metals Handbook. Both of these publications are now in process of editing and very likely will be available within the near future.

Active cooperation has also been maintained on technical matters of mutual interest with the American Society of Mechanical Engineerrs, Society of Automotive Engineers, American Society for Testing Materials, American Standards Association, American Society of Engineering Education, and the American Vocational Association. Active participation has been maintained with the Foundry Educational Foundation, of which the A.F.A. is a sponsoring society. A profitable contact has been made with the Army-Navy Munitions Board, to establish proper liasion between A.F.A. and this group, so as to be helpful in their industrial planning.

Technical Service

During the period November 1, 1946 to June 30, 1947, the technical staff replied to 230 individual technical inquiries received.

The technical staff has been appreciative of the understanding support and cooperation received from the Executive Committee and the Board of Directors during the past year. Their counsel and encouragement have been extremely helpful in accomplishing a more orderly organization of our technical affairs.

Respectfully submitted,
S. C. MASSARI, *Technical Director*

Comparative Condensed Balance Sheet

As of June 30, 1945, 1946 and 1947

ASSETS	June 30, 1945	June 30, 1946	June 30, 1947
Cash	$ 86,920.86	$131,341.19	$ 58,216.09
Securities	131,993.90	181,949.60	181,949.60
Accounts Receivable	2,404.59	9,379.56	9,457.28
Inventories	21,364.52	33,182.95	23,898.63
Prepaid Expenses	206.73	1,780.21	13,815.04
Furniture & Fixtures	2,017.93	3,765.59	6,354.21
TOTAL	$244,908.53	$361,399.10	$293,690.85

LIABILITIES	June 30, 1945	June 30, 1946	June 30, 1947
Accounts Payable	$ 374.40	$ 18,197.03	$ 26,618.98
Reserves	49,939.99	51,069.06	52,134.53
Accrued Items		2,013.17	733.05
Deferred Income	8,893.70	12,991.53	24,264.13
Unexpended Special Funds	141,705.87	164,699.80	174,346.16
Surplus	43,995.07	112,428.51	15,594.00
TOTAL	$244,908.53	$361,399.10	$293,690.85

Condensed Statement of Income & Expense

(All Expenses Distributed to Major Activities)

Fiscal Year July 1, 1946-June 30, 1947

INCOME

Membership Dues	$158,382.96
Publications	25,808.84
AMERICAN FOUNDRYMAN	84,046.25
Convention 1947	1,488.00
Other Income	88.50
Appropriation from 1945-46 Surplus	32,800.00
TOTAL	$302,614.55

EXPENSE

MAJOR ACTIVITIES	Amount	Total Expense Per Cent
General Administration	$ 91,858.77	25.4
Chapter Activities	51,715.77	14.3
Technical Activities & Publications	111,026.15	30.7
AMERICAN FOUNDRYMAN	92,582.07	25.6
Convention & Exhibit	14,465.95	4.0
TOTAL	$361,648.71	100.0

Comparative Membership Report

As of June 30, 1943-1947 Inclusive

	June 30, 1943	*June 30, 1944*	*June 30, 1945*	*June 30, 1946*	*June 30, 1947*
Sustaining Members	113	169	239	183	193
Company Members	1,341	1,471	1,571	1,462	1,548
Personal Members	3,958	4,720	5,722	6,426	7,359
Student and Apprentice Members	79	42	23	20	89
Honorary Life Members	54	55	55	58	64
Foreign Members	120	163	205	390	430
Total	5,665	6,620	7,815	8,539	9,683
New Members, 12 months	1,172	1,510	1,803	1,743	2,164
Resignations	150	215	235	370	186
Delinquents dropped	384	316	356	631	829
Removed by death	20	24	17	18	5
Net gain for year	618	955	1,195	724	1,145
Members in Chapters	5,003	5,902	7,110	7,767	8,809

Chapter Distribution

June 30, 1947

Chapters	*Sustaining*	*Company*	*Personal*	*Affiliate*	*Associate*	*Apprentice and Student*	*Honorary and Life Membership*	*Chapter Membership*
Birmingham	4	41	67	156	6	0	1	275
British Columbia	0	14	16	30	5	0	0	65
Canton District	0	33	15	19	2	0	0	141
Central Illinois	1	9	17	104	3	7	1	142
Central Indiana	3	37	34	167	4	3	0	248
Central Michigan	0	7	18	27	0	0	0	52
Central New York	1	34	28	94	2	0	0	159
Central Ohio	1	32	26	126	4	0	1	190
Chesapeake	3	28	40	92	30	1	4	198
Chicago	23	121	121	502	17	15	9	808
Cincinnati	7	50	53	144	7	3	1	265
Detroit	15	60	86	283	20	8	5	477
E. Canada and Newfoundland	8	52	76	197	18	5	0	356
Metropolitan	11	70	94	202	16	0	2	395
Mexico	0	12	29	9	0	0	0	50
Michiana	3	40	36	135	4	2	0	220
Northeastern Ohio	17	86	97	308	13	3	9	533
Northern California	8	30	74	132	15	2	0	261
No. Illinois-So. Wisconsin	4	21	15	64	2	0	1	107
Northwestern Pennsylvania	2	21	21	63	1	2	0	110
Ontario	2	91	118	101	2	1	2	317
Oregon	2	11	25	56	4	2	0	100
Philadelphia	16	69	66	189	12	1	3	356
Quad City	4	42	47	129	4	1	2	229
Rochester	2	13	25	76	4	0	0	120
Saginaw Valley	6	19	15	229	3	11	2	285
St. Louis	2	49	66	139	5	5	0	266
Southern California	3	62	132	151	10	0	1	359
Texas	7	32	33	53	2	0	0	127
Timberline	0	5	54	17	3	0	0	79
Toledo	0	20	27	47	1	0	1	96
Tri-State	0	11	45	45	2	3	1	107
Twin City	5	40	43	121	8	5	0	222
Washington	0	15	24	25	7	0	0	71
Western Michigan	5	31	19	171	1	0	2	229
Western New York	1	40	51	130	2	0	1	225
Wisconsin	16	89	92	353	10	7	2	560
Total in Chapters	182	1437	1845	4958	249	87	51	8809
Total Foreign								430
Total Outside of Chapter								444
GRAND TOTAL								9683

Recommendations of Retiring President S. V. Wood

IT APPEARS to me that, as your retiring Executive Officer, it is not only fitting and proper but indeed an obligation that I should put before you some of the thinking and conclusions on various angles of operation and activities that have been brought home to me as we have dealt with the problems of the year.

I should like to point out briefly my observations as to things that have been done successfully, and call your attention to some mistakes that we may have made. This I am doing for the benefit of incoming officers in order that they may avoid mistakes of the past and profit by our successes. All of this I am presenting, not with any desire whatever to commit you to policies of this year, but rather to be helpful to all concerned, to the end that we may continue to progress.

First of all, it has been said many times that this is a technical society, an educational institution, to which I agree. To my mind, neither one of those things alone will make this a permanent organization. We are committed to certain things as a matter of business procedure and I think we should be careful that we do not allow ourselves to run wild in thinking only along technical lines, or only along educational lines.

There has been a strong feeling in my mind that we are not utilizing the full thinking of our Board members in our activities. You are all busy men; you are all over-crowded with work. Therefore, there is always a reluctance among your officers to impose excessive travelling incident to more frequent Board meetings. As a result, I feel that many Directors may have felt the lack of a full understanding of what goes on, lack of familiarity with the policies and plans as they are brought out by agenda at the few meetings held.

May I therefore suggest that we endeavor to increase the insight of our Board members by assigning to them individual or group thinking upon specific questions constantly arising, to the end that each member, at home and at his leisure, may prepare himself to give the whole Board of the Executive Committee the benefit of his careful and best thinking on that particular subject.

This member might then well be invited to attend an Executive Committee meeting to give the Committee his mature thoughts and lead the discussion, thereby avoiding decisions often hurriedly arrived at by Executive Committees as a result of crowded calendars which leave too little time for study and decision.

It is my feeling, based upon observations of this year's meetings at Chapters, that eventually we must give thought to the formulation of definite policies on Chapter operations and control. The Chapters have been created and built up until now largely by unguided evolution, with the result that we have great variance in the activities, operations and thinking within these groups—some good, some bad.

Should we not put together a committee from our Board to start thinking and planning definitely for this problem of policy control for Chapters, with no desire to take away from the Chapters their individuality but rather to provide them with an overall guide that will point out the areas within which they must operate for the general benefit of the Association as a whole?

In my judgment this should be done rather soon, for tomorrow may be too late. Tomorrow the tail may be wagging the dog and we may find that some carelessly operated Chapter may unthinkingly fall into the fault of assuming that they have no responsibility to the general Association.

How many Chapters should we have? Can there be too many Chapters? These questions and others of a similar nature involve careful thinking. Our Chapters are improving constantly, but we must give concentrated thinking to questions similar to these.

Can we not well put two or three of our Directors to work thinking on such problems against the day when we may suddenly be confronted with a delicate and difficult problem of deciding what to do with this Chapter question? Would not each one of you Directors welcome the opportunity to attack some particular question as an assignment and become an expert upon this particular subject so that your Executive Committee and officers might turn to you for expert opinion when this question may arise?

Chapter Chairman Conference

We have just held our fourth Chapter Chairmen's Conference and I am only sorry that all Directors could not have been present to see this group of new chairmen at work, attentively and enthusiastically trying to learn how to become the best possible leaders of their Chapters. It was indeed inspiring and enthusing to see these men at work.

These conferences have become, in my judgment, the best meetings of the year, and I am particularly proud to have been a part of the group of men who conceived this idea. However, these meetings occur only once a year, and we badly need a Chapter contact man whose business it is to visit Chapters continuously. Today we are trying to find a man to do the job. It is a big order and men of this type are scarce. It is my hope that we will continue in our search and that soon we may be successful in finding and employing a man capable and willing to attempt this job.

Inter-Society Relations

During the past year your officers have endeavored to better the relationships existent between the American Foundrymen's Association and the various trade associations and other groups allied with the foundry industry. Much progress has been made along this line, but it must be an ever-continuing process.

The National Castings Council is making progress and is painstakingly putting in the footings for a substantial structure that will accomplish much for the industry if we continue to unselfishly support this effort.

It is my hope that we shall continue to vigorously promote better relationships between independent societies and groups within our industry. May I suggest that this next year our elected officers issue invitations to elected officers of these other groups, one group at a time, in order that they may meet and discuss our common problems to promote better acquaintanceship and understanding?

May I also strongly urge a vigorous and continuing support by the American Foundrymen's Association for the Foundry Educational Foundation, of which this Association at present is a sponsoring member. This Foundation is meeting with remarkable enthusiasm and is in the hands of fine management, all of which speaks well for its sponsors and truly merits our support.

National Officer Nights

As you very well know, I have this year visited a number of Chapters. It has indeed been a rare and pleasant experience, although, I can assure you, an exhausting one.

You will remember that last year when I accepted this assignmen of acting as your President for the year, I asked for the privilege of changing somewhat the usual routine of National Officer visitations at the various Chapters. I asked for this privilege due to the fact that I have for some years had a definite feeling that something must be done to sell management on the American Foundrymen's Association.

With that thinking in mind, whenever I have visited Chapters I have tried to arrange for at least two distinct meetings: first, a Management luncheon to which only top management was invited; and second, a meeting with the Board of Directors and Committee Chairmen of the local Chapters. If the occasion of my visit happened to coincide with a regular Chapter meeting,

I would also arrange to appear at that regular meeting of the Chapter membership, not as the principal speaker but as an extra speaker on their regular program.

In none of these meetings have I attempted to make a technical talk, for several simple reasons. First, in the case of management and the boards, they were interested in thinking other than of technical matters; and in the case of the general membership meeting, the avoidance of a technical talk was due to the fact that, it being their regular meeting, they had already arranged for one. One other reason is a personal one—the fact that I have been so long removed from the technical part of foundry activities that I no longer feel competent to assume the responsibility for a talk of this nature.

My remarks in each case have been concerned with sales talks for the American Foundrymen's Association, its value to individuals, employer and employee, and the process by which it operates.

For me to express an opinon as to the success of my efforts would be presumptuous indeed. Such an appraisal must come from the words of commendation immediately following the meetings and letters which have come to me later from those in attendance. I am convinced, however, that if I have failed to sell the American Foundrymen's Association to the above-mentioned groups, it is not because the idea was wrong but only because I have not been able to do the job personally as well as others might have done it.

It is my strong conviction that this type of effort is well worth while. It can be done only by National Officers and Directors, and I commend this type of effort to the Directors for the ensuing year.

If in your judgment this idea is meritorious and should be continued, I would sincerely recommend careful advance planning to set up these three meetings for Chapters with proper advance arrangements to dignify them. I consider it very important that the anticipated attendance have a definite idea as to what is going to be discussed and the purpose for which the meetings are being held.

National By-Laws

May I suggest some thought be given to our By-Laws Committee. Many times situations arise wherein it appears that changes in by-laws might clarify and solidify our controlling boundaries. These situations appear generally in the midst of discussions pertaining to matters far removed from the question of by-laws, and due to the fact there is not at hand sufficient time for the Executive Committee or the Board to give much thought to the particular point.

Would it not be constructive to put such questions immediately into the hands of the By-Laws Committee for their consideration, asking that they study, recommend and report back upon the point in question as soon as convenient? This would obtain thinking on the matter while it still is in the minds of the Board or Executive Committee and could be put into the files together with any action suggested by the Board or Executive Committee, against the day when general revisions might be in order as a result of an over-all study by the By-Laws Committee.

The By-Laws Committee, it appears to me, should be a standing committee, charged of themselves to make suggestions or changes as necessary, and to give thought and study to such points as mentioned above.

Apprentice Contest

As you know, we have an Apprentice Contest of long standing and indeed a worthy effort. It appears to me we might well give it some additional attention and expand the number of Chapters participating, as well as the number of contestants.

Might we not give better publicity to the men involved? To publicize the winners is fine, they are to be congratulated; but should we not publish the names of all contestants? Is not the weakest contestant to be commended, at least for entering the contest? Should we not make sure that his boss, the man at the top, knows of his effort in order that the apprentice may take more pride in his effort, even if not a top winner?

It is my feeling it would be well worth while to examine anew this whole apprenticeship contest and make more of it.

A.F.A. Awards

What about annual awards? How many of our Board members are thoroughly informed as to this activity? Who are the recipients of these awards? How are they chosen? I am not raising these questions in criticism, but rather to arouse interest, increase the significance and strengthen the dignity of the awards, lest in our lack of knowledge of what is involved we become careless in our search for merit.

Let us not allow a lack of information as to these awards cause a lowering of standards of merit and thereby the quality of honors which we wish to bestow. Awards are highly prized, but only if the standards of merit are jealously protected. Should we not have a committee of the Board to study this activity?

Nominating Procedures

Another question involving by-laws, consistent methods and procedures which seems to merit attention and study, is the matter of selection of our Nominating Committees, and all of the election procedures. With an ever-expanding membership and an increasing number of chapters, the present procedures are becoming somewhat cumbersome. Those who have had experience with election matters in recent years will concur, I am sure, in the opinion that study is necessary to clarify the operation and so assure proper representation.

This is quite an involved subject and I am not making any proposal except to invite your consideration of the whole matter.

"American Foundryman"

May I direct your thinking for a few minutes to the subject of AMERICAN FOUNDRYMAN? You know, of course, that only recently did we decide to expand this publication and to accept advertising.

I am more than ever convinced that in adopting this policy we acted wisely. It must be said, however, that in so doing it plunged us into a very perplexing activity; an activity which calls for the best thinking of all of us.

We have made much progress with this publication. The future is ahead of us, a future which will be a credit to the American Foundrymen's Association if we but plan carefully, be wise in policy and pursue our policies vigorously.

Our reading public is composed of a membership at many levels. It must necessarily continue so. Reader interest must be not only maintained, but increased to the end that the entire membership reads "from cover to cover." We must take care not to lose sight of the basic things we are trying to accomplish with this activity, and we must plan our policy accordingly. We must strengthen our editorial staff and our advertising sales personnel.

Only a high grade publication will do credit to the Association. It is my sincere hope that plans for renewed vigor in this activity will be an accepted part of the future.

Appreciation

The foregoing points I trust will be helpful, and once again I would assure you that they have been mentioned with no desire to commit the Board, its committees or its officers to any of them, but rather in a spirit of helpfulness for the general welfare.

Suggestions which I may have for the Staff have been given to various members and have been presented to them in a similar spirit.

In closing, may I thank you most sincerely for the opportunity that you have given me to serve as your Executive Officer for the year 1946-47. It has been a glorious experience which I shall cherish throughout my life.

Your courtesy and kind helpfulness has been most gracious and I am deeply appreciative. If I have rendered some service of value, I shall be happy indeed and shall truly feel that I have made at least a partial payment on the great debt which I owe to A.F.A.

To the Staff goes my very best thanks for a job well done. I am deeply grateful for the cooperation I have had from all members of the group, and I am happy to commend them to the incoming Officers and Directors. Their job is a difficult one; it calls for hard work, long hours, tact, tolerance, patience, understanding, and a willingness to serve an ever-changing group.

Fourth Annual Chapter Chairman Conference

June 30-July 2, 1947 — Palmer House, Chicago

PROGRAM

Monday, June 30

9:30 AM MORNING PROGRAM

Presiding Chairman, Bill Wallis. *Introductory Remarks* on purpose of Chapter Chairman Conference.

Welcome to Chapter Officers .....*President S. V. Wood*

'Round-the-Table Introductions .....*Chairman W. B. Wallis*

Know Your A.F.A. .....*President S .V. Wood*

A brief outline of A.F.A.—its origin, development, basic policies and aims, and its place in the Foundry Industry.

Technical Activities of A.F.A. .....*Tech. Dir. S. C. Massari*

An outline of how A.F.A. functions to develop and disseminate technical and practical information for foundrymen. Publication policy. Research activities.

Technical Publications of A.F.A. ....*Tech. Dir. S. C. Massari*

Description of some major publications of A.F.A., including published books, books being revised, new books scheduled. The A.F.A. Library.

National Office Operations .....*Sec'y. W. W. Maloney*

How the National Office operates in the interest of the Chapters and the membership. Closer relations with the Chapters. Conventions and Exhibits. Organization. "Off-the-Record." Membership reports.

Remarks by Vice-President Kuniansky

2:00 PM AFTERNOON PROGRAM

Chairman Wallis, presiding.

AMERICAN FOUNDRYMAN ...*Chairman Wallis*
President Wood
Vice-President Kuniansky

Bringing out scope and plans for *"American Foundryman."* Purpose and importance of magazine in present form, service to membership, place of magazine in Association activities. *Practical articles wanted.*

Chapter Programs .....*Vice-President Kuniansky*

6:30 PM DINNER FOR CHAPTER CHAIRMEN

Presiding—Chairman Wallis.

Announcement—Secretary Maloney.

Guest Speaker—E. A. McFaul, De Paul University.

Subject: "So You Think You're Slipping."

Tuesday, July 1

9:30 AM MORNING PROGRAM

Chairman Wallis, presiding.

Chapter Operations .....*Chairman Wallis, Moderator*

Entire Morning Program will be devoted to a PANEL DISCUSSION on Chapter Operations, panel to include *Chairman Wallis* as Moderator, a Panel Guest from among the Chapter Delegates and the following:

President Wood, Director Walls, Director Smith, Secretary Maloney, Technical Director S. C. Massari.

(1) *Management Meetings* .....*Panel Guest: E. C. Troy, Philadelphia*

Experiences and discussion relating to the holding of meetings for *Top Management.*

(2) *National Officer Visitations* ...*Panel Guest: W. E. Jones, Birmingham*

Experiences and suggestions on staging National Officer Nights during 1947-48, and discussion of plan for more efficiently arranging such visits. How program should be arranged.

(3) *Membership Activities* .....*Panel Guest: O. L. Voisard, E. Canada-Newfoundland*

Discussion of Chapter membership activities and how Membership Committees function. How new member applications are handled and approved. Membership aids of National Office. Holding present members.

(4) *Nominations and Elections* ...*Panel Guest: F. W. Fuller, Central Ohio*

Discussion on Nomination and Election procedures of the Chapters . . . Selection, Appointment, Number, Instructions, Attendance, Candidates, Elections.

(5) *Chapter Officer Succession* .*Panel Guest: R. J. Anderson, Wisconsin*

Discussion of Chapter policies regarding definite plans for the succession of Officers, as now employed by a few Chapters. What experience should represent consideration for Director, for Secretary, for Treasurer, for Vice-Chairman, and for Chapter Chairman. Advantages (or disadvantages) of a paid Secretary.

(6) *Chapter By-Laws* .....*Panel Guest: W. W. Bowring, Detroit*

Discussion of Chapter by-laws in light of (a) Uniformity, (b) Conformity with National by-laws. Issuance to new members. Amendment procedure. Number of Directors specified. Number of Board Meetings per year.

(7) *Chapter Finances* .....*Panel Guest: J. W. Clarke, N. W. Pennsylvania*

Discussion on how to make Chapter self-supporting—Dues revenue – budgets – advertising – special solicitations. Importance of Treasurer. Average reserves per member. Where to utilize surplus for good of Chapter. Statements of Income and Expense for National Office.

12:45 PM LUNCHEON

Presiding—Chairman Wallis.

Guest Speaker—G. K. Dreher, Executive Director, Foundry Educational Foundation, Cleveland.

Subject: "The Foundry Goes to College."

2:30 PM AFTERNOON PROGRAM

Chairman Wallis, presiding.

National Educational Activities .....*H. F. Scobie*

Outline and discussion of Educational activities undertaken by National Association. Opportunities for Chapters in 1947-48.

Chapter Educational Activities ..... *Chairman Wallis, Herb Scobie*

Discussion of Chapter Educational Committee activities and desirability of having such a Committee in *every Chapter.* Some outstanding Chapter work.

National Castings Council .....*President Wood*

A brief description of the Council and its possible future importance to the Foundry Industry.

National A.F.A. Financing .....*Sec'y-Treas. Maloney*

Outline of A.F.A. financial policy with comment on present financial status. Where the A.F.A. Dollar Comes From. Where the A.F.A. Dollar Goes. Comparative Income and Expense.

Wednesday, July 2

9:30 AM MORNING PROGRAM

Chairman Wallis, presiding.

Entire Morning Program devoted to presenting (a) Information of *direct* value to Chairmen in running their Chapters, and (b) Presenting information of *personal* value to Chapter Officers.

The Chairman's Job

By special arrangements, *Harold Hauslein,* of Harold Hauslein & Associates, Chicago, with broad experience in training sales personnel, was invited to conduct the entire program.

Here are the important subjects covered:

(1) *Courtesies to the Guest Speaker*
(2) *The Meeting Room Setup*
(3) *Setting up the Speakers Table*
(4) *Handling Tickets at the Meeting*
(5) *Arranging the Entire Program*
(6) *Proper Microphone Technique*
(7) *Arranging the Technical Program*
(8) *How to Induce Technical Discussion*
(9) *The Speech of Introduction*
(10) *The Speech of Presentation*
(11) *The Speech of Acceptance*
(12) *Getting up on Your Feet*

Official Remarks
by *Chairman Wallis* and *President Wood.*

12:30 PM LUNCHEON
Presiding—*Chairman Wallis.*
Guest Speaker—George N. Sieger, President and Gen. Mgr., S.M.S. Corporation, Detroit.
Subject: "How to Run a Meeting."

2:15 PM AFTERNOON PROGRAM
Chairman Wallis, presiding.
"The Shakeout" .....*Chairman Wallis*

2:55 PM CLOSING REMARKS
by *Chairman Wallis* and *President Wood.*

ATTENDANCE

Conference Chairman, W. B. Wallis, President, Pittsburgh Lectromelt Furnace Corp., Pittsburgh, Pa.; Vice-President Elect.
National President, S. V. Wood, President, Minneapolis Electric Steel Castings Co., Minneapolis.
National Vice-President, Max Kuniansky, Vice-President and General Manager, Lynchburg Foundry Co., Lynchburg, Va.
National Director, J. H. Smith, General Mgr., Central Foundry Div., General Motors Corp., Saginaw, Mich.
National Director, S. C. Wasson, Mgr., National Malleable & Steel Castings Co., Cicero, Ill.
National Director, B. L. Simpson, President, National Engineering Co., Chicago.
Guest Speaker (Dinner, June 30)—E. A. McFaul, Head, Dept. of Speech, DePaul University, Chicago.
Guest Speaker (Luncheon, July 1)—G. K. Dreher, Executive Director, Foundry Educational Foundation, Cleveland.
Guest Speaker (Morning, July 2)—H. F. W. Hauslein, Harold Hauslein & Associates, Engineers and Contractors, Chicago.
Guest Speaker (Luncheon, July 2)—George N. Sieger, President and General Manager, S.M.S. Corp., Detroit.
Guest—D. L. Parker, Fdy. Supt., General Electric Co., W. Lynn, Mass.; President, New England Fdymens. Assn.
Guest—W. H. Gude, Managing Editor, *The Foundry,* Cleveland.

Chapter Officers

Birmingham—Chairman W. E. Jones, Chief Engineer, Stockham Pipe Fittings Co., Birmingham, Ala.
British Columbia—Secretary-Treasurer L. P. Young, metallurgist, A-1 Iron & Steel Foundry, Ltd., Vancouver, B.C., Canada.
Canton—Chairman C. F. Bunting, foundry metallurgist, Pitcairn Co., Barberton, Ohio.
*Vice-Chairman E. H. Taylor, plant engineer, F. E. Myers & Bros. Co., Ashland, Ohio.
Central Illinois—Chairman A. V. Martens, president, Pekin Foundry & Mfg. Co., Pekin, Ill.
Central Indiana—Chairman W. B. Ziegelmueller, vice-president and general manager, Electric Steel Castings Co., Indianapolis.
Central Michigan—Chairman D. J. Strong, president, Foundries Materials Co., Coldwater, Mich.
Central New York—Chairman R. A. Minnear, foundry superintendent, Ingersoll-Rand Co., Painted Post, N.Y.
Central Ohio—Vice-Chairman F. W. Fuller, field engineer, National Engineering Co., Westerville, Ohio.
Chesapeake—Chairman W. H. Holtz, superintendent, American Brake Shoe Co., Baltimore, Md.
Chicago—President F. B. Skeates, foundry superintendent, Link-Belt Co., Chicago.
Cincinnati—Chairman E. A. Kihn, general foreman, Cincinnati Milling Machine Co., Cincinnati.
Detroit—Chairman W. W. Bowring, sales engineer, Frederic B. Stevens, Inc., Detroit.
*Vice-Chairman A. W. Stolzenburg, products and process engineer, Aluminum Co. of America, Detroit.
Eastern Canada and Newfoundland—Chairman O. L. Voisard, general superintendent, The Robert Mitchell Co., Ltd., Montreal, Que.
Metropolitan—Chairman K. A. De Longe, Development and Research Div., International Nickel Co., New York, N.Y.
Mexico City—President Manuel Goicochea, general manager, Fundiciones de Hierro y Acero, S.A., Mexico City, Mexico.
*Secretary-Treasurer N. S. Covacevich, Owner, Casa Covacevich, Mexico City, Mexico.
Michiana—Chairman H. B. Voorhees, foundry superintendent, Peru Foundry Co., Peru, Ind.
Northeastern Ohio—President H. C. Gollmar, general manager, Elyria Foundry, Elyria, Ohio.
*Vice-President E. C. Zirzow, Core Room Foreman, National Malleable & Steel Castings Co., Cleveland.
Northern California—President A. M. Ondreyco, plant manager, Vulcan Foundry Co., Oakland, Calif.
Northern Illinois-Southern Wisconsin—Chairman John Clausen, foundry engineer, Greenlee Bros. & Co., Rockford, Ill.
*Secretary-Treasurer L. C. Fill, foundry superintendent, Geo. D. Roper Corp., Rockford, Ill.
Northwestern Pennsylvania—Chairman J. W. Clarke, assistant superintendent of foundries, General Electric Co., Erie, Pa.
Ontario—Chairman James Dalby, manager, Wilson Brass & Aluminum Foundries, Toronto, Ont.
Oregon—Chairman A. R. Prier, manager, Oregon Brass Works, Portland, Ore.
Philadelphia—Chairman E. C. Troy, vice-president, Research and Development Div., Dodge Steel Co., Philadelphia.
Quad City—Chairman R. H. Swartz, general manager, Riverside Foundry Co., Bettendorf, Iowa.
Rochester—President L. C. Gleason, foundry engineer, Gleason Works, Rochester, N.Y.
Rocky Mountain Empire—Treasurer J. W. Horner, Jr., vice-president, Slack-Horner Brass Mfg. Co., Denver, Colo.
Saginaw Valley—Chairman M. V. Chamberlin, metallurgist, Dow Chemical Co., Bay City, Mich.
*Vice-Chairman O. E. Sundstedt, vice-president and general manager, General Foundry & Mfg. Co., Flint, Mich.
St. Louis—Chairman N. L. Peukert, supervisor, Carondelet Foundry Co., St. Louis.
*Secretary P. E. Retzlaff, assistant superintendent, Busch-Sulzer Diesel Engineering Co., St. Louis.
Southern California—President H. E. Russill, vice-president, Eld Metal Co., Los Angeles, Calif.
Texas—Chairman M. W. Williams, foundry manager, Hughes Tool Co., Houston, Texas.
Tri-State—Chairman R. W. Trimble, foundry superintendent, Bethlehem Supply Co., Tulsa, Okla.
Twin-City—Chairman S. P. Pufahl, president, Pufahl Foundry, Inc., Minneapolis.
Washington—Chairman C. M. Anderson, vice-president, Eagle Brass Works, Seattle, Wash.
Western Michigan—Chairman C. H. Cousineau, metallurgist, West Michigan Steel Foundry Co., Muskegon, Mich.
Western New York—Chairman E. A. Jones, plant superintendent, Lumen Bearing Co., Buffalo, N.Y.
*Vice-Chairman M. J. O'Brien, Jr., assistant works manager, Symington-Gould Corp., Depew, N.Y.
Wisconsin—President R. J. Anderson, works manager, Belle City Malleable Iron Co., Racine, Wis.

* Attending on Chapter's request.

Minutes
1st Meeting 1946-47 Board of Directors
Hotel Stevens, Chicago—July 26, 1946

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: H. A. Deane, F. J. Dost, E. W. Horlebein, H. H. Judson, J. E. Kolb, R. T. Rycroft, S. C. Russell, J. Sully, J. H. Smith, B. L. Simpson, F. J. Walls, S. C. Wasson, F. M. Wittlinger, L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Also Present: Retiring Directors: R. M. Jacobs, R. J. Teetor, W. B. Wallis, retiring Treasurer C. E. Hoyt.
Absent: Directors: G. K. Dreher, H. G. Lamker

President Wood accepted the gavel from Retiring President F. J. Walls as the new President of A.F.A. and expressed his great pleasure at having served as a Director and Vice President under previous administrations.

Election of Executive Committee and Staff Officers

As a first order of business President Wood appointed a Board Nominating Committee consisting of Director Horlebein as Chairman, and Directors Russell and Simpson, to place in nomination four members of the Board for election to the 1946 Executive Committee, as well as names for the election of Staff Officers. The Committee retired and on return offered in nomination for the Executive Committee the names of Directors Sully, Smith, Walls and Wilson, to serve in accordance with the By-Laws of the Association, with the President and Vice-President. On motion duly made, seconded and carried, the Directors nominated were declared unanimously elected by the Board.

The Nominating Committee recommended that the offices of Secretary and Treasurer be combined and nominated Secretary Wm. W. Maloney as Secretary-Treasurer for the fiscal year. On motion by Director Horlebein, seconded by Director Rycroft and unanimously carried, the Secretary-Treasurer was elected.

Recommendations of the Finance Committee were presented dealing with staff compensations in excess of $3600.00 per year covering the fiscal year 1946-47. On motion by Director Horlebein, seconded by Director Russell, the recommendations were approved.

The Board unanimously approved the appointment of S. C. Massari as Technical Director and John P. Mullen as Assistant Treasurer in charge of Publicity and Public Relations Work, both for the fiscal year ending June 30, 1947.

Approval of 1946–47 Budget

President Wood announced that the 1946-47 Finance Committee would consist of Director Walls, as Chairman, Vice-President Max Kuniansky, and the President. He stated that the Finance Committee had held its first meeting in Chicago, June 27, for the purpose of formulating a 1946-47 budget of estimated income and expense.

On motion by Director Smith, seconded by Director Wilson, the budget as prepared by the Finance Committee was approved.

Authorization for Appointment of Standing and Special Committees

In accordance with an accepted Board procedure, President Wood entertained a motion authorizing the President to appoint Standing and Special Committees as might be deemed necessary, subject to approval by the Board. Motion was made by Director Smith, seconded by Director Rycroft, and unanimously carried.

President Wood then declared that with the exception of the Finance Committee, personnel of which he had already announced, he would withhold appointments to the various standing and special committees until a later date.

President Wood also announced that Chairmanship of the National Membership Committee had been accepted by Vice-President Kuniansky, and that the Chairman of each Chapter Membership Committee would comprise the balance of the Committee personnel.

Action on Recommendations of 1945–46 Board of Directors

Recommendations of the 1945-46 Board were considered and actions taken by the 1946-47 Board covering the fiscal year ended June 30, 1947, as follows:

(a) On motion of Director Smith, seconded by Vice-President Kuniansky, the recommendation on retirement compensation proposed for Treasurer C. E. Hoyt in resolutions of the Board adopted July 19, 1945, and as recommended by the 1945-46 Board, was approved.

(b) On motion by Director Smith, seconded by Director Horlebein, the recommendation on retirement compensation for Secretary Emeritus R. E. Kennedy, as approved by the Board of Directors on July 19, 1945, was approved for extension during the 1946-47 fiscal year.

It was announced by the Secretary that other obligations assumed by the Association on behalf of Secretary Emeritus R. E. Kennedy, in accordance with resolutions taken at the 1945 Annual Board Meeting, had been concluded prior to June 30, 1946.

(c) On motion by Vice-President Kuniansky, seconded by Director Smith, it was expressed as the sense of the Board, and as recommended, that Treasurer C. E. Hoyt and Secretary Emeritus R. E. Kennedy, whose services have proved of incalculable value to the Association and to the foundry industry as well over more than a quarter of a century of A.F.A. progress, be definitely retired as of August 1, 1946, and that the Secretary be given no discretion otherwise. Motion unanimously carried.

(d) On motion by Director Teetor, seconded by Director Smith, the recommendation for compensation of N. F. Hindle during the fiscal year 1946-47, covering the expected period of his convalescence, was approved.

(e) Recommendations for adoption of a program for (1) Technical Committee Organization, (2) 1947 Technical Convention Program, (3) A.F.A. Sponsored Research, and (4) A.F.A. Publication Policy, were unanimously approved.

(f) Recommendations covering (a) authorized signatures for issuance of checks by the Association, (b) establishing a General Reserve Fund checking account at the Harris Trust Company, and (c) establishing a Cupola Research Fund checking account at the Northern Trust Company, were approved.

(g) Recommendations that First, Second and Third prizes in each of four contests for the National Apprentice Contests of A.F.A. be increased to $100.00, $50.00 and $25.00, respectively, were approved.

(h) Recommendation that President Wood be authorized to explore further the matter of A.F.A. participation in an over-all Committee of Foundry Associations, subject to approval by the Executive Committee of possible commitments, was approved.

Safety and Hygiene Program

Lengthy discussion took place regarding the proposed A.F.A. Safety and Hygiene Program, Director Wilson commenting extensively on the status of his investigations as Chairman of a Temporary Committee appointed by the 1945-46 Board to explore the need for and possibilities of such a program. Due to lack of time, this subject could not be covered at the Annual Meeting of the 1945-46 Board, being referred to the new Board for consideration and possible action. On motion by Director Walls, seconded by Director Horlebein, the Board instructed the Secretary to procure membership by A.F.A. in the American Standards Association.

It was pointed out that action by A.F.A. on the proposed Safety and Hygiene Program might well be withheld pending further investigation of the possibility that such a program might become an undertaking of an overall committee of the various foundry associations, or that it might be underwritten by such a group. In view of the purely formulative period existing with regard

to such an overall committee, it was the sense of the Board that action by the A.F.A. Board on the Safety and Hygiene Program should be withheld pending further report by President Wood. It was felt that such a report might well be considered at the next meeting of the Executive Committee of the Board.

French Foundry Congress

The Secretary called attention to the holding of a French Foundry Congress in Paris on October 18, 1946, and stated that it was possible that a meeting would be held at that time concerning revival of the former International Committee of Foundry Technical Associations. It was pointed out that Vincent Delport of London is still the European Representative on the A.F.A. Committee of International Relations. On motion by Director Russell, seconded by Director Horlebein, the Secretary was instructed to request Mr. Delport to act officially for A.F.A. at the French Foundry Congress in October, 1946.

1947 Chapter Chairman Conference

On motion by Director Horlebein, seconded by Director Deane, the Board approved the holding of a 1947 Chapter Chairman Conference, the time to be announced at a later date.

In connection with recommendations made at the 1946 Chapter Chairman Conference, the Board expressed the opinion that, due to lack of time, such recommendations should be presented before the next meeting of the Executive Committee for proper consideration.

1947 and 1948 Convention Cities

The Secretary called attention to an invitation extended July 1945 by the Northern California Chapter for A.F.A. to hold the 1947 Convention and Exhibit in San Francisco. He pointed out that, due to an implied policy of holding exhibits in even-numbered years, the question of exhibit support of such an event was of considerable importance. He reported conversations held to determine the possible degree of such support had given evidence that it would be difficult to stage a successful 1947 exhibit on the West Coast.

Director Wilson expressed the opinion that the West Coast would not be interested in a non-exhibit Convention but would prefer awaiting such time as a combined Convention and Exhibit could be held in that area, largely due to the great distances between Chapters. He emphasized, however, the interest of West Coast foundrymen in having the Annual Convention and expressed the hope that such an event might materialize in the not distant future.

Vice-President Kuniansky pointed out that every consideration should be given the West Coast for a future Convention and Exhibit but that considerations of revenue as well as distances would necessarily have to be taken into account. Following some further discussion, Director Horlebein moved that the selection of the 1947 Convention city be referred to the Executive Committee for decision. Motion seconded by Director Wilson and carried.

With regard to the 1948 Convention city, the Secretary reported that conversations were being held but that recommendations could not yet be made. The consensus was that the matter should be referred for future action by the Executive Committee.

There being no further business, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
August 15, 1946.

Minutes
Meeting 1946-47 Executive Committee
Palmer House, Chicago—November 6-7, 1946

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: J. H. Smith
Joseph Sully
F. J. Walls
L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Reading and Approval of Minutes

Minutes of the Annual Meeting of the 1946-47 Board of Directors, held in Chicago, July 26, 1946, were read and approved.

Appointment of Standing and Appointive Committees of the Board

The personnel of all Standing and Appointive Committees, as appointed by President Wood, was read as follows:

By-Laws Committee—Reappointed as constituted in 1945-46 in view of the fact that the Committee has not yet submitted a set of revised By-Laws. Personnel reappointed as follows:

H. Bornstein, *Chairman*
H. A. Deane
E. W. Horlebein
W. B. Wallis
R. F. Lincoln
C. E. Hoyt, Consultant
W. W. Maloney, Secretary, ex-officio

Finance Committee—Personnel as previously appointed:
F. J. Walls, *Chairman*
S. V. Wood
Max Kuniansky

International Relations Committee—The previous Chairman, F. G. Steinbach, having asked to be relieved, a new Chairman was appointed. The Mexico representative was dropped in view of the new Chapter status. The European, Brazilian and Australian representatives were reappointed, at least until the International Committee of Foundry Technical Associations is once more active, when it may be desirable to appoint a representative from each country on the I.C.F.T.A. Tentative personnel:
A.F.A., *Chairman*, W. W. Maloney
European Representative, Vincent Delport
Brazilian Representative, Miguel Siegel
Australian Representative, W. A. Gibson

U. S. Ordnance Committee—Technical Director S. C. Massari appointed as Chairman *pro tem*. Balance of committee to be appointed as circumstances require.

Technical Correlations Committee—This committee, inactive for several years since the TDP Advisory Committee was established, reappointed by President Wood in accordance with recent reorganizational plan for A.F.A. Technical Committees, approved by the Board in July, with one exception. Although the reorganizational plan calls for the Vice-President of A.F.A. to serve as Chairman, on motion by Director Sully, seconded by Director Smith, it was decided

THAT the immediate past President of A.F.A. serve as Chairman of the Technical Correlations Committee, together with the current Vice-President of A.F.A.

TDP Advisory Committee—Not reappointed, its functions being made a part of the reorganized Technical Correlations Committee.

Youth Encouragement Committee—Not reappointed, its activities being covered in the Educational Division.

Management Committee—Not reappointed, its present functions being incorporated into the Chapter Contacts Committee.

Chapter Contacts Committee—Reappointed with the entire Board of Directors acting as Committee members, the Vice-President of A.F.A. serving as Chairman. President Wood urged every Director to visit as many Chapters as possible during the current year.

AMERICAN FOUNDRYMAN Policy Committee—Not reappointed for the present, since functions of previous Policy Com-

mittees dealt largely with the question of regular advertising, now an accomplished fact.

Motion was made by Director Walls, seconded by Director Sully, and unanimously carried

THAT the 1946-47 Standing and Appointive Committees of the Board be approved as offered by the President.

1946–47 Nominating Committee

The personnel of the 1947 Nominating Committee, having been previously selected in accordance with the By-Laws by the Executive Committee of the Board, was announced, with Geographical and Industrial representation, as follows:

Chairman, Past President F. J. Walls, District Manager, International Nickel Co., Detroit.

Past President R. J. Teetor, Cadillac Malleable Iron Co., Cadillac, Mich.

R. S. Davis, Manager, National Malleable & Steel Castings Co., Indianapolis—(Central Indiana Chapter) rep. Malleable and Steel.

N. J. Dunbeck, Vice-President, Eastern Clay Products, Inc., Jackson, Ohio—(Central Ohio Chapter) rep. Supplies.

J. J. McFadyen, Supt., Galt Malleable Iron Co., Ltd., Galt, Ont., Canada—(Ontario Chapter) rep. Malleable.

E. C. Troy, Foundry Engr., Dodge Steel Co., Philadelphia—(Philadelphia Chapter) rep. Steel.

M. E. Brooks, Foundry Engr., Magnesium Div., Dow Chemical Co., Bay City, Mich.—(Saginaw Valley Chapter) rep. Light Metals.

Harry E. Ladwig, Asst. Gen. Supt., Allis Chalmers Mfg. Co., West Allis, Wis.—(Wisconsin Chapter) rep. Gray Iron.

R. J. Harrington, Asst. Works Mgr., Hunt-Spiller Mfg. Corp., Boston—(Outside Chapter) rep. Gray Iron.

President Wood stated that since the Committee had been chosen by letter ballot, it was desirable that the Executive Committee take action in meeting to approve. Motion was made by Director Smith, seconded by Director Wilson, and unanimously carried

THAT the Nominating Committee for 1947, as announced, be approved by the Executive Committee.

Membership Report

The Secretary-Treasurer presented a Membership report showing total membership on October 31, 1946, of 9,021; 8,792 on September 30, and 8,539 on June 30.

Financial Report

The Secretary-Treasurer presented Financial reports, showing the condition of the Association at the end of the first quarter, September 30, including the Cupola Research fund.

Discussion followed with reference to financial and administrative matters and it was understood that henceforth all expense items would be allotted to the various A.F.A. activities in rendering financial reports. Director Smith suggested that the original authorizations and obligations involved in each special fund of A.F.A. be investigated for discussion at the mid-year Board meeting.

National Castings Council

President Wood reported on the organization Meeting of the National Castings Council and stated that a fourth meeting was scheduled for November 8, 1946, making it impossible for him to attend. He suggested, and the members of the Executive Committee individual concurred, that A.F.A. express its willingness to cooperate with the council, but not to participate financially at this time.

It being learned that former A.F.A. Director I. R. Wagner, was about to become President of National Founders Association, it was agreed that President Wood would confer with Mr. Wagner at the earliest opportunity in order to establish with the Council still further the A.F.A. position with reference to participation in Council activities.

1947 Convention Program

The Secretary presented a published report on the 1947 Convention of A.F.A. to be held in Detroit, April 28-May 1. He presented, for tacit approval of the Executive Committee, an outline program of sessions. Discussion followed as to speakers for the Opening Day session, the Alumni Dinner, and the Annual Banquet. It was agreed that a Chapter Officers and Directors Dinner should be included and considerable attention given this event, with the entire Board of Directors of A.F.A. attending, as well as Division Chairman. The Secretary was instructed to submit a program at the mid-year Board meeting in January.

It was agreed to again hold a Canadian Luncheon for A.F.A. members in Canada. Other details of the program were considered generally.

Joint Educational Committee

The Secretary called attention to a suggestion made by the Joint Educational Committee that A.F.A. participate financially in a $60,000 Scholarship Fund. This matter was considered at the Annual Board meeting in July 1946 and referred back for Staff recommendations. Technical Director Massari recommended that A.F.A. cooperate with the program without financial participation, in view of the feeling that A.F.A. could not sponsor specific scholarships in a certain few schools against all others.

It was the consensus that scholarships could best be sponsored by individual companies and it was suggested that A.F.A., if it should desire to play an active part in providing scholarships, should promulgate the matter through the Chapters and at the Chapter Chairman Conference.

The Secretary was authorized to inform the Joint Educational Committee that A.F.A. could not financially participate in the Joint Scholarship Fund.

Safety and Hygiene

Considerable discussion arose on the question of whether or not A.F.A. should at this time embark on a full-fledged Safety and Hygiene Program with a permanent staff chairman. The Staff pointed out the desirability of re-forming the Safety and Hygiene Committee of A.F.A., not reappointed since resignation of the former Chairman, J. R. Allan. It was pointed out, however, that a proper Safety and Hygiene program might involve an annual expenditure of between $25,000 and $35,000, and that this could not come out of current operating funds. Rather, it was felt necessary to obtain the cooperation and financial interest of all foundry management to make such a program successful, and it was the consensus of the Committee that such cooperation was not in sufficient evidence as yet to warrant announcement of a full-fledged Safety and Hygiene Program.

Instead, a motion was made by Director Smith, seconded by Director Sully, and carried

THAT AMERICAN FOUNDRYMAN be used as a medium for stimulating interest in Safety and Hygiene work and that the Staff take immediate steps to obtain papers for this purpose.

It was agreed that the Staff might, if necessary, retain someone for this purpose. It was also agreed that the Staff might well commence a search for a man to prepare a sound Safety and Hygiene program, to eventually head up the program as a member of the Staff.

President's West Coast Trip

President Wood and the Secretary presented separate reports on their West Coast trip, September 28-October 22, during the course of which they visited Seattle, Portland, San Francisco and Los Angeles. They reported that at Seattle they attended a meeting which resulted in the signing of a petition for a new Washington Chapter, the 34th Chapter of A.F.A.

President Wood emphasized the point that West Coast foundrymen are very conscious of their distance from the eastern foundry centers and he urged future A.F.A. presidents to maintain closer contact with the Pacific Coast Chapters.

New Washington Chapter

The Secretary pointed out that a petition for a new Washington Chapter of A.F.A. had been forwarded to the Board of Directors for letter-ballot approval and requested the Directors present to complete the balloting at this time. He stated that all ballots received had expressed approval of the petition and that the Washington Chapter officers would be promptly notified so that arrangements could be made for an official installation meeting in the near future.

On motion duly made, seconded and carried, the petition for a new Washington Chapter of A.F.A. was approved unanimously.

By-Laws Interpretations

The Staff requested interpretation of two By-Laws referring to (1) the holding of more than one membership by a single individual, and (2) factors governing the acceptance of membership applications.

(a). In view of the fact that membership in A.F.A. is National membership rather than membership in a local Chapter alone, a motion was made by Director Wilson, seconded by Director Sully, and carried

THAT no individual shall be permitted to hold more than one membership in A.F.A. in his own name, until otherwise decided.

(2) It was pointed out that, in accordance with suggestion by the Board and as stated at the Chapter Chairman Conference of 1946, all applications for membership involving Chapter affiliation are first referred to the Chapter Membership Committees for approval before acceptance. It was the consensus that the primary consideration in approving membership applications should be the interest of the applicant in the objectives of A.F.A. A motion was made by Director Walls, seconded by Director Wilson, and carried

THAT the Secretary-Treasurer be instructed to clarify the legal aspects involved in membership applications, for submission to the Board at a later meeting.

Official Visit to Mexico City Chapter

The Secretary called attention to the desirability of arranging an official visit to the Mexico City Chapter for the first time and suggested that R. L. Lee of General Motors Corp., Detroit, who speaks Spanish, might be asked to make the trip officially for the Association. The Secretary was authorized to make such arrangements for a visit at the earliest possible date.

There being no further business, the meeting was adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
January 7, 1947

Minutes
Meeting 1946-47 Executive Committee
Palmer House, Chicago—January 22, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: J. H. Smith
Joseph Sully
F. J. Walls
L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Reading and Approval of Minutes

Minutes of the Executive Committee Meeting held in Chicago, November 6-7, 1946, were read and approved.

(NOTE: All actions of this Executive Committee meeting, being subject to approval by the Board of Directors, are included in the Minutes of the Mid-Year Board meeting held January 23, 1947, and are shown therein as Recommendations from this Executive Committee meeting).

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
March 1, 1947

Minutes
Mid-Year Meeting 1946-47 Board of Directors
Palmer House, Chicago—January 23, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: H. A. Deane, F. J. Dost, G. K. Dreher, E. W. Horlebein, H. H. Judson, J. E. Kolb, H. G. Lamker, B. L. Simpson, J. H. Smith, F. J. Walls, S. C. Wasson, L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari
Absent: Directors: S. D. Russell, R. T. Rycroft, Joseph Sully, F. M. Wittlinger

Reading and Approval of Minutes

Minutes of the Annual Board Meeting held July 26, 1946, and Minutes of the Executive Committee Meeting held November 6-7, 1946, were read and approved.

Minutes of the Executive Committee Meeting held January 22, 1947 were read in outline as to motions, due to lack of time in preparing official Minutes. Approval deferred to next Board Meeting.

Membership Report

The Secretary presented a report on Membership covering the 6-months period ending Deccember 31, 1946, showing total membership of 8814, compared with 8539 on June 30, 1946. New members added, 1004; members dropped due to cancellations, death, and delinquency, 731; net gain, 275. Total membership in all chapters, 7,939 or 90%; non-chapters, 509; foreign, 366.

Foreign Memberships. Discussion took place on the matter of accepting foreign memberships, and the dues rates for same, it being pointed out that the A.F.A. By-Laws make no distinction between foreign and domestic memberships insofar as membership classifications or dues are concerned. It was the consensus of the Committee, without vote, that foreign memberships be encouraged with no change in dues thereof for the present.

Financial Report

A Financial Report for the 6-months period ending December 31, 1946, showed total Income of $233,619.85, and total Expenses of $154,268.16. In accordance with request of the Executive Committee, the Financial Report was also presented with all

overhead distributed to major activities as follows:

INCOME	
Membership Dues	$153,662.32
Publications	11,347.28
AMERICAN FOUNDRYMAN	35,735.25
Miscellaneous	75.00
Surplus	32,800.00
Total	$233,619.85
EXPENSE	
General Administration	$ 47,873.79
Chapter Activities	24,843.44
Publications	14,334.20
Technical Committees	17,584.08
AMERICAN FOUNDRYMAN	43,989.05
Annual Meetings	5,643.60
Total	$154,268.16

Statement of Comparative Cash Balance as of December 31, 1946, showed a total of $163,445.52 in cash, in all funds of the Association. The Investment Record for December 31, 1946, showed a total of $181,600.00 in all investment funds of the Association.

Financial Position as of December 31, 1946

Fund	Cash	Investments
Checking Account	$126,508.89	
General Reserve Fund	15,984.01	$ 35,600.00
Technical Development Fund	5,136.28	100,000.00
Cupola Research Fund	12,525.65	20,000.00
Award Funds	3,290.69	26,000.00
TOTAL	$163,445.52	$181,600.00

AMERICAN FOUNDRYMAN. The Secretary reported on editorial and advertising progress of AMERICAN FOUNDRYMAN during the current fiscal year. The advertising report showed a gross income of $31,114.12 for the period July 1-December 31, 1946, compared with $44,005.01 for the same period a year ago.

Chapter Organization

The Secretary reported on the organization of new Chapters and announced:

(1) Installation of the Washington Chapter at Seattle on December 20, 1946.

(2) Installation of the Rocky Mountain Empire Chapter at Denver, Colo. on January 14.

(3) The Secretary stated that a petition had been received for a 36th Chapter, the Tri-State Chapter, with headquarters at Tulsa, Okla., and recommended that the petition be approved by the Board. A motion was made by Director Horlebein, seconded by Director Dreher, and unanimously carried

THAT the petition of the Tri-State Chapter of A.F.A. be accepted and the Chapter Chairman notified.

Chapter Policy

Considerable discussion took place concerning the continued growth of A.F.A., the admission of additional Chapters, and the formulation of some form of National Policy with regard to relationship between the National organization and the Chapters. Motion was made by Director Simpson, seconded by Director Horlebein, and unanimously carried

THAT the Executive Committee consider formulation of a National policy on Chapter formation and coordination, for submission to the Board of Directors.

1947 Convention Progress

The Secretary and Technical Director reported on progress of the 1947 Convention Program and discussed a Tentative Program, already announced to the membership. The Technical Director developed information on the Technical Program, preprinting of all papers prior to the Meeting, and Committee Meetings.

In connection with the Chapter Officers and Directors Dinner to be held Tuesday, April 29, during the Convention, it was the consensus of the Committee that President Wood act as Chairman and the Vice-President Kuniansky be the main speaker, so as to leave with all who attend an inspirational and informative message. It was agreed that all National Officers and Directors and Division Chairmen of A.F.A. should be invited and urged to attend.

National Castings Council

President Wood reported briefly on a meeting of the National Castings Council held in Cleveland, January 17. He expressed satisfaction over the degree of cooperation present and stated that no permanent officers had as yet been elected by the Council. He expressed the thought that a prime purpose of the Council may be the discussion of industry-wide problems that may arise in the future.

Recommendations of Executive Committee Meeting November 6-7, 1946

(a) *Allocation of A.F.A. Securities.* The Secretary-Treasurer presented, as approved by the Executive Committee, an allocation of all A.F.A.-held securities, showing allocation of each security to a particular fund of the Association. Motion was made, duly seconded and carried, accepting the Executive Recommendation

THAT the allocation of A.F.A. fund securities be approved as submitted.

Following brief discussion, it was the consensus of the Board that A.F.A. should continue to purchase U. S. Government securities only, in investing Association funds. It was pointed out that all security purchases by the Secretary-Treasurer are first approved by the Finance Committee as to amounts, type of securities, purchase price, etc.

(b) *Contracts for Research Work.* Technical Director Massari outlined the terms of a new contract drawn up between A.F.A. and Cornell University in connection with the A.F.A. Sand Research Fellowship at that school. This contract makes mandatory the preparation of reports at regular intervals and provides for such contingencies as patent rights, etc. On motion duly made, seconded and carried, accepting Executive Committee recommendation that the Secretary be authorized to negotiate the contract with Cornell as outlined.

A further motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT the Secretary be authorized to sign contracts for A.F.A.-sponsored research, following approval of projects involved by the Executive Committee of the Board.

(c) *TDP Advisory Committee.* It was pointed out that reestablishment of the Technical Correlations Committee had made unnecessary reappointment of the TDP Advisory Committee, since the functions of the latter can and should be even broader than those of the former. It was pointed out, however, that since the TDP Advisory Committee was set up by action of the Board, Board action should be required for its discontinuance. Accordingly, motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT the TDP Advisory Committee be suspended as a separate body and its functions be incorporated into those of the Technical Correlations Committee.

(d) *Expanded Publication Policy.* The Secretary and Technical Director presented, as recommended by the Executive Committee, an expanded publication policy for A.F.A., involving the following:

(a) Setting up Editorial Committees in each A.F.A. Division to seek out and obtain more original and more practical articles for AMERICAN FOUNDRYMAN.

(b) Publication, in general, of TRANSACTIONS material in the Annual Bound Volume of TRANSACTIONS alone.

(c) Issuance of the Annual Bound Volume of TRANSACTIONS gratis to each member under paper covers in order to fulfill the obligations of providing each member with complete Convention Transactions.

Following discussion, a motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT the expanded publication policy, as outlined, be accepted and put into effect as promptly as the Staff deems feasible.

(e) *Staff Retirement Plan.* The President called attention to the need, as recommended by the Executive Committee, for setting up a definite Retirement Plan for members of the National

Office Staff, and asked authority of the Board to appoint a committee to this end. After discussion, motion was made, duly seconded and unanimously carried, accepting Executive Committee recommendation

THAT the President be authorized to appoint a committee on retirement plans for presenting a definite retirement plan for members of the National Office Staff, for presentation to the Board of Directors at the earliest possible date.

President Wood then announced the appointment of the Committee on Retirement Plans, as follows: B. L. Simpson, Chairman; W. B. Wallis; D. P. Forbes; and S. C. Wasson. Director Simpson, as Chairman, reported the intention of his Committee to meet soon for presentation of recommendations to a Board meeting in the near future.

(f) *Membership in A.S.A.* The question of A.F.A. membership in American Standards Association was discussed, membership having been previously authorized at the Annual Board meeting in July. Following discussion, motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT A.F.A. become a member body of A.S.A. with annual dues of $500.00 per year, including, for the time being, one representative on the Standards Council, and that the membership be exercised by Technical Director S. C. Massari.

(g) *Board of Awards Procedures.* The Secretary-Treasurer called attention to the fact that the 1946 Board of Awards had recommended to the Board of Directors that consideration be given the setting up of some form of recognition for company organizations, for their technical contributions and extreme willingness to cooperate in the purposes of A.F.A. This matter was discussed at the Annual Board meeting in July 1946, but no decision reached.

Discussion brought out the point that to distinguish between a company or a personal contribution of a technological nature would be extremely difficult, and that several A.F.A. Gold Medals have already been awarded to individuals on behalf of their companies. Accordingly, motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT no change be made in the Board of Awards procedures at this time.

(h) *Annual Lecture Proposals.* It was brought out that the 1946 Annual Lecture Committee had recommended to the Board (a) a change in the name of the Foundation Lecture, and (b) setting up some form of emolument for the Annual Lecturer. The recommendations, considered at the Annual Board meeting in July 1946, were referred back to the Lecture Committee for more specific recommendations.

In the course of discussion it was suggested that the Secretary might recommend that the Lecture Committee consider having the lecture presented only on alternate years.

After further discussion, a motion was made, duly seconded and carried, accepting Executive Committeee recommendation

THAT the name of the Foundation Lecture be changed to that of the Charles Edgar Hoyt Annual Lecture, and the Annual Lecture Committee be authorized to present to the Annual Lecturer some suitable form of recognition, as well as an appropriate scroll.

Recommendations of Executive Committee Meeting January 22, 1947

(a) *Apprentice Winners.* A recomomendation of the Executive Committee as presented dealing with a plan to bring the 1st-Prize-Winning Apprentices to the 1947 Convention. Motion was made by Director Dost, seconded by Director Horlebein, and carried, accepting the Executive Committee recommendation

THAT A.F.A. assume the cost of rail and pullman fare to and from the Detroit Convention for the 1st Prize Winner in each of the four 1947 Apprentice Contest divisions; that Apprentice expenses at the Convention be assumed either by the winner, his company, or his Chapter; and that no provision be made for the expense of other Apprentice Contest entrants.

(b) *Registration Fees.* A recommendation of the Executive Committee on Registration Fees for the 1947 Convention was presented and discussed. Motion was made by Director Horlebein, seconded by Director Dreher, and carried, accepting the Executive Committee recommendation

THAT a non-member registration fee of $2.00 be charged at the 1947 Convention, this fee to be applicable against membership dues either during the Convention or within a definite period of time thereafter; that registering non-members be followed up with membership solicitation following the Convention through the Chapter Membership Committees; and that no gratis distribution of preprints be made at the Convention.

(c) *Convention Entertainment Policy.* Recommendation of the Executive Committee was presented, that the A.F.A. policy prohibiting the use of public space for entertainment purposes, as enforced at the 1946 Convention, be modified. Considerable discussion arose on the workability of such a policy, Association obligations to the industry, past precedent and experience, and the like. It was pointed out that the discussion revolved solely around the use of public space and not private rooms and suites. It was also pointed out that entertainment, as a whole, should be discouraged during the hours of meetings and other official events, the feeling being expressed that the question of hours might be regulated through hotel cooperation.

President Wood recalled an announcement and resolution prepared in 1929 dealing with the 1930 Convention of A.F.A., which read as follows:

"The following resolutions, unanimously adopted by the Board of Directors, have been unanimously approved by the Foundry Equipment Manufacturers' Association and the Foundry Supply Manufacturers' Association. Furthermore, all Cleveland hotels have pledged their cooperation.

" 'WHEREAS, the practice of lavish, unwarranted and wholesale entertainment in hotel rooms and elsewhere has been indulged in by certain exhibitors and others attending conventions of the American Foundrymen's Association, resulting in conditions inimical to the best interests of said Association, and

" 'WHEREAS, such practices detract from and tend to defeat the purposes of such conventions, reflect discredit on the men of the industry and discourage companies from sending their representatives to such conventions, and

" 'WHEREAS, such practices result in excessive, unnecessary and unwarranted expense, contrary to good business ethics and practices: now therefore be it

" 'RESOLVED, by the Board of Directors of the American Foundrymen's Association at the annual meeting held in the City of Cleveland, Ohio, September 12, 1929, that exhibitors and all other interests concerned be requested to cooperate in the abatement of such practices, and that the Manager of Exhibits and the Committee on Convention Arrangements be and they hereby are authorized and directed to take such action as in their judgment may be expedient or necessary to abate such practices.' "

"The cooperation of all visitors is earnestly desired in carrying out the spirit of these resolutions."

It was further pointed out that the 1947 agreement between A.F.A. and the Detroit Hotels contains the following clause: "No public space will be rented or permitted to be used for purposes of entertainment or meetings during the Convention period without the written approval of A.F.A."

At the conclusion of the discussion, a motion was made by Vice-President Kuniansky, seconded by Director Judson, and carried

THAT the "no public space for entertainment" clause in the 1947 A.F.A.-Hotel agreement be revised to permit the use of public space for entertainment purposes between the hours of 5:00-7:00 pm only, on approval of A.F.A.; that the Foundry Equipment Manufacturers' Association and the Foundry Supply Manufacturers' Association be sent a copy of the 1929 resolution on entertainment together with official notification as to revision of the A.F.A. policy, and that this revised entertainment policy be properly publicized for cooperation of all parties interested.

(d) *Joint Educational Committee.* Recommendation of the Executive Committee was presented, suggesting A.F.A. endorsement of an educational program sponsored by the Joint Ferrous Foundry Committee composed of the Malleable Founders Society and the Gray Iron Founders Society. This program involves the financing of both scholarships and facilities for better foundry instruction at five schools: Case School of Applied Science, Cleveland; University of Cincinnati, Cincinnati; Cornell University, Ithaca, N.Y.; Massachusetts Institute of Technology, Cambridge, Miss.; and the University of Wisconsin, Madison, Wis. Motion was made by Director Horlebein, seconded by Director Simpson, and carried, accepting the recommendation of the Executive Committee

THAT the A.F.A. Board of Directors heartily endorses the educational program of the Joint Ferrous Foundry Committee, and that the Committee be so notified of A.F.A. desire to assist, without financial participation.

(e) *Safety and Hygiene.* Recommendation of the Executive Committee was presented involving A.F.A. cooperation with the announced intention of the U. S. Public Health Service to survey health and sanitation conditions in the foundry industry. Following discussion, motion was made by Director Simpson, seconded by Director Judson, and carried for the record

THAT A.F.A. cooperate within the limitations of Association policy.

(f) *Staff Recommendations.* Recommendations of the Executive Committee, authorizing salary increases for two Staff members, effective January 1, 1947, were presented. On motion by Director Simpson, seconded by Director Wasson, and carried, recommendations approved.

(g) *National Office Headquarters.* The Secretary announced completion of plans for moving the National Office Headquarters into new, enlarged and more efficient quarters by about February 15, and requested authorization for remodeling expense involved. Motion was made, duly seconded and carried, accepting the recommendation of the Executive Committee

THAT approximately $5,000.00 be authorized for alterations necessary to efficient occupancy of new National Office Headquarters.

(h) *Resignation of N. F. Hindle.* President Wood read a letter from former Technical Director N. F. Hindle, resigning from the A.F.A. Staff as of February 1, 1947, in order to accept a position as Assistant Professor of Mechanical Engineering at the University of Idaho, Moscow, Idaho. In this letter, Norm Hindle thanked the Board of Directors for its consideration in granting a leave of absence since February 1946, and expressed regrets over his inability to return to active work in A.F.A. The Board, in turn, expressed its regret over circumstances impelling Norm Hindle's decision, and motion was made, duly seconded and carried, approving the recommendation of the Executive Committee

THAT the resignation of N. F. Hindle from the National Office Staff, be accepted as of February 1, 1947.

(i) *1947 Chapter Chairman Conference.* Recommendations of the Executive Committee pertaining to attendance at the 1947 Chapter Chairman Conference were presented and discussed. Motion was made by Director Deane, seconded by Director Wasson, and carried, accepting these recommendations

THAT invitations to the 1947 Chapter Chairman Conference be extended all newly elected Chapter Chairmen at A.F.A. expense; and that the attendance of any one Chapter be limited to three Officers.

(j) *A.F.A. Research Projects.* (1) Recommendation of Executive Committee was discussed by Technical Director Massari, requesting approval for an expenditure of approximately $215.00 over the original appropriation of $1400.00 for the current fiscal year for heat transfer tests at Battelle Memorial Institute, Columbus, Ohio.

(2) Recommendation of Executive Committee was discussed by the Technical Director, requesting that an additional $2,000.00 be authorized over the original appropriation of $2,000.00 for the current fiscal year, for heat transfer investigations at Columbia University, New York. He stated that all of the entire additional amount should be made available but that it probably would not be entirely utilized.

(3) Recommendation of the Executive Committee was discussed by the Technical Director, requesting authorization for an expenditure not to exceed $5,000.60 during the fiscal year 1947-48, for an A.F.A.-sponsored research on behalf of a regularly submitted project of the A.F.A. Aluminum and Magnesium Division. He stated that this was the first Divisional research project thus far received under the authorization for such projects given by the Board of Directors at the Annual Board Meeting, July 1946.

Following discussion by the Board, motion was made by Director Horlebein, seconded by Director Simpson, and carried, accepting the above three recommendations of the Executive Committee on A.F.A.-sponsored research.

(k) *National Machine Tool Show.* The Board, on motion by Director Wasson, seconded by Director Dost, and carried, accepted the recommendation of the Executive Committee

THAT A.F.A. endeavor to participate in the 1947 Machine Tool Show, September 16-26, with a Technical Program, Technical Director Massari to be in charge of such participation, to be worked out with the National Machine Tool Builders Association.

Recommendation of the Executive Committee was presented, seeking approval for holding the 1948 Convention and Exhibit at Philadelphia at the Commercial Museum and Convention Hall. Motion was made by Director Horlebein, seconded by Director Simpson, and carried, approving the recommendation of the Executive Committee

THAT Philadelphia be selected for the 1948 Convention and Exhibit of A.F.A. and that the Staff be authorized to make necessary commitments to that end.

(l) *Future Convention Cities.* The Secretary discussed informally the selection of future Convention cities, emphasizing the importance of selecting cities with adequate housing facilities as well as meeting rooms and exhibit hall facilities. In connection with 1948, an exhibit year, he stated that the facilities of Chicago, Atlantic City and Philadelphia had been looked into, and recommended that the 1948 Convention and Exhibit be held at the Philadelphia Commercial Museum and Convention Hall.

The last previous A.F.A. meeting in Philadelphia was in 1934, Chicago in 1940, and Atlantic City in 1915. He recommended that Chicago be withheld until a later date and pointed out that Atlantic City would not provide facilities in any form for plant visitations within reasonable distances.

Recommendations of 1946-47 Board of Awards

(a) *Medal Awards.* Director Walls, as Chairman of the 1947 Board of Awards, presented the following recommendations for approval by the Board of Directors:

(1) THAT the Joseph S. Seaman Gold Medal of A.F.A. be awarded to Henry S. Washburn, Pres., Plainville Casting Co., Plainville, Conn., for outstanding service to the Association and valuable contributions to the gray iron casting industry.

(2) THAT the John A. Penton Gold Medal of A.F.A. be awarded to Russell J. Allen, Metallurgical Engr., Worthington Pump & Machinery Corp., Harrison, N.J., for his earnest and unceasing contributions toward the advancement of gray iron technology.

(3) THAT the Wm. H. McFadden Gold Medal of A.F.A. be awarded to Harry Mark St. John, Supt., Brass Foundry & Forge Shop, Crane Co., Chicago, for outstanding work in the field of non-ferrous casting research over a period of many years.

(4) THAT the Peter L. Simpson Gold Medal of A.F.A. be awarded to Richard Aloysius Flinn, Jr., Metallurgist, American Brake Shoe Co., Mahwah, N.J., for outstanding work during the past year in the field of chilled and white iron castings.

On motion by Director Horlebein, seconded by Director Simpson, the recommendations of the Board of Awards on 1947 Medalists were unanimously approved.

(b) *Honorary Life Membership Awards.* Chairman Walls presented the recommendations of the Board of Awards on Honorary Life Memberships as follows:

(1) THAT John Grennan, Foundry Practice Instructor, University of Michigan, Ann Arbor, Mich., be made an Honorary Life Member of A.F.A. for his long service to the Association, collaboration in technical papers of value to the industry, and his constant encouragement of young men toward the castings industry.

(2) THAT Sheldon V. Wood, Pres., Minneapolis Electric Steel Castings Co., Minneapolis, be made an Honorary Life Member of A.F.A. on his retirement from the A.F.A. Presidency.

On motion by Director Horlebein, seconded by Director Simpson, the recommendations of the Board of Awards on Honorary Life Memberships were unanimously approved.

(c) *Apprentice Prize Funds.* Chairman Walls, for the Board of Awards, reviewed the question of funds for the Apprentice Contest Awards, pointing out that by action of the Board of Directors, these prizes have now been increased from $30 to $100 for 1st Prize, from $20 to $50 for 2d Price, and from $10 to $25 for 3d Prize in each of the four Contest Divisions. Thus, the Apprentice Awards now total $700 annually instead of $240. The Chairman also expressed inquiry as to the availability of the Obermayer Fund for apprentice awards. The Board of Awards recommended that the Apprentice Contest Funds henceforth be drawn from the General Fund of the Association rather than from Award Funds, and that the Obermayer Fund be examined as to availability for such purposes.

On motion by Director Judson, seconded by Director Dreher, the recommendation of the Board of Awards was approved.

(d) *Annual Lecture Funds.* Recommendation of the Executive Committee was presented, authorizing, as recommended by the 1946 Annual Lecture Committee, (a) a change in the name of the "Foundation Lecture," and (b) setting up some form of suitable recognition for the Annual Lecturer. Motion made, seconded and carried, accepting Executive Committee recommendation

> THAT the name of the Foundation Lecture be changed to the Charles Edgar Hoyt Annual Lecture, and the Annual Lecture Committee be authorized to present to the Annual Lecturer some suitable form of recognition, as well as an appropriate scroll.

Chairman Walls, for the Board of Awards, in referring to the above action, presented a recommendation from the Board of Awards that the expense of recognizing the Annual Lecturer be paid from General Funds of the Association, rather than from Award Funds. Motion was made, duly seconded and carried, accepting the recommendation.

(e) *General Recommendations.* (1) The Board of Awards reaffirmed its 1946 recommendation that the National Office compile complete information on men of the industry who, by reason of their work and accomplishments, might be considered by the Board of Awards for Association honors.

(2) The Board of Awards recommended that the original contracts covering the five award endowments be completely reviewed for the purpose of setting up more definite yardsticks for judging the merits of individual candidates for Association honors.

SAE Cooperation

The Technical Director presented a request from the Society of Automotive Engineers for data on the fundamentals of castings design for inclusion in the Society's Handbook. It being the sense of the Board that A.F.A. should accept this opportunity, the Technical Director agreed to prepare such material at an early date.

Report of 1946-47 Nominating Committee

As Chairman of the 1947 Nominating Committee, Director Walls presented the following slate of Nominees for election at the 1947 Convention:

For President (to serve 1 year) Max Kuniansky, Vice-President and General Manager, Lynchburg Foundry Co., Lynchburg, Va.

For Vice President (to serve 1 year) W. B. Wallis, President, Pittsburgh Lectromelt Furnace Corp., Pittsburgh, Pa.

For Directors (to serve 3 years each) —

E. N. Delahunt, Superintendent, Warden King Ltd., Montreal, Quebec, Canada.

W. J. MacNeill, General Manager, G.H.R. Foundry Co., Dayton, Ohio.

R. H. McCarroll, Chief Metallurgist, Ford Motor Co., Dearborn, Mich.

John M. Robb, Jr., Resident Manager, Hickman, Williams & Co., Inc., Philadelphia.

A. C. Ziebell, President, Universal Foundry Co., Oshkosh, Wis.

(Note: Retiring President S. V. Wood also serves on the Board of Directors for one year, in accordance with the By-Laws).

No action being required of the Board of Directors on selections of the Nominating Committee, the Board expressed to Chairman Walls its congratulations on selecting a slate of Officers and Directors who might be expected to progress the work of the Association.

Chairman Walls informally recommended that the A.F.A. Staff take definite steps to institute a "Who's Who" of the foundry industry to the end of facilitating Nominating Committee and Board of Awards deliberations in the selection of men who have shown their qualifications for National Office by their A.F.A. activities and spirit of cooperative enterprise.

There being no further business, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
March 1, 1947

Minutes
Meeting 1946-47 Executive Committee
Palmer House, Chicago—March 19, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: J. H. Smith
Joseph Sully
F. J. Walls
L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Reading and Approval of Minutes

Minutes of the Executive Committee Meeting, January 22, 1947, and Minutes of the Board of Directors Meeting, January 23, 1947, were read and approved.

Membership Report

The Secretary presented a Membership report showing a total membership of 9142 on February 28, 1947, compared with 8539 on June 30, 1946, an increase of 603. He stated that during the month of February, 169 new members had been added.

Some discussion took place on means of obtaining the attendance of more operating men at Chapter meetings, with some

indication that a new type of Regional Conference might be arranged, on a national level, in certain foundry areas.

Financial Report

The Treasurer submitted a Financial Report for the eight months July 1, 1946 to February 28, 1947, showing a total income of $258,473.65, compared with the budget of $349,000.000 for the entire 1946-47 fiscal year. The statement also showed total expenses of $222,999.84. A breakdown of Expense distributed to the major activities of A.F.A. was presented.

Chapter Organization

The Secretary reported on the following new Chapter organizations and possibilities:

(1) Installation of Chapter No. 36, the Tri-State Chapter, with headquarters at Tulsa, Okla., on February 28.

(2) The probability of organization of a new chapter at Marshall, Mich., including the Marshall-Kalamazoo-Lansing Area. He stated that after inquiries and correspondence during the past year, a meeting was held in Marshall in February with 35 foundrymen in attendance. A Steering Committee was formed and another meeting was held on March 15 with 50 present and that the Steering Committee had been authorized to prepare a petition for formation of a Central Michigan Chapter. He stated that the five chapters surrounding the proposed Central Michigan Chapter—Western Michigan, Michiana, Detroit, Saginaw Valley, and Toledo Chapters had been invited to send representatives to an open meeting to be held in Marshall, March 24.

(3) The Secretary reported a definite movement under way in the Vancouver, B.C. area for the formation of a Chapter, and a similar movement in the Kansas City area.

1947 Convention Program

The Secretary and Technical Director gave progress reports on the 1947 Convention Program. The Technical Director reported that about 53 papers had been approved by the Committees for presentation during the Convention and that all papers to be preprinted were in process.

The Secretary reported that Mr. John H. Van Deventer, Director of Information of CED, had accepted an invitation to be the speaker at the Alumni Dinner.

Safety and Hygiene

The Technical Director gave a comprehensive report on a recent meeting with Dr. Castberg and Mr. Berry of the U. S. Public Health Service, and discussions with them on the degree of A.F.A. cooperation possible. He reported that the Health Service, planning a comprehensive survey of foundries as to heat, dust, lighting and ventilation conditions, had agreed to submit to A.F.A. progressive reports as developed. It was the consensus of the Executive Committee that A.F.A. should continue working as closely as possible with the Health Service in this Survey.

Committee on Retirement Plans

President Wood stated that the Committee on Retirement Plans had submitted a report to him but that, due to lack of time, the report had not been transcribed for submission to the Executive Committee. He stated that each member of the Executive Committee would receive a copy of this report prior to the June meeting, for study and consideration at that time.

National Machine Tool Show

Technical Director Massari reported on a meeting of the National Machine Tool Builders Association in Cleveland, called for the purpose of arranging the program of the National Machine Tool Show to be held in Chicago, September 16-26, 1947. He stated that several technical societies such as A.F.A., ASME, etc., in Chicago would cooperate in sponsoring a "Technical Day" during the show and that the program for this day was in the state of formation.

AMERICAN FOUNDRYMAN

The Secretary stated that advertising in the Pre- and Post-Convention issues of AMERICAN FOUNDRYMAN was coming in well, and that these issues would be entirely creditable. Announcement was made that the magazine, effective with the February 1947 issue, henceforth would be printed by the Ann Arbor Press, Ann Arbor, Mich., until further notice.

Chapter Chairman Conference

The Secretary reported on correspondence regarding the 1947 Conference, scheduled for July 7-8-9, indicating an intention to present less topics for discussion so as to cover all thoroughly. With regard to extending invitations to the Conference to the Chairmen of Steering Committees of imminent chapters, it was the consensus of the Executive Committee that such invitations should be left to the discretion of the Secretary.

By-Laws Committee

Discussion arose on the non-uniformity of Chapter by-laws. It was agreed by the Committee that there might be incorporated in the National By-Laws some policy regarding Chapter formation but that this recommendation should come from the By-Laws Committee to the Executive Committee, for recommendation to the Board of Directors.

The need for some more definite over-all policy of the Board on organization of new Chapters was discussed at length. It was the consensus of the Executive Committee that some degree of uniformity was necessary among all Chapters and that an organization plan should be discussed with all Chapters. It was pointed out that this could best be done during a session at the 1947 Chapter Chairman Conference and that at that time the Manual for Chapter Officers should be distributed and discussed, pointing out the irregularities which now exist.

Projected A.F.A. Publication

The Secretary read a letter from Honorary Life Member Harold J. Roast of Montreal, announcing his retirement from Canadian Bronze Co., and his intention to write, and offer first to A.F.A. for possible A.F.A. publication a book on "The Bronze Foundry." The Executive Committee requested the Secretary to thank Mr. Roast for his most courteous consideration and to cooperate in every way toward possible A.F.A. publication of his book, subject to review of its contents by the Association's Brass and Bronze Division.

Aluminum and Magnesium Research

The Technical Director reported that, in accordance with action of the Executive Committee at their January 22 meeting, invitations to bid were sent to six educational institutions covering the Aluminum and Magnesium Division Research Project. He stated that work on the project would be assigned on the basis of the bids received, both as to estimated cost and available facilities.

Cooperation with S.A.E.

The Technical Director reported completion of information on castings for the forthcoming S.A.E. Handbook, and its forwarding to the Society.

Cooperation with A.S.M.E.

The Technical Director reported that, in accordance with the request of the A.S.M.E. Metals Engineering Section, a comprehensive manuscript covering the foundry product as an engineering material had been completed and forwarded to the Society for inclusion in its forthcoming A.S.M.E. Handbook.

National Committee Roster

Completion of the National Committee Roster for the current year was reported by the Technical Director. Copies forwarded to each Committee member.

There being no further business, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
April 17, 1947

Minutes
Meeting 1946-47 Executive Committee
Book-Cadillac Hotel, Detroit—April 29, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: J. H. Smith
Joseph Sully
F. J. Walls
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari
Also Present: Vice President-elect W. B. Wallis
Absent: Director L. C. Wilson

Reading and Approval of Minutes

Minutes of the Executive Committee held March 19, having previously been mailed to all members of the Executive Committee, following approval by the President, were approved viva voce.

Convention Program

The Secretary called attention to those items on the Program of the 51st Annual Meeting advisable to be attended by the Officers and Directors. He also reported briefly on progress of the Convention technical meetings.

Membership Report

The Secretary reported that membership in the Association as of March 31 had reached the total of 9341, representing a net gain of 802 members since June 30, 1946. Thus, another new all-time high in membership was recorded.

The Secretary also pointed out that collections of membership dues as of March 31 totaled $167,215.87 against the total budget of $165,000.00 for the current fiscal year. A detailed report of membership for Chapters showed 90 per cent of the entire membership in Chapter areas, plus 474 in non-Chapter areas, plus 410 foreign memberships.

Financial Report

The Secretary-Treasurer reported on A.F.A. finances for the period ended March 31, 1947, showing a total income of $283,943.72, against a total budget of $349,000.00 income for the fiscal year; total expenses of $250,502.53. The Financial Report also contained a breakdown of all expenses, ditsributed to major activities.

EXPENSE

	Budget 7/1/46–6/30/47	Actual, Distributed 7/1/46–3/31/47
General Administrative ...	$ 88,911.00	$ 63,627.64
Chapter Activities	49,998.00	35,821.86
Publications	61,578.00	44,338.95
Technical Committees	45,574.00	32,565.33
AMERICAN FOUNDRYMAN	89,259.00	64,128.65
Annual Meetings	13,680.00	10,020.10
Total	$349,000.00	$250,502.53

Public Health Service Survey

The Secretary reported on cooperation between A.F.A. and the U. S. Public Health Service in connection with the latter's projected survey of health and sanitation conditions in the foundry industry. He stated that, in a conference between representatives of the Health Service and Technical Director Massari, A.F.A. had been assured that the Health Service would discuss with the Association the progress made in its preliminary surveys prior to the making of concentrated surveys in various foundry areas. The opinion was expressed that in this manner A.F.A. might represent the industry in obtaining a fair and impartial analysis of foundry plant conditions.

Foundry Safety Film

A letter from the National Safety Council, proposing A.F.A. participation in the cost of producing a foundry safety film, was presented by the Secretary, indicating a total cost of approximately $5,000.00 for a 15-minute film. The Executive Committee requested that additional details and information be obtained with reference to the specific subject and field to be covered by such film, and suggested that the expenses of any film produced might well be spread more generally than beyond the National Safety Council and A.F.A. alone.

Cooperation with Armed Forces

At the request of the President, Vice-President-elect Wallis reported on recent meetings in Washington pertaining to cooperation of the foundry industry with the Armed Forces and stated that such cooperation, previously under the jurisdiction of the Army Industrial College had been transferred to the Army and Navy Munitions Board. Following discussion, it was agreed that Technical Director Massari would contact the proper parties in Washington for the purpose of a personal visit in order to cement future cooperative relations between A.F.A. and the Armed Forces, for report back to the Board of Directors at the Annual Board Meeting in July.

Committee on Permeability

The Secretary presented a letter from the American Geophysical Union, under the auspices of the National Research Council, Washington, D.C., suggesting representation of A.F.A. on a Committee on Permeability for a study of the flow of fluids through porous solids. Following discussion, it was recommended that Dr. H. Ries of Cornell University and Chairman of the A.F.A. Sand Division, be asked to serve as the official A.F.A. representative on this committee, and the Secretary was instructed to communicate with Dr. Ries accordingly.

There being no further business, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
May 26, 1947

Minutes
Meeting 1946-47 Board of Directors
Book-Cadillac Hotel, Detroit—April 29, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: F. J. Dost, G. K. Dreher, E. W. Horlebein, H. H. Judson, J. E. Kolb, S. D. Russell, B. L. Simpson, J. H. Smith, Joseph Sully, F. J. Walls, S. C. Wasson, F. M. Wittlinger
Secretary-Treasurer Wm. W. Maloney

Also Present:
Vice-President-elect W. B. Wallis

Absent:
Directors: H. A. Deane, H. G. Lamker, R. T. Rycroft, L. C. Wilson

Reading and Approval of Minutes

Minutes of the following meetings of the Board of Directors and Executive Committee, having been mailed to all Directors,

the Secretary was instructed to obtain written approval of the Directors following their individual perusal:

Meeting of Executive Committee, January 22, 1947.

Meeting of Board of Directors, January 23, 1947.

Meeting of Executive Committee, March 19, 1947.

Reading of minutes of the Executive Committee Meeting held in Detroit the morning of April 29, 1947, was deferred for letter-ballot approval.

Membership Report

The Secretary presented the Membership Report covering the first 9 months of the current fiscal year to March 31, 1947, showing a total membership of 9341 against a total of 8539 on June 30, 1946, representing a net gain of 802 members. Membership on February 28, 1947, totaled 9141; resignations during the month of March, 23; new members added during March, 222. Total membership in all Chapters, 8457, or 90%; non-Chapter, 474; foreign, 410.

Financial Report

The Secretary-Treasurer presented a Financial Report for the first 9 months of the current fiscal year to March 31, 1947, showing total income of $283,943.72, total expenses of $250,502.53, against the budget of $349,000.00 income for the current fiscal year. The Financial Report also was presented with the overhead distributed to major activities as follows:

INCOME

	Budget 7/1/46–6/30/47	Actual 7/1/46–3/31/47
Membership Dues	$165,000.00	$167,215.87
Publication: General	7,500.00	4,294.68
Special	40,000.00	23,513.42
Advertising AMERICAN FOUNDRYMAN	100,000.00	54,031.25
A.F.A. Emblems	500.00	88.50
Convention Registration Fee	1,200.00	
Surplus (1945-46 Income)	34,800.00	34,800.00
Total	$349,000.00	$283,943.72

EXPENSE

	Budget 7/1/46–6/30/47	Actual, Distributed 7/1/46–3/31/47
General Administrative	$ 88,911.00	$ 63,627.64
Chapter Activities	49,998.00	35,821.86
Publications	61,578.00	44,338.95
Technical Committees	45,574.00	32,565.33
AMERICAN FOUNDRYMAN	89,259.00	64,128.65
Annual Meetings	13,680.00	10,020.10
Total	$349,000.00	$250,502.53

AMERICAN FOUNDRYMAN

The Secretary reported that advertising revenue in AMERICAN FOUNDRYMAN for the 9-month period ended with the April Pre-Convention issue involved gross billing of $68,370.00 and a net revenue of $59,559.20. He stated that the gross billing for the April Pre-Convention issue was $14,775.00, net billing $12,984.75. Plans for continuously improving the editorial content were discussed briefly by the Directors.

British Columbia Chapter. The Secretary presented a petition for a new A.F.A. Chapter to be known as the British Columbia Chapter, with headquarters at Vancouver, B.C., said petition being signed by 80 individuals representing 40 companies. Acting on the recommendation of the Secretary, a motion was made by Vice-President Kuniansky, seconded by Director Wasson, and unanimously carried

THAT the petition for a British Columbia Chapter be accepted to be designated the 37th Chapter of A.F.A.

Central Michigan Chapter. The Secretary presented a petition for formation of a Central Michigan Chapter in the approximate area of Marshall-Kalamazoo-Battle Creek-Jackson-Lansing, with headquarters at Marshall, Mich., this petition being signed by 66 individuals representing 24 companies in 12 separate cities. Acting on the recommendation of the Secretary, a motion was made by Vice-President Kuniansky, seconded by Director Walls, and unanimously carried

THAT the petition for a Central Michigan Chapter of A.F.A. be accepted, to be designated as the 38th Chapter of A.F.A.

Chapter Activities

The Secretary reported briefly on Chapter possibilities and activities, especially on activities in various non-Chapter areas, indicating several possibilities for Chapter extension in the future. He reiterated that the previously expressed policy of the Board was being followed, namely, that the formation of Chapters should follow the spontaneous desire of foundrymen for such activities.

Committee on Retirement Plans

President Wood stated that the Committee on Retirement Plans, headed by Director Simpson as Chairman, had submitted two separate retirement plans to the Executive Committee immediately prior to the March 19 Executive Committee Meeting, but that insufficient time had passed for that Committee to consider them. He stated that the plans would be thoroughly considered at the next meeting of the Executive Committee on June 4 after which they would be submitted to the Board of Directors for further action at the Annual Board Meeting in July 1947.

Public Health Service Survey

The Board of Directors was brought up-to-date on a projected survey of health and sanitation conditions in the foundry by the U. S. Public Health Service and discussed at the January Board Meeting. The Secretary announced that following discussions between Technical Director Massari and Health Service officials, it was agreed that A.F.A. would cooperate in a general way with the Service and that the Service would discuss with A.F.A. its findings of preliminary analysis at certain selected key plants.

Recommendations of Executive Committee

Sand Research Project. The Secretary announced that the contract between A.F.A. and Cornell University, covering the Foundry Sand Research Project, would expire on June 30, 1947, necessitating the approval of the expenditures beyond that period. He stated that the contract for the 1947-48 fiscal year was recommended at $7,500.00, an increase of $500.00 over 1946-47, including salary of the investigator, the estimated cost of moving present library facilities to a better and more efficient location, and the use of a consultant on ceramic problems.

The Secretary stated that the previous investigator, D. C. Williams, was leaving Cornell and would be replaced with a new investigator, John P. Fraser. Accordingly, acting on recommendation of the Executive Committee, a motion was made by Director Walls, seconded by Vice-President Kuniansky, and unanimously carried, approving the expenditure of $7500.00 for A.F.A. Foundry Sand Research at Cornell University, during the fiscal year 1947-48.

Student Chapter Policy. The Secretary presented a suggested Student Chapter Policy, essentially as follows:

(1) That Student Chapters be encouraged and approved on the basis of a written petition signed by no less than 15 students and, as in the case of a Chapter petition, subject to approval by the A.F.A. Board of Directors.

(2) That each Student Chapter have a Chairman, Vice-Chairman, Secretary and Treasurer, all students, no Board of Directors being necessary.

(3) That the Officers of each Student Chapter also include a Faculty Advisor and an Industrial Advisor. If the Student Chapter is located in a regular Chapter area, then the Industrial Advisor may be either the A.F.A. Chapter Chairman or the Chairman of the Educational Committee.

(4) That the entire amount of student member dues, $4.00 per year, be refunded for financing of the Student Chapter. These funds shall be held under control of the school and expenditures approved in every case by the Faculty Advisor, or as may be required under the faculty rules for each school or college.

(5) The Industrial Advisor to work with the Program Chairman in obtaining speakers for the student group. Members of the Student Chapter might well be invited to each regular chapter meeting.

(6) Closer tie-up between the regular chapter and Student Chapter should result in members of the regular chapter speaking before the student groups at regular intervals. Another possibility is that of student visitations to the plants of chapter member companies, especially during vacation periods.

After brief discussion, in which it was pointed out that student chapters should be formed at the five schools at which the Joint Ferrous Educational Committee is setting up scholarships—(Case School of Applied Science, Cleveland; University of Cincinnati, Cincinnati; Cornell University, Ithaca, N.Y.; Massachusetts Institute of Technology, Cambridge, Mass.; and the University of Wisconsin, Madison, Wis.) as well as other colleges, motion was made, seconded and unanimously carried, accepting the recommendation of the Executive Committee.

THAT the proposed Student Chapter policy be adopted and sponsored by A.F.A.

Election of Officers and Directors

The Secretary announced that, in accordance with the procedure prescribed in the By-Laws of the Association, no nominations other than those of the 1947 Nominating Committee had been received by the Secretary within a period of 45 days prior to the Annual Business Meeting, and that the nominees selected by the Nominating Committee therefore, would be declared unanimously elected at the Annual Business Meeting of the Association on April 30.

There being no further business to come before the Board, the meeting was adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
May 26, 1947

Minutes
Meeting 1946-47 Executive Committee
Hot Springs, Virginia—June 4, 1947

Present:	President S. V. Wood, presiding
	Vice President Max Kuniansky
	Director J. H. Smith
	Secretary-Treasurer Wm. W. Maloney
	Technical Director S. C. Massari
Also Present:	Vice-President-elect W. B. Wallis
Absent:	Directors Joseph Sully, F. J. Walls, L. C. Wilson

Reading and Approval of Minutes

Minutes of the Executive Committee and Board of Directors meetings held at Detroit, April 29, 1947, mailed previously to all members of the Committee, were declared approved by vote of the Committee Members present.

Membership Report

The Secretary reported Total Membership on April 30, 1947 at 9447, compared with 8539 on June 30, 1946—a net gain of 908, with 90.9% of the total represented by members of 35 active chapters. (British Columbia and Central Michigan Chapters not included).

The Secretary also reported total Dues Revenue on April 30, 1947, as $173,112.87, as against the budget of $165,000.00 for the entire fiscal year.

Financial Report

The Treasurer presented a report on Income and Expense as of May 21, 1947, showing: (a) Total Income $329,853.23; (b) Total Expenses $310,038.27; (c) Budget for fiscal year ending June 30, $349,000.00. Major Income and Expense items as of May 27 were included, with all Expenses broken down into major activities.

AMERICAN FOUNDRYMAN

The Secretary reported on progress of AMERICAN FOUNDRYMAN, stating that efforts were being made to add still further to the prestige and importance of the magazine and that it was intended to obtain a full-time Advertising Manager during the coming year. He pointed out the relationship between editorial and advertising activities, and declared that it would be the policy of the Staff to continue adding to the interest and scope of the editorial content.

Awards Policy. Vice-President Kuniansky requested clarification of A.F.A. policy on approval of any award, within the Association family, by organizations other than A.F.A. It was the consensus of the Executive Committee that any award made officially in the name of A.F.A., or semi-officially with A.F.A. Sponsorship, must be approved by the Board of Awards.

1948 Convention City

The Secretary pointed out that the Board of Directors in session on January 23, 1947, had approved Philadelphia as the 1948 Convention city. He stated that announcement of this action had been deferred pending written confirmation of adequate hotel accommodations, and such confirmation now being at hand, that the announcement of the 1948 Convention City would first be announced at the 1947 Chapter Chairman Conference, followed by its publication in the July issue of AMERICAN FOUNDRYMAN.

1949 Convention City

President Wood stated that the West Coast Chapters had invited A.F.A. to hold a Convention and Exhibit in San Francisco or Los Angeles in 1949, although the policy of the Association has been to stage exhibits only in even-numbered years. He declared that some 10 per cent of the Association's membership is represented by the five West Coast Chapters, that no Convention of A.F.A. has ever been staged on the Pacific Coast, and that the West Coast Chapters have expressed strongly their desire for an exhibit convention rather than a non-exhibit convention.

President Wood expressed the belief, concurred in by the Executive Committee, that the exhibits of A.F.A. in even-numbered years should continue to be held in the great foundry centers East of the Mississippi, for reasons of both attendance and revenue, and that any exhibit on the West Coast probably should be considered for an odd-numbered year. On the other hand, it was brought out strongly that, in view of the stated A.F.A. policy regarding staging exhibits in alternate years only, no decision on a West Coast exhibit in 1949 should be reached without prior discussion with the Foundry Equipment Manufacturers Association. Accordingly, without vote, President Wood was empowered to arrange such a meeting at the earliest possible date.

Retirement Plan

Recommendations of the Special Committee on Retirement Plans, as appointed by President Wood, having been forwarded to members of the Executive Committee in advance of the meeting, considerable discussion followed. The opinion was expressed that the two plans submitted called for too great an annual expenditure by the Association from current operating revenues. It was felt that further study should be made by an impartial and experienced agency before definite action could be taken and a plan recommended for Board consideration. Accordingly, Vice-President Kuniansky moved

THAT the Special Committee on Retirement Plans be thanked for its studies and discharged; and THAT the Secretary be empowered to retain the firm of Marsh & McLennan, for the sum of $500.00, to survey the situation and submit a definite plan to a meeting of the Executive Committee at the time of the Annual Board Meeting in July.

Motion seconded by Director Smith and unanimously carried.

There being no further business, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
June 20, 1947

Minutes
Meeting 1946-47 Executive Committee
Palmer House, Chicago—July 28, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: J. H. Smith
Joseph Sully
F. J. Walls
L. C. Wilson
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari
Also Present: Vice President-elect W. B. Wallis

Reading and Approval of Minutes

Minutes for the Executive Committee Meeting, held at Hot Springs, Va., June 4, 1947, were read and approved.

Staff Retirement Plan

In accordance with action of the Executive Committee on June 4, 1947, requesting more specific recommendations on a proposed Staff Retirement Plan, Mr. B. Russell Thomas of Marsh & McLennan, Insurance Actuaries, Chicago, entered the meeting and offered specific proposals. Numerous questions were asked by members of the Executive Committee on the proposed plan, especially pertaining to the per cent of retirement benefits to total employee contributions, extent of employee versus A.F.A. contributions, continuances of death benefits, conditions surrounding the withdrawal of benefits and the vesting of protection, and type of insurance considered feasible under present and anticipated Staff conditions.

Detailed questions on the proposed plan brought out a desire for new estimates of costs and benefits based on increased employee contributions. Accordingly, Mr. Thomas agreed to revise the figures of his proposal for consideration of the revised plan by the 1947-48 Board of Directors on July 30.

1948 Convention Contracts

The Secretary presented, for approval by the Executive Committee, a ballot authorizing the Secretary to execute all contracts necessary to the staging of the 1948 Convention and Exhibit in Philadelphia. Approved, and signed by President Wood for the Board of Directors.

Cupola Handbook Sales

The Secretary requested discussion on whether revenue from the sale of the Cupola Handbook, a publication of the A.F.A. Cupola Research Project should be allocated (a) 100 per cent of selling price to the Cupola Project, or (b) cost price to the Cupola Project, or (c) 100 per cent of selling price to the Project until project funds have been completely reimbursed for expenditures, with all revenue thereafter to A.F.A. general funds.

A letter dated July 24, 1947 from R. G. McElwee, as Chairman of the Cupola Research Project, pertaining to this subject, was read as follows:

> "The committee feels that because of being reimbursed for actual expenditures from the fund toward the preparation of the book and because of our knowledge that the general fund was used to an undetermined extent, that we are justified in instructing you to keep for the general fund any moneys from the sale of the book in excess of the amount necessary to replace withdrawals from the Cupola Funds.
>
> "We feel however that a future policy should be determined by the Committee and the Board of A.F.A. to govern reissues of the book."

The Executive Committee, without vote, expressed agreement with Mr. McElwee's outlined proposal and authorized the Secretary to work out necessary agreement on procedures with the Cupola Research Project.

Technical Correlations Committee Functions

The Secretary requested clarification of the functions of the Technical Correlations Committee, as a self-perpetuating Committee of the Board of Directors, particularly as to whether actions of the T.C.C. shall be subject to Board approval. Technical Director Massari, called into the meeting, was instructed to prepare a digest of T.C.C. functions as now understood, for acceptance by the Board of Directors.

NOTE: Other actions of this meeting incorporated as Executive Committee recommendations in minutes of Board of Directors Meeting, July 29.

There being no further business, meeting was adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
August 15, 1947

Minutes
Annual Meeting 1946-47 Board of Directors
Palmer House, Chicago—July 29, 1947

Present: President S. V. Wood, presiding
Vice-President Max Kuniansky
Directors: H. A. Deane, F. J. Dost, G. K. Dreher, E. W. Hoslebein, H. H. Judson, S. D. Russell, R. T. Rycroft, B. L. Simpson, J. H. Smith, Joseph Sully, F. J. Walls, S. C. Wasson, L. C. Wilson, F. M. Wittlinger
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari
Directors-elect present as observers: E. H. Delahunt, W. J. MacNeill, R. H. McCarroll, John M. Robb, Jr., A. C. Ziebell.
Absent: Directors: J. E. Kolb, H. G. Lamker.

Reading and Approval of Minutes

Minutes of the Executive Committee Meeting and Board of Directors Meeting, both held during the Detroit Convention on April 29, 1947, were mailed to all members of the Board of Directors previously. Motion of approval by Director Horlebein, seconded by Director Walls, and carried.

Minutes of the Executive Committee Meeting held at Hot Springs, Va., June 4, 1947 were read. On motion by Director Horlebein seconded by Director Walls, approved.

Minutes of the Executive Committee Meeting held at Chicago on July 28, 1947 were briefed by the Secretary. On motion by Director Smith, seconded by Director Dost, approved.

Report of the Secretary

The Secretary's report was presented and filed for further study by the Board, following comments by the Secretary on certain sections. Report accepted (see page xvi).

Membership. The Secretary's report on membership showed total membership on June 30, 1947 of 9683 against 8539 the previous June 30, a net gain of 1144 members, or 13.4 per cent. The report also showed total dues revenue for the fiscal year of $182,042.04, against the annual budget of $165,000.00.

Report of Technical Director

The report of the Technical Director, covering the past fiscal year, emphasized accomplishments made in reorganization of the technical committees, the establishment of a definite plan for A.F.A.-sponsored research, the stepping-up of educational activities, and better planning for special opportunities. Report accepted (see page xviii).

Report of the Treasurer

The Treasurer presented the official audited report of the Association's books for the fiscal year ended June 30, 1947, as prepared by Lybrand, Ross Bros. & Montgomery. Attention was directed also to a Consolidated Fund Statement showing net worth of the Association on June 30, 1947 to be $266,338.82, as against $341,188.90 on June 30, 1946. (See Statement, p. xix).

The Auditor's statement of Income and Expense showed income of $302,614.55 (excluding Deferred Dues Income $23,636.63; Accounts Receivable $9,884.14), against budgeted income of $349,000.00; actual expense $361,648.71.

The statement showed that all funds (Technical Development, Cupola, General Reserve, and Awards) had increased slightly in value during the past year, the reduction in the net worth of the Association being represented by a reduced cash position, a not unusual condition for the close of a non-exhibit year. It was pointed out that, whereas the Association has found it necessary to borrow from the banks at the close of several previous non-exhibit years, there appeared to be no necessity for doing so this year.

The Treasurer pointed out the time difficulty in analyzing completely for the Board of Directors the annual audited statement of Association finances as of June 30, prior to the annual Board Meeting, which the By-Laws prescribe must be held "within 30 days after the close of the fiscal year." It was felt, nevertheless, that the retiring Board of Directors were entitled to a complete analysis of the Association's finances as incurred under their stewardship. Accordingly, a motion was made by Director Smith

> THAT an Auditing Committee of three members of the Board of Directors be appointed by the retiring President to analyze the Association's finances and to report their findings and recommendations to the old Board covering their stewardship of the Association for the fiscal year ended June 30, 1947.

Motion seconded by Director Horlebein and carried, retiring President Wood to appoint such an Auditing Committee.

Reports of Board Committes

President Wood called for brief verbal reports from several committees of the Board of Directors as follows:

Vice President Kuniansky, reporting as Chairman of the *National Membership Committee* and the *Chapter Contacts Committee* stressed the thought that the National Office should give greater attention to assisting Chapters in their technical programs, as the single interest factor in attracting and holding members. He stated that the Association's membership work in the future should be directed more on quality than on quantity lines.

Secretary Maloney reported as Chairman of the *International Relations Committee*, stating that the International Committee of Foundry Technical Associations had been recognized and was expected to be functioning fully in the relatively near future; that A.F.A. was actively cooperating as a member of this Committee; that Vincent Delport of England was continuing as European representative of the A.F.A. committee, and that exchange paper relations were continuing with the Institute of British Foundrymen and had been reinstated with the French Foundry Technical Association and the Institute of Australian Foundrymen.

F. J. Walls reported, as Chairman of the *Technical Correlations Committee*, particularly on the June 20 meeting of this committee, the first held in many years. He expressed the hope that this important policy-making Committee would be continued as a correlating group for all A.F.A. technical activities.

President Wood stated that the By-Laws Committee had not met during the previous year, but was scheduled for a meeting later in 1947; that the *Annual Lecture Committee* had completed plans for both the Charles Edgar Hoyt lecturer and a lecture course at the 1948 Convention.

1949 Convention City

Extensive discussions took place on an invitation from the West Coast for a Convention and Exhibit there in 1949. The Executive Committee Minutes of June 4 and July 28 were read, the latter recommending that A.F.A. reaffirm its intention of staging exhibits not more frequently than every two years.

President Wood commented on a meeting held June 22 between the A.F.A. and F.E.M.A. Executive Committees, which conversations resulted in the July 28 Executive Committee recommendation. He then called upon each member of the Executive Committee to comment further on the results of the June 22 meeting mentioned above, the consensus being that the meeting resulted in much better understanding between the two groups, that F.E.M.A. recognized a dual responsibility with A.F.A. for staging successful exhibits, and that all agreed that an Annual Meeting of A.F.A. should be held on the West Coast in the near future.

Following some further discussion, Director Simpson moved for acceptance of the Executive Committee recommendation of July 28, as follows:

> THAT appropriate resolutions of the A.F.A. Board be prepared, reaffirming as a Board policy the intention of A.F.A. to stage exhibits of the foundry equipment and supplies, in conjunction with Conventions of the Association, not more frequently than every two years; that the F.E.M.A. and F.S.M.A. be notified accordingly.

Motion seconded by Director Dreher and carried, as a recommendation to the 1947-48 Board of Directors.

It was brought out that, as a result of the June 22 meeting with F.E.M.A., there was evidence to indicate that F.E.M.A. exhibitors would fully support any A.F.A.-sponsored exhibit in the intent and purpose of the resolution above, as adopted by the Board. It was suggested that the matter be fully developed at the time of the F.E.M.A. Annual Meeting on September 18-20, 1947.

Director Russell spoke as a member of the Northern California Chapter, stating that the Southern California Chapter had given evidence of their desire to have any 1949 Convention and Exhibit of A.F.A. held in Los Angeles; and that the Northern Chapter, in spite of its prior invitation to A.F.A. for such an event, therefore was withdrawing in favor of the Southern California Chapter. Speaking as a Director, he then requested specific action on the Southern California invitation for 1949, presented in letter form. Discussion followed, concluding with a motion by Director Horlebein

> THAT the Secretary prepare a letter, for the approval of the President, expressing to the officers of the Southern California Chapter the consensus of the Board of Directors on the staging of exhibits not more frequently than every two years as a real and implied obligation of the Association to its exhibitors, and that for these reasons the Board of Directors could not accept the Southern California Chapter's invitation for a Convention and Exhibit on the West Coast in 1949.

Motion seconded by Director Simpson and carried.

Convention Registration Fees

Action of the Executive Committee taken on June 4 was presented, recommending that registration fees at non-exhibit Annual Meetings of A.F.A. be as follows: Members $2.00 each, non-members $5.00 each. Following discussion on the timing of this recommendation, motion was made by Director Simpson

> THAT action on the question of registration fees for non-exhibit Conventions be deferred for consideration at a time when plans are being formulated for the next non-exhibit convention.

Motion seconded by Director Russell and carried.

Exhibit Space Rentals

As a recommendation of the Executive Committee, space rental charges for the 1948 Exhibit were discussed. Similar charges made by other societies were brought out, fees ranging from $1.50 per square foot to $3.00 per square foot, and it was pointed out that all costs of staging such an event have materially increased during the past eighteen months. It was recalled that $1.50 had been charged exhibitors at the Buffalo exhibit in 1944, and the Cleveland exhibit in 1946. The consensus was that net exhibit

revenue for the Association under present conditions of rising costs, made practicable some reasonable increase in exhibit space rates for the 1948 A.F.A. Exhibit in Philadelphia. Accordingly, motion was made, duly seconded and carried, accepting Executive Committee recommendation

THAT the price of exhibit space for the 1948 show in Philadelphia be established at $2.50 per square foot.

Retirement Compensations

Action of the Executive Committee on July 28 recommending the extension of retirement compensations for C. E. Hoyt and R. E. Kennedy, retired, to June 30, 1948 on the existing basis, was presented for the purpose of maintaining continuity of thought between successive Boards. Discussion took place on the question of recommendations to the new Board, and members of the two previous Executive Committees recalled the background for actions taken initially by the Board of Directors on July 19, 1945. Vice-President Kuniansky read from the resolutions adopted on July 19, 1945, the following paragraph:

"AND BE IT FURTHER RESOLVED that this Board of Directors unanimously and strongly urges that the conclusions as set forth in these resolutions shall be irrevocable, and binding upon all future Boards of Directors of the American Foundrymen's Association."

Several Directors discussed the desirability for giving the retired members of the Staff greater assurance as to continuity of future Board actions, after which motion was made by Director Simpson

THAT the Association enter into a contract with C. E. Hoyt and R. E. Kennedy covering a period of 7 years commencing on July 1, 1947, guaranteeing C. E. Hoyt and R. E. Kennedy definite rates of compensation for the full period of the contract, with the exception that after July 1, 1948, successive Boards of Directors of the Association shall have the right to reduce such compensations, if economic circumstances warrant, by not more than 50 per cent of the above stated amounts, such compensations to be terminated in the event of the death of the recipients.

Motion seconded by Director Russell and carried, as a recommendation to the new Board.

Motion then was made by Director Rycroft that the Board of Directors reaffirm the resolutions adopted by the 1945-46 Board of Directors on July 19, 1945, above, as a part of these Board Minutes. Motion seconded by Director Simpson and carried.

Membership on International Foundry Committee

Action of the Executive Committee on June 4 was presented, recommending that A.F.A. approve the application of a newly reorganized Italian Foundry Technical Associations. It was brought out that, following recommendation of Vincent Delport of England as the European representative of the A.F.A. International Committee, A.F.A. had already transmitted such approval to Mr. Delport.

The action of the Executive Committee was approved by unanimous vote of the Board.

Technical Correlations Committee

Director Walls, as Chairman of the Technical Correlations Committee, presented a recommendation of that Committee calling for the preparation by the Association of several textbooks on the college, trade school, vocational school and high school levels. He brought out the urgent necessity for better texts being made available to educational institutions throughout the country for the instruction on foundry fundamentals and practice. Director Dreher, as the Executive Director of the Foundry Educational Foundation further emphasized the importance of this project, and after discussion, Director Walls moved

THAT the Board of Directors approve the recommendation of the Technical Correlations Committee for preparation of appropriate textbooks at several educational levels, subject to later approval by the Board of specific projects as submitted by the Educational Division of A.F.A.

Motion seconded by Director Horlebein and carried, as a recommendation to the 1947-48 Board of Directors.

Centrifugal Castings Research

Technical Director Massari presented a request from the Centrifugal Castings Committee of the Aluminum & Magnesium Division for an expenditure of $1,000.00 during 1947-48 for a study on the feasibility of centrifugal casting of light alloys, such work to be carried on at the Canadian Bureau of Mines, Ottawa, Canada, the appropriation requested to cover the cost of materials, patterns, and incidental expense. It was stated that the cost of this research had been included in the 1947-48 A.F.A. budget.

On motion by Director Smith, seconded by Director Walls and carried, the expenditure of $1,000.00 for an investigation of Centrifugal Casting of light alloys during 1947-48 was approved, and recommended to the new Board for confirmation.

Brass and Bronze Research

Technical Director Massari presented a request of the Brass & Bronze Research Division of A.F.A. for an expenditure of not to exceed $5,000.00 for a study of the fracture test as an indication of the quality of tin bronze. It was stated that the cost of this project had been included in the 1947-48 A.F.A. budget.

On motion by Director Wasson, seconded by Director Wittlinger, a maximum expenditure of $5,000.00 for A.F.A.-sponsored research on behalf of the Brass and Bronze Division during 1947-48 was approved, and recommended to the new Board for confirmation.

Malleable Iron Research

Technical Director Massari presented a request of the Malleable Iron Division of A.F.A. for an expenditure of $5,000.00 during 1947-48 for a fundamental study on the suitable microstructure for pearlitic malleable iron castings preparatory to selective hardening. It was stated that the cost of such research had been included in the 1947-48 A.F.A. budget.

On motion by Director Horlebein, seconded by Vice-President Kuniansky, a maximum expenditure of $5,000.00 for Malleable Iron research in 1947-48 was approved, and recommended to the new Board for confirmation.

Aluminum and Magnesium Research

Technical Director Massari reported that the contract for carrying on the previously approved research project of the Aluminum & Magnesium Division for 1947-48 had been negotiated with Battelle Memorial Institute, Columbus, Ohio, on the basis of bids taken from several institutions.

Resignation of Director G. K. Dreher

Director Dreher requested the privilege of addressing the Board on his new status as Executive Director of the Foundry Educational Foundation, Cleveland, stating that his acceptance of this position had placed him in the position of being both manager and managed in the same organization, since A.F.A. is a supporting member of the Foundation, and at the same time the members of A.F.A. are in large part the contributors to the work of the Foundation. He felt that the purpose of the Board of Directors would be better served from the standpoint of both territorial and industrial representation by the appointment of another Director in his place, and therefore tendered his resignation from the Board of Directors.

President Wood expressed for the Board his understanding of the motives involved and his appreciation for Director Dreher's thoughts on behalf of the Association coverage on the Board. At the same time he expressed the sincere regrets of the Board for those conditions which had brought about Director Dreher's decision. The President then pointed out that Board action was required on Director Dreher's resignation. Accordingly, on motion duly seconded and carried, the resignation was accepted by the Board.

Appointment of Robt. Gregg as Director

President Wood then pointed out the necessity for filling the vacancy thus left on the Board and called attention to Art. XVI, Sec. 2 of the By-Laws, reading "Should a vacancy occur in the the Board of Directors . . . through death, resignation, or other cause, the Board of Directors may select a member of the Association to fill the vacancy until the next Annual election . . . " The President called attention to the fact that the resignation of

Director Dreher, formerly resident in Los Angeles, and the imminent retirement from the Board of Director Russell of San Francisco after a three-year term of office, indicated the desirability of appointing to the Board a Director representing the West Coast Chapters.

Accordingly, Director Russell moved that Robert Gregg, President of Reliance Regulator Co., Los Angeles, be appointed to the Board of Directors to fill the vacancy left by the resignation of George K. Dreher. Motion seconded by Director Smith and unanimously carried. The Secretary was instructed to notify Director Gregg of his appointment, for acceptance.

Recommendations of the Retiring President

President Wood presented for guidance of the incoming Board of Directors his recommendations on the conduct of Association activities, gained through impressions obtained during the previous year, during which he had travelled some 70,000 miles in visiting 14 Chapters of the Association from Coast to Coast. (The President's recommendations are made a part of these minutes, but separately appended.)

Appreciation

Director and Past-President Walls expressed on behalf of the retiring members of the Board of Directors his sincere appreciation of the opportunity to have served under President Wood and on the Board, and to the incoming President Max Kuniansky congratulations and best wishes for a successful term of office. Director Simpson then moved

> THAT the Board of Directors go officially on record to thank President Wood for his conscientious and successful term of office, and that they extend to Mrs. Wood the Board's felicitations on the great amount of time spent by the President away from his family during the previous year.

Motion seconded by Director Russell, and unanimously carried.

There being no further business, the 1946-47 Board of Directors was declared adjourned.

Respectfully submitted,
WM. W. MALONEY,
Secretary-Treasurer

APPROVED:
S. V. WOOD, *President*
August 15, 1947

Charles Edgar Hoyt Annual Lecture

THE CUPOLA FURNACE

Dr. James T. Mackenzie
Director and Chief Metallurgist
American Cast Iron Pipe Co.
Birmingham, Ala.

ABSTRACT

The author discusses the development of the cupola through the years as a means for melting iron. He discusses the design features of the many types of cupolas which have been used in the past with the reasons for the passing of some of them.

The author also reminisces of the grand men of the foundry industry who have contributed to the development of cupola furnace practice.

THE WORD "CUPOLA" is the diminutive of the Latin word "cupa," meaning cask. The reason for the use of this word is shown in Fig. 1 and 2. A cupola can be defined simply as a refractory-lined cavity with necessary openings at the top for the escape of the gases and for the charging of the stock, at the bottom for entry of the air blast, and for drawing off the molten iron and slag. A bed of fuel is laid in the cupola and ignited, after which, alternate layers of metal and fuel are charged and the blast is turned on. If a few simple laws are obeyed, melting begins quickly and continues for a long time if so desired.

An extremely simple cupola (Fig. 3) was described recently. It was constructed by our armed forces in New Guinea from an oil drum and some other scrap welded together in the field. The cupola is by far the simplest and the most efficient melting furnace. It is used for almost all gray iron melting, for most of the rock wool production, as the primary melting unit for a great deal of the malleable iron manufactured, and as the sole melter of a considerable tonnage of pig, scrap, and spiegel for transfer to the open-hearth furnaces, as well as some copper alloys. According to a recent survey, there were 5235 cupolas in the 3436 gray iron foundries of the United States and Canada.

Reactions in Cupola

When the blast is put on, a series of reactions occur. The coke burns and the iron melts but so does the lining. Some of the iron, manganese and silicon also burn and form, with the ash of the coke, a slag which is extremely corrosive to the lining. The resulting material freezes readily when it strikes the cold blast at the tuyeres. Hence, during a long heat it is necessary to get the iron melted before the brick lining gives out and/or the tuyeres stop up; and if one of these things happens, the other is usually not far behind.

Fortunately, the attack on the lining is slow and it is still further reduced by the use of flux which reduces the corrosiveness of the slag and improves its fluidity so that with good brick and proper slag, steady and uniform blast, and good melting stock, heats of many hours are feasible. (It is common to run a week in steel works practice). The author made an extensive study some years ago when on 120-hr. continuous operation in the pipe foundry. He tried heats of 12 to 40 hr. and came to the conclusion that 20-hr. heats were the most economical under operating conditions in the author's plant.

Water-cooled cupolas were used—Gmelin's cupola was the first on record (Fig. 4)—but they have not been used to any great extent in iron melting. The reason for this is probably because of the large amount of heat extracted from the zone in which it is most needed, and also because of the well known incompatibility of water and liquid iron. Water-cooled cupolas are generally used in melting rock for wool. There is a mild cooling effect with consequent warming of the blast in the windbox type (Fig. 5), such as the Whiting, the balanced-blast, the Equi-blast, etc. In this case the heat is not lost but on the other hand the cooling effect on the shell is rather mild.

Various Types of Cupolas

The modern cupola is commonly associated with the name of John Wilkinson. The date of his patent was June 2, 1794 (Fig. 6). Actually, it was intended as an improvement on the old forge used for centuries as a smelting furnace but it turned out to be a melting unit instead and developed along those lines. Such units as the bellows-driven cupolas (Fig. 1) are said to have been used by intinerant foundrymen for mending broken cooking utensils in China for many centuries.

Small cupolas have been ingeniously designed for special purposes. W. J. Keep made one in sections and mounted it on wheels (Fig. 7) while J. W. Paxson built a special cupola (Fig. 8) for repairing trolley tracks which had its own blower and a guard to prevent damage to the trolley wire. T. D. West, an energetic experimenter, designed a combined cupola and crucible furnace (Fig. 9) largely for experimental melts but his

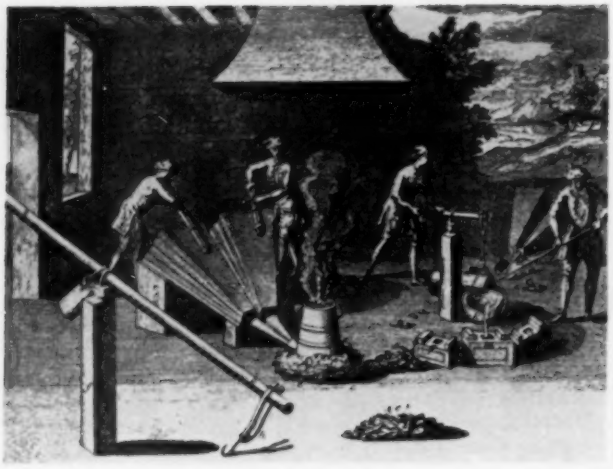


Fig. 1—A French iron foundry in 1789.

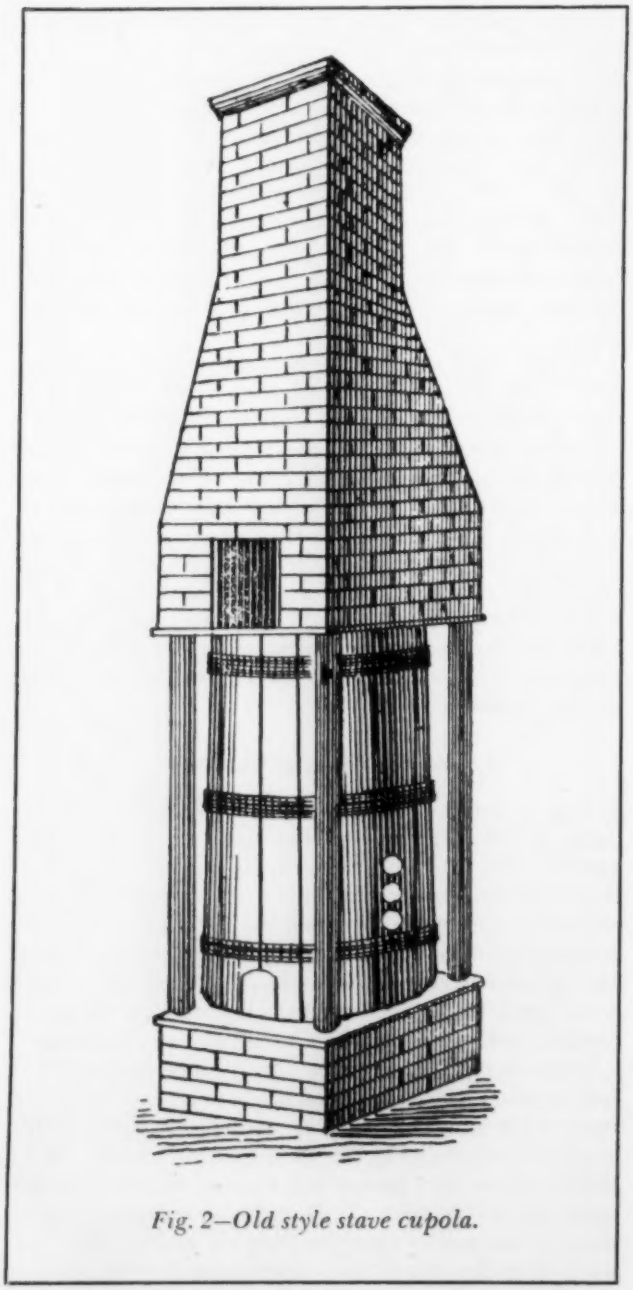


Fig. 2—Old style stave cupola.



Fig. 3—Small cupola constructed from oil drums.

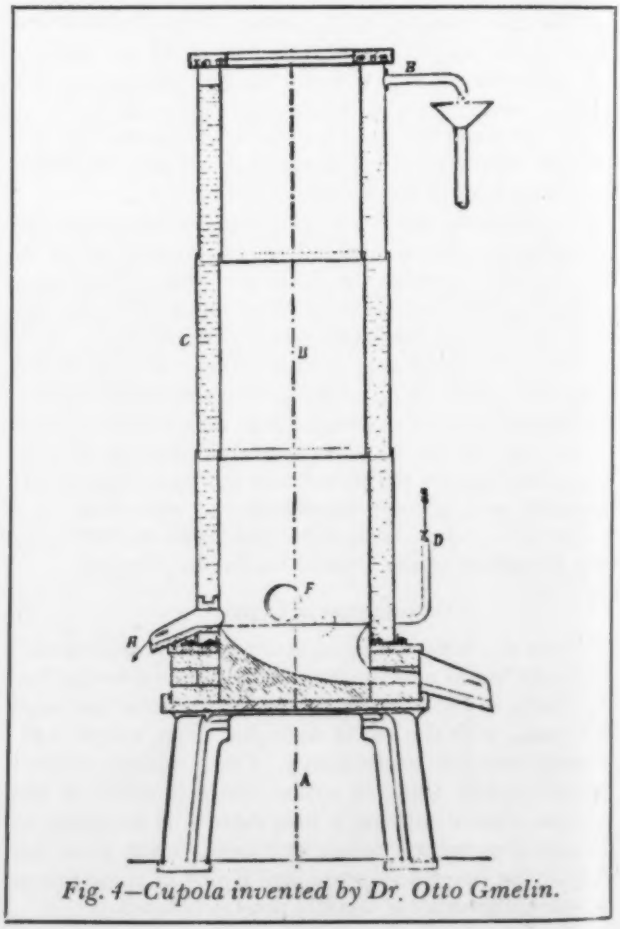


Fig. 4—Cupola invented by Dr. Otto Gmelin.

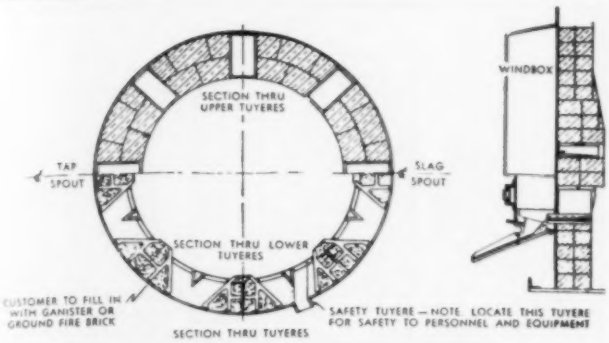
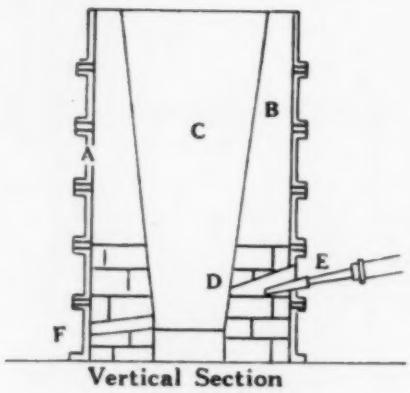


Fig. 5—Cross-section of windbox type of tuyere.



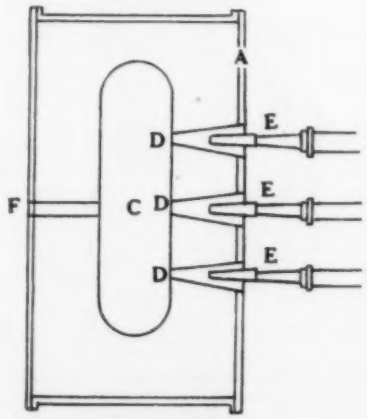


Fig. 6—Vertical and horizontal sections of John Wilkinson's unit for melting iron.



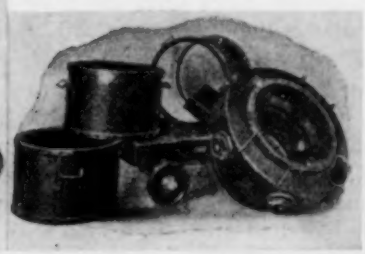


Fig. 7—W. J. Keep's sectional cupola made in sections 18 in. deep and about 27 in. outside diameter.

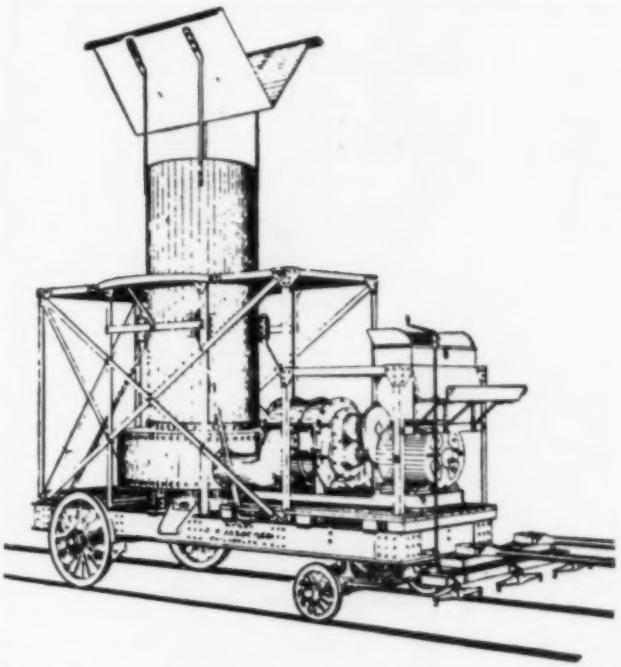


Fig. 8—Paxson truck and track cupola used in melting iron for making joints in connecting the ends of rails for trolley roads.

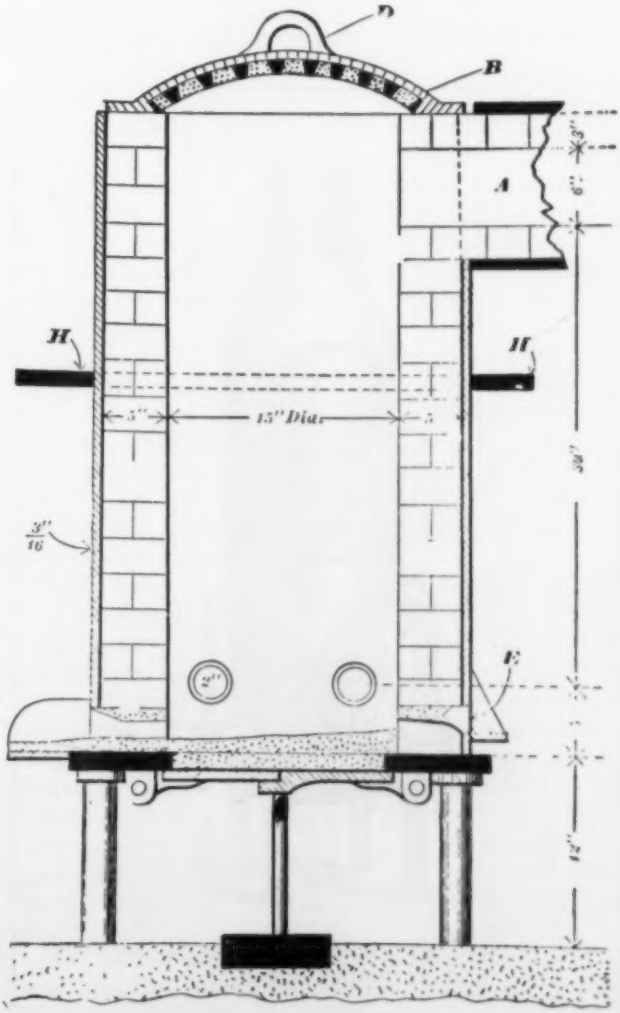


Fig. 9—West's combined cupola and crucible furnace.

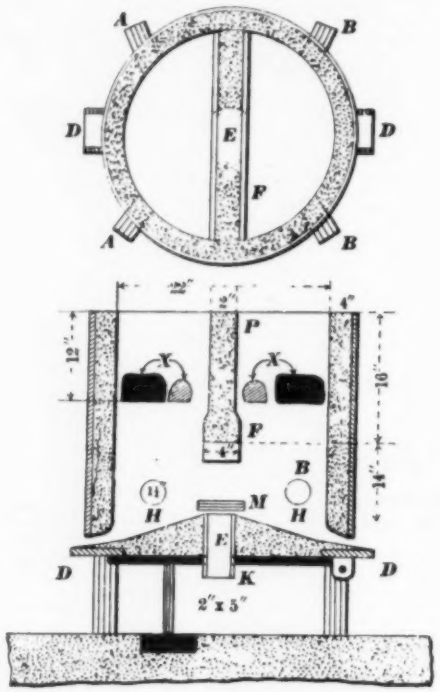


Fig. 10—West's comparative fusion test cupola.

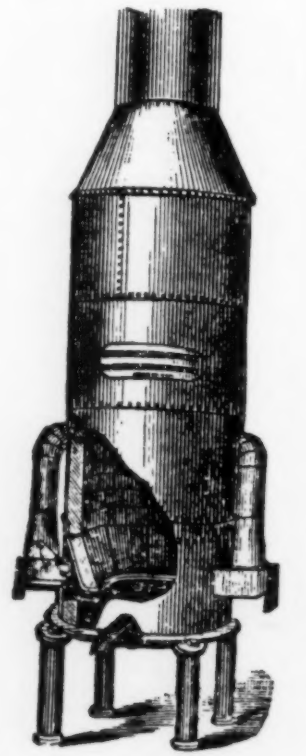


Fig. 11—Oval-shaped Mackenzie cupola with continuous tuyere.

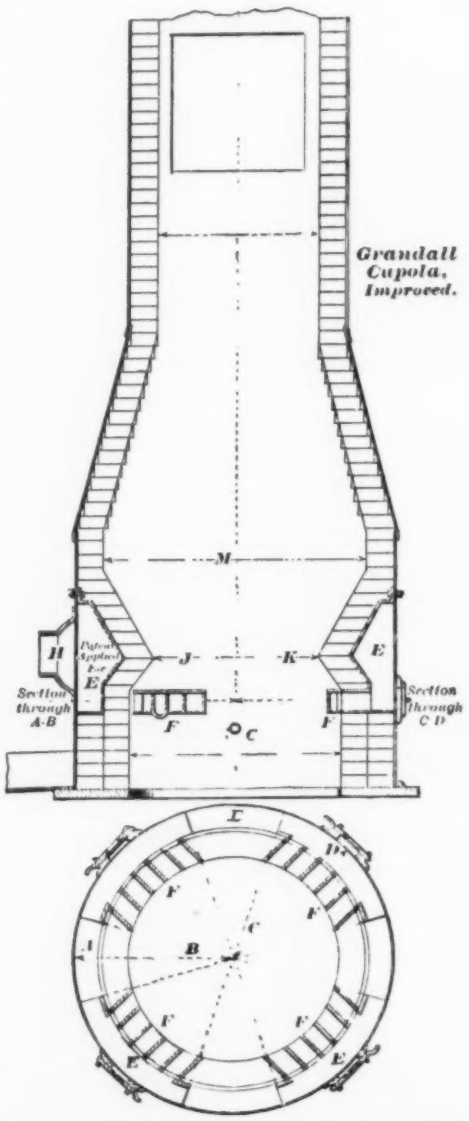


Fig. 12—Cross-section of the Grandall cupola.

repeat results were not satisfactory so he designed the Comparative Fusion Cupola (Fig. 10) for melting one kind of iron in one side and another in the other and he assumed the conditions would be the same. He also employed the center tuyere.

The Shaw-Walker Cupolette is an ingenious little cupola with an inside diameter of about two feet and only about two feet high above the tuyere with a small blower similar to a small hair dryer in each tuyere. A cover over the top with a small hole, about 6 in. in diameter, helps force the blast to the center and also radiates the heat downward. Batch melting of 100 to 200 lb. of iron at frequent intervals and temperatures of 2600° F. or higher is a feature of this small unit.

Cupolas have been built of heights from the 2-ft. stack of the cupolette to the 50-ft. stack Dr. R. A. Moldenke tells about employed in Germany. He states that it melted hot iron but it is doubtful if the extreme height had anything to do with it. Sizes range from

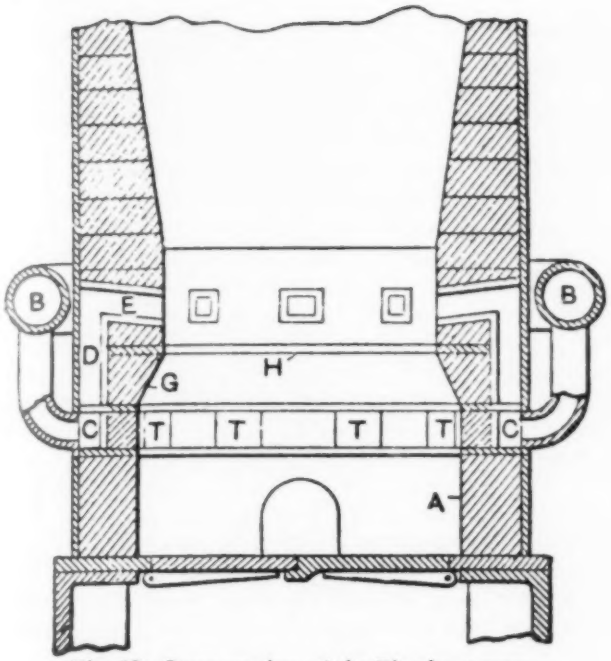


Fig. 13—Cross-section of the Zippler tuyere.

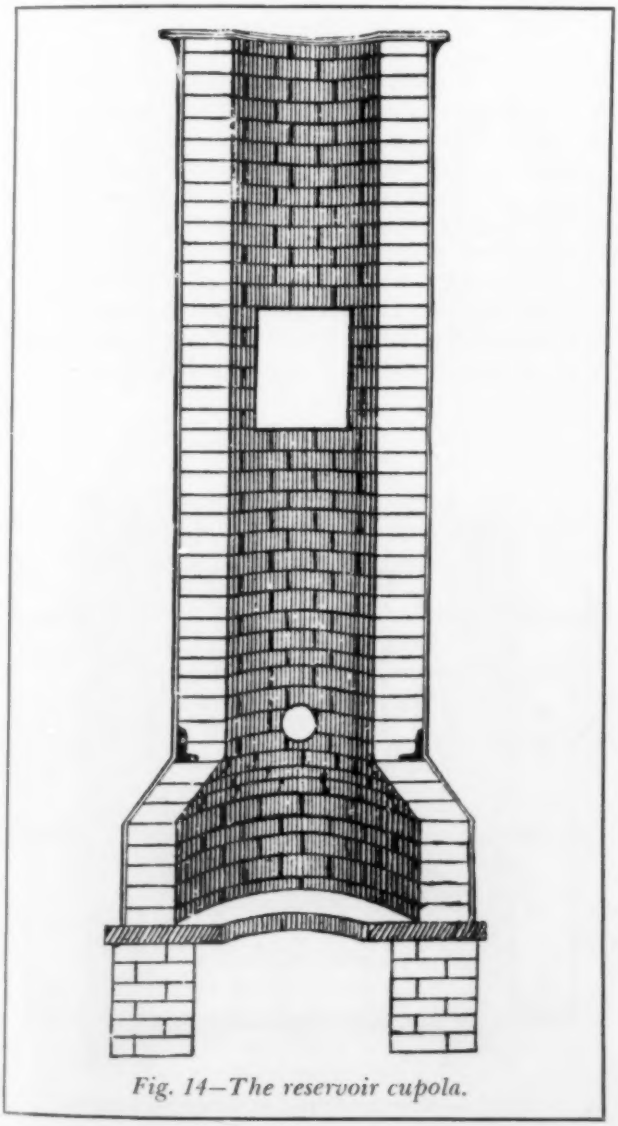


Fig. 14—The reservoir cupola.

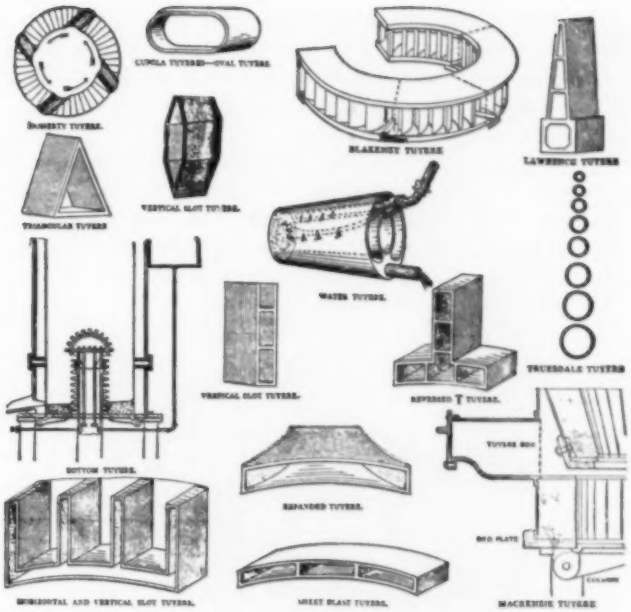


Fig. 15—Various types and shapes of tuyeres used in the foundry cupola.

about 5 in. to 10 ft. and shapes from rectangular to oval and round. The oval cupola (Fig. 11) taken from T. D. West's *American Foundry Practice* (1882), was designed to give better penetration of the blast in larger sizes. Naturally, the brick work is quite complicated but the idea is sound and the difficulty has been overcome by making the furnace link-shaped, i.e., two half-circles with a rectangular section between them. The three 40-ton cupolas at the author's plant are two 72-in. semi-circles with a 30-in. x 72-in. rectangular section. Standard firebrick shapes are thus available for lining. Some of the earlier cupolas were made square and some rectangular, but the circular shape is almost universally used today.

The vertical cross-section has been the subject of much experimentation. E. Kirk at one time thought the shape of a lamp chimney like the Grandall (Fig. 12) was the ideal shape but later changed his opinion in favor of a straight lining or a slight bosh, as shown in the Zippler arrangement (Fig. 13). The reservoir cupola (Fig. 14) was another curious shape with three courses of odd-shaped fire brick.

By far the greatest interest in the structural details has been shown in the tuyeres. Some of these have been shown in previous illustrations, the most successful of the unusual ones being the Zippler tuyere (Fig. 13) which, Kirk said, worked well so long as Zippler ran the cupola. The brick work is somewhat complicated. The modern tuyere employed to the greatest extent is shown in Fig. 5. Hurst illustrates tuyeres in Fig. 15. Others are shown in Figs. 16, 17, and 18. The Watt cupola, which Kirk says was a good melter, and the Knoeppel (Fig. 19), which was a practically continuous lower tuyere with very high upper tuyeres are also shown.

Kirk says this system was quite successful on some large cupolas. One of the first continuous tuyeres and the first cupola patented in this country was the Mackenzie cupola (Fig. 11). Of this cupola E. Kirk says:

"Before these improvements were made cupolas melted very slow, and it was the practice to put on the blast just after the noon hour and melt all afternoon. From four to five hours were commonly required to melt any ordinary heat.

"Molders generally stopped molding when the blast was put on, and a great deal of valuable time was lost waiting for iron. To prevent this loss of time, Mr. Mackenzie, a practical molder and founder of Newark, N.J., conceived the idea of melting a heat in two hours, and designed the Mackenzie cupola, which, when first introduced, was known as the two-hour cupola.

"This cupola, I believe, was the first cupola patented in this country, and it presented a number of new features in cupola construction.

"The old theory of driving blast to the center of the cupola by force of the blower and the small tuyeres was entirely abandoned, and the theory of supplying a sufficient volume of blast to fill the cupola adopted. Cast-iron tuyere boxes were bolted to the castings for the attachment of blast pipes, and blast was delivered to the cupola from an inside belt air chamber and continuous tuyere. The air chamber which was formed by an apron riveted at the top to the cupola shell was entirely open at the bottom, giving unlimited space for escape of blast into the cupola.

"This was a complete change from the old theory of putting blast into a cupola with great force, and revolutionized the theory of melting. This cupola gave excellent results, and was adopted by all the leading foundrymen of the time, and many of them are still in use, and continue to give good results in melting when properly managed.

"But this cupola has its objectionable features, the greatest of which is its tendency to bridge and bung up when not properly managed. This tendency to bridge is due to a large extent to the cupola being boshed by the inside air chamber, and the blast being supplied to it just at the point of the lower angle of the bosh. The blast passes up over the bosh before it becomes heated, causing a chilling of cinder and slag at this point, and the building-out of the lining with a very hard substance that is difficult to remove, and careless or incompetent melters frequently permit the lining to grow at this point until the melting capacity of the cupola is reduced one-half, and the smaller ones frequently bridge before they are in blast more than an hour when the lining is permitted to get out of shape.

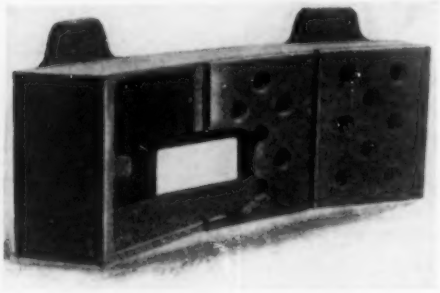


Fig. 16—Block used for Watt tuyere.

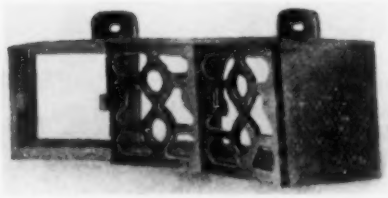


Fig. 17—Section with tuyere plates attached.

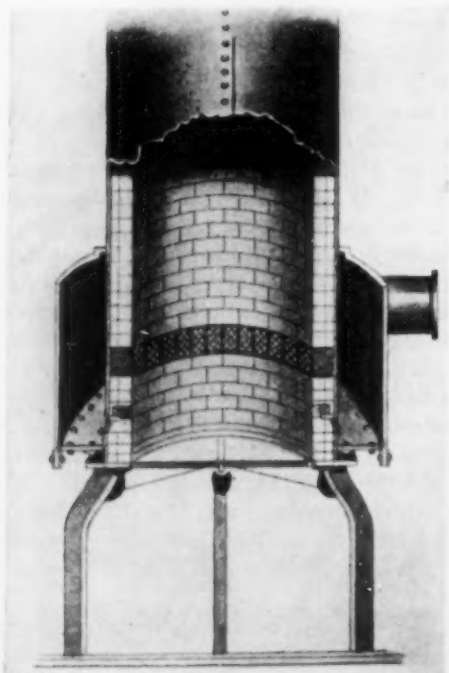


Fig. 18—Section of a cupola fitted with Watt tuyeres.

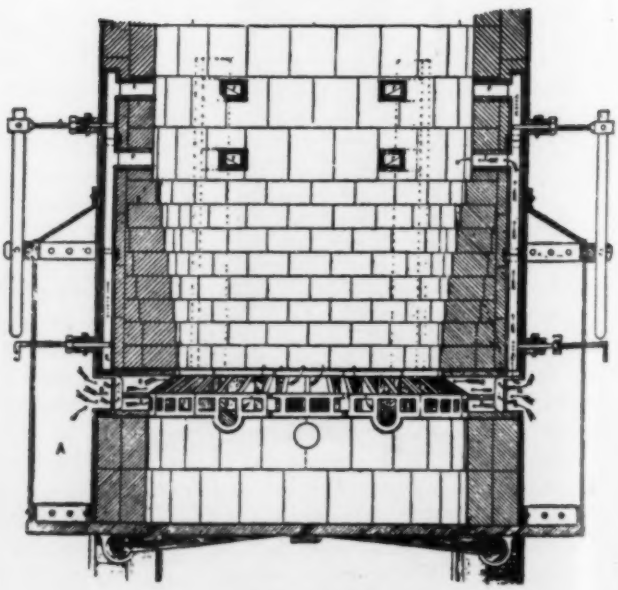


Fig. 19—Knoeppel system of cupola tuyeres.



Fig. 20—Colliau cupola.

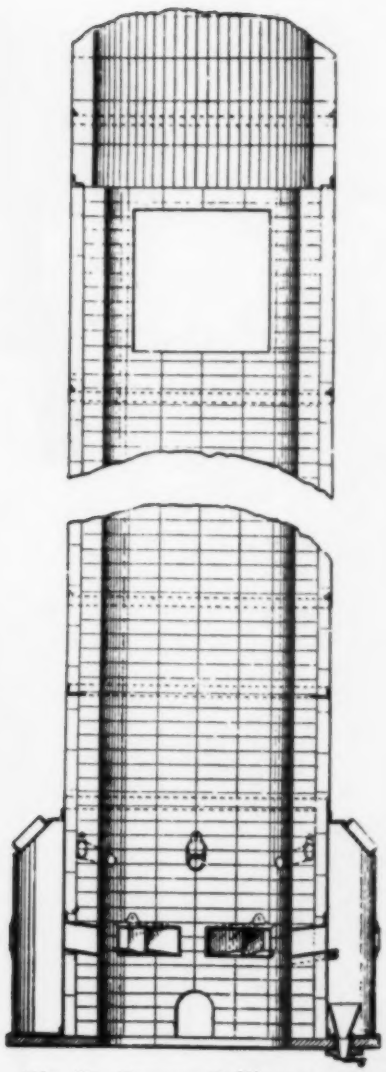


Fig. 21—Paxson Colliau cupola.

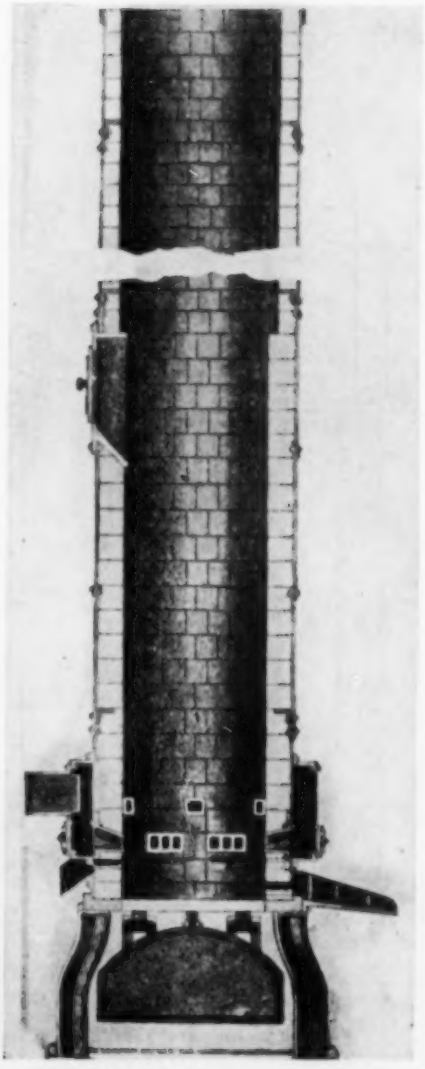


Fig. 22—Newton cupola.

"Much better results might have been obtained from this cupola had the inventor furnished, to be hung up near the cupola for the guidance of the melter, a framed diagram or blueprint, showing the proper shape for lining of the bosh and melting zone when the lining was new and as it burned away; but such a diagram was never furnished, and I have frequently seen melters running these cupolas who did not have the least idea of the shape the lining should be put in when repairing it for a heat, having never seen one when newly lined or in shape."

Thus Mackenzie obviated the need for penetration on large cupolas by the oval shape already discussed.

The furnaces most generally used in the last half of the nineteenth century were the Colliau (Fig. 20), which were later built by J. W. Paxson. An internal view is shown in Fig. 21. The Newton was another good cupola (Fig. 22) along the plain simple lines of the Colliau. Among the most popular cupolas in England, and on the Continent, was the Ireland (Fig. 23) though the tuyere system is complicated and difficult to maintain. These cupolas were replaced with a plain cupola of the Colliau type before 1929. It is one of the first attached forehearths shown in the literature (*ca.* 1890). Two other interesting ones are the Magill, with its extremely expensive refractory tile tuyeres construction (Fig. 24) and the Groves cupola with the triangular tuyeres and the cast iron bricks at the charging door (Fig. 25).

Figure 26 shows the Greiner cupola, about 300 of which, Kirk says, were used in Europe and in no case was the coke saving less than 20 per cent and in some cases even 40 and 50 per cent. The Stewart, made in Scotland, was of quite similar construction. Figure 27 shows the section of the Greiner-Erpf. (Alex Moore said of this: "A number of pop-gun tuyeres at different heights with which to vaccinate the monoxide as it tries to leave by the fire escape without a permit from the Board of Health"). There is a striking similarity to the Poumay cupola (Fig. 28) which was patented many years later. However, there were many Poumays operating in England and on the Continent with apparently good results in 1929.

The Sheehan cupola (Fig. 29) frankly and boldly

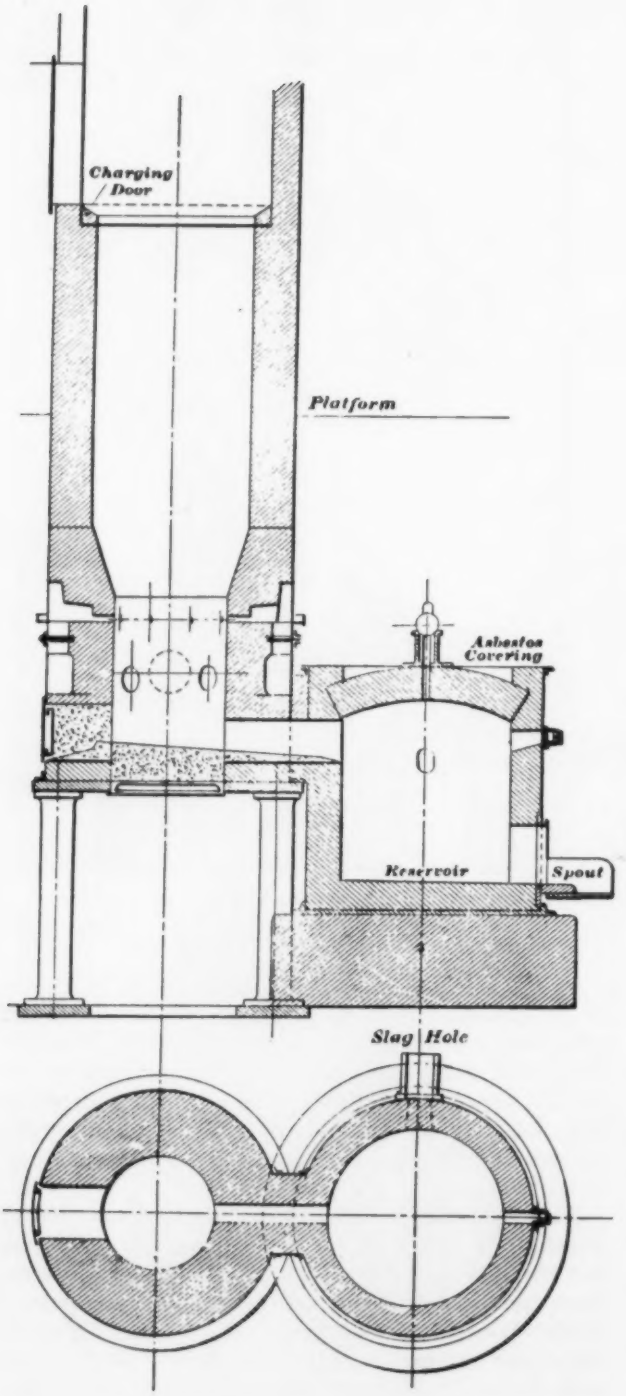


Fig. 23—Vertical and horizontal cross-sections of Ireland cupola.

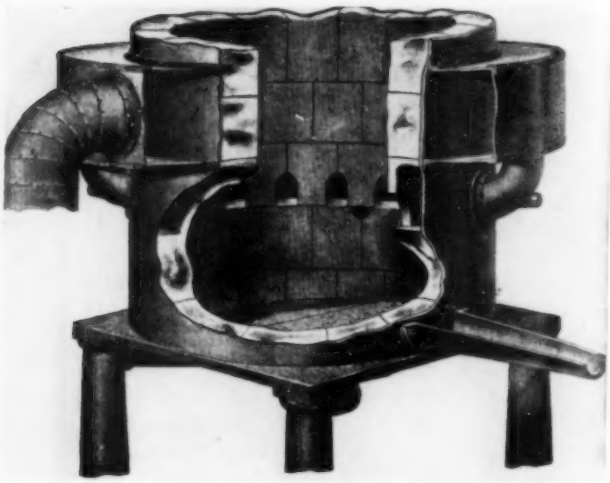


Fig. 24—Magill patent cupola.

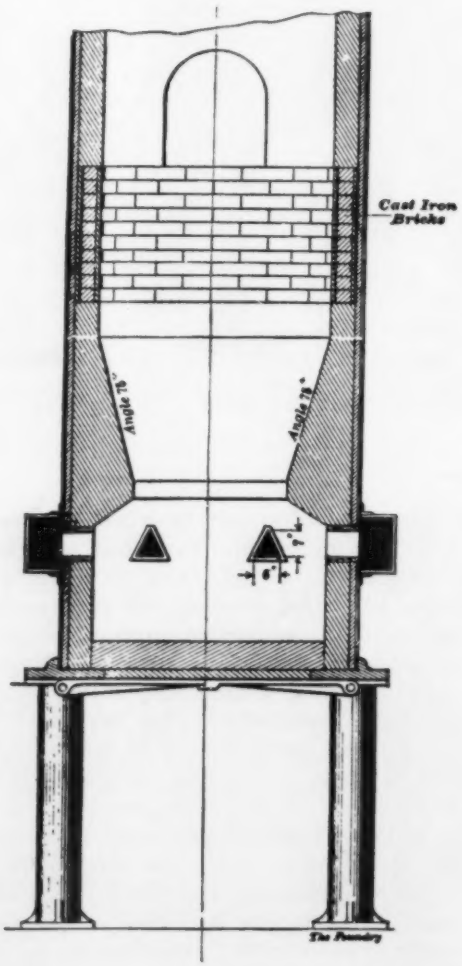


Fig. 25—Section of Grove cupola with triangular tuyeres and cast iron bricks at the charging door.

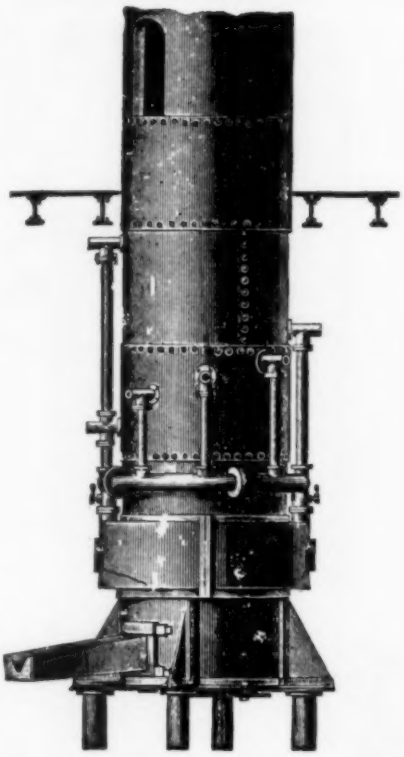


Fig. 26—Greiner economical cupola.

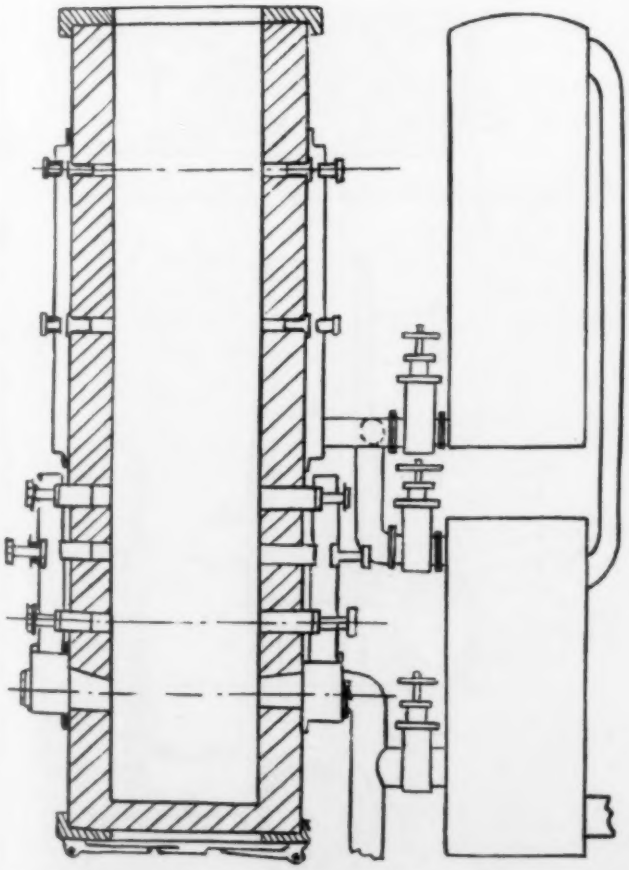


Fig. 27—Arrangement of auxiliary tuyeres described in Greiner and Erpf's original patent specifications.

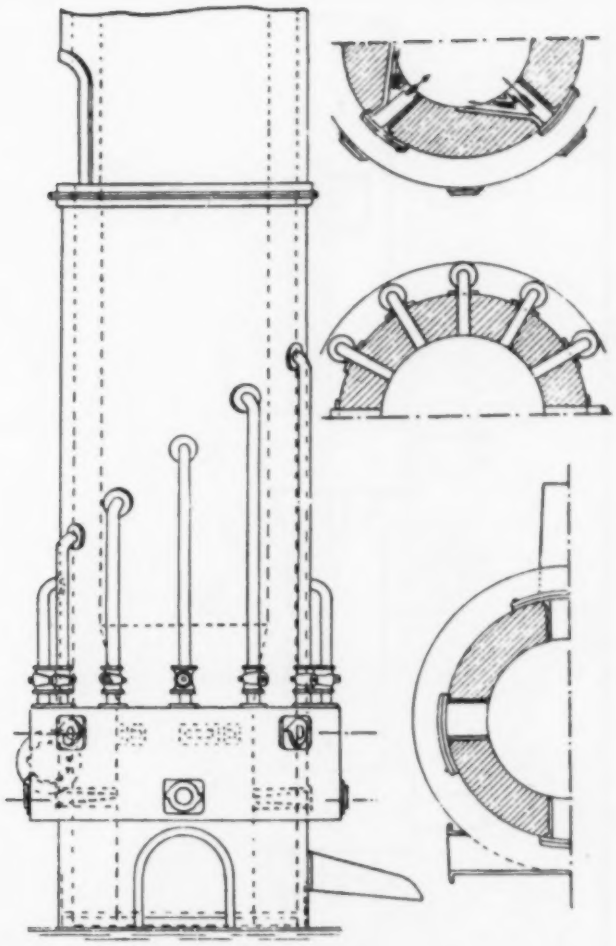


Fig. 28—Poumay system of secondary air injection through spiral row of small openings.

puts in two sets of tuyeres and proposes to melt with the upper row and superheats with the lower. Sheehan claims it can reduce silicon from silica in the reaction zone between tuyeres. The author tried this by pouring a ladle of molten iron through a 21-in. cupola running with nothing but coke in it but the rise in temperature was only about 100° F.

The latest attempt to accomplish the recovery of some of the carbon monoxide heat loss is the Balanced Blast Cupola of the British Cast Iron Research Association (Fig. 30). It is said to differ from the previous examples by having the relative amounts of air admitted at the several levels adjusted to give a maximum of carbon monoxide at the bottom and a minimum at the top. Most of the users report excellent results. A useful feature of this cupola is the provision of valves by which the bottom tuyeres may be cut off one or two at a time and any accumulation of slag melted away. One user reports regular heats of 30 hr.

Many attempts have been made to recover the waste heat, both sensible and latent, in the blast itself. Cameron (Fig. 31) tried to cool the refractories on one side and heat them on the other. Olivo (Fig. 32) tried to add the heat of the gas escaping from the slag hole.

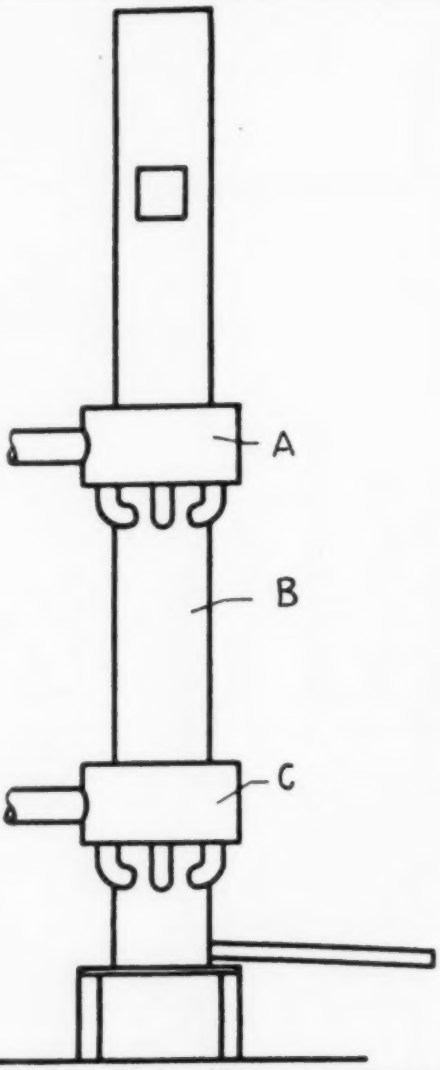


Fig. 29—Sheehan cupola with two sets of tuyeres; A, melting zone; B, reaction column, and C, superheating zone.

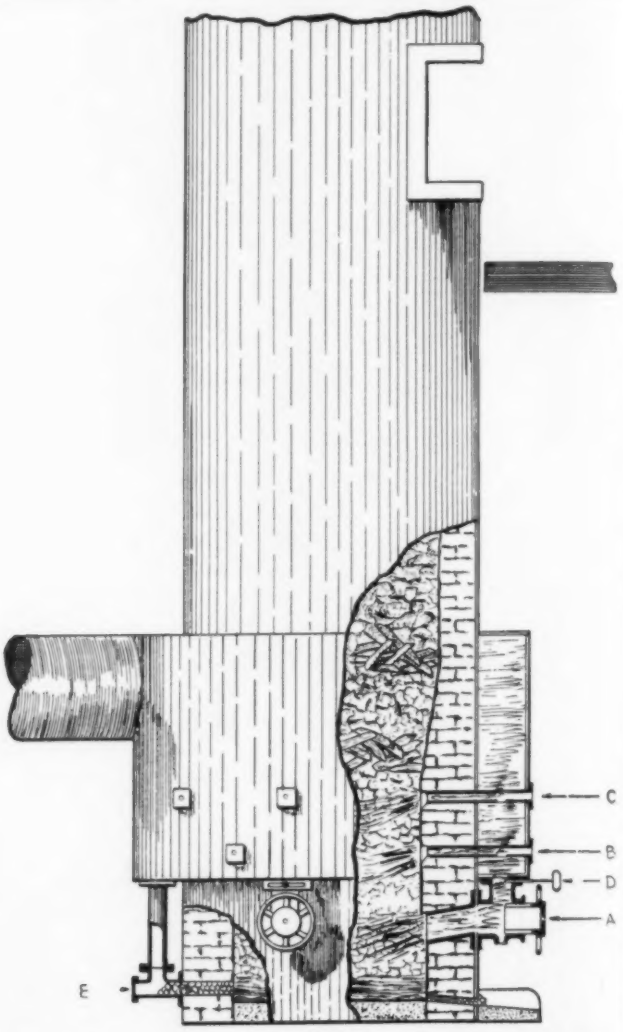


Fig. 30—General arrangement of Balanced Blast Cupola.

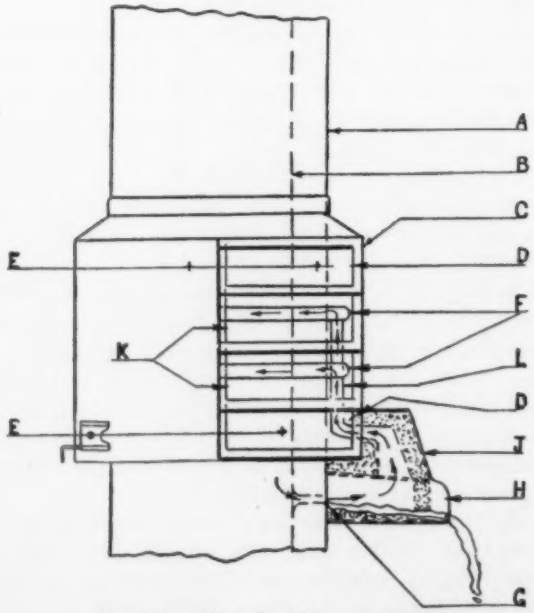


Fig. 32—Olivo hot-blast cupola.

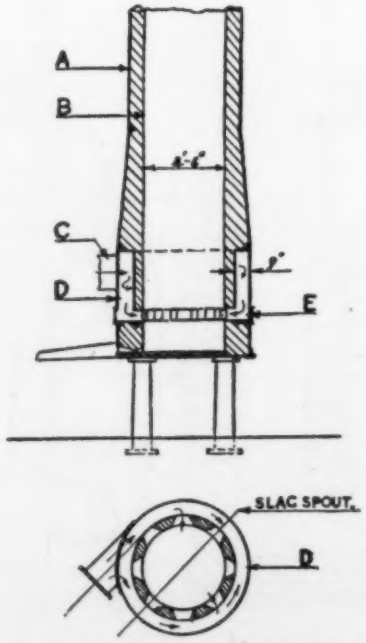


Fig. 31—Cameron hot-blast cupola.

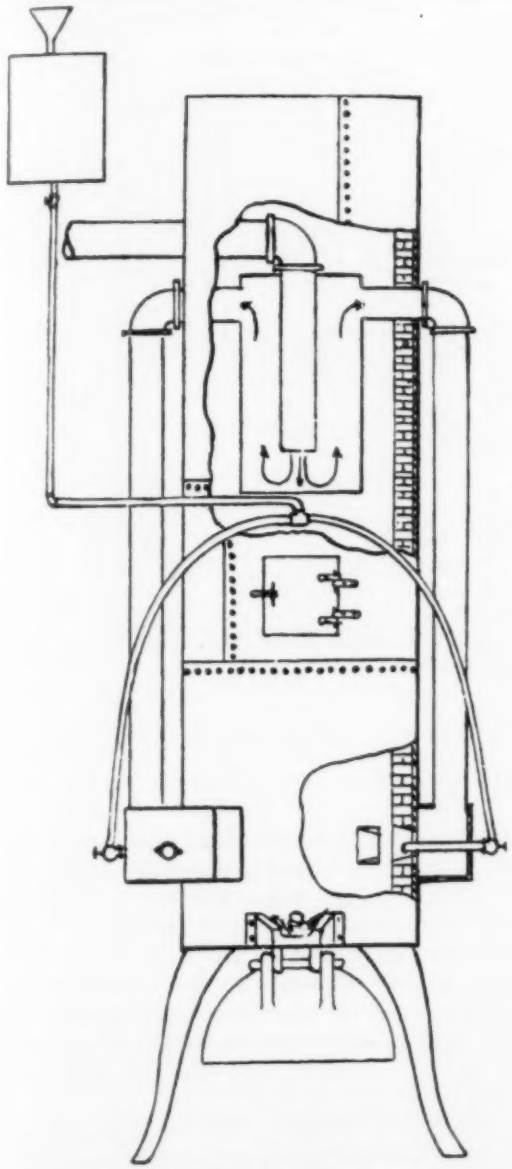


Fig. 33—Holland cupola with heat exchanger above the charging door.

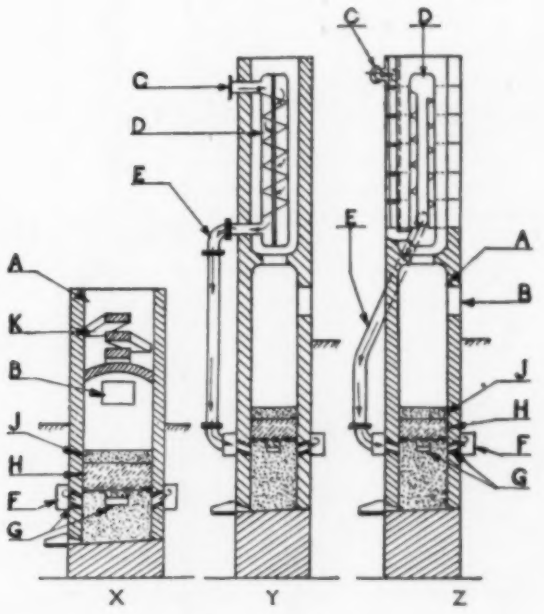


Fig. 34—Zoller hot-blast cupola.

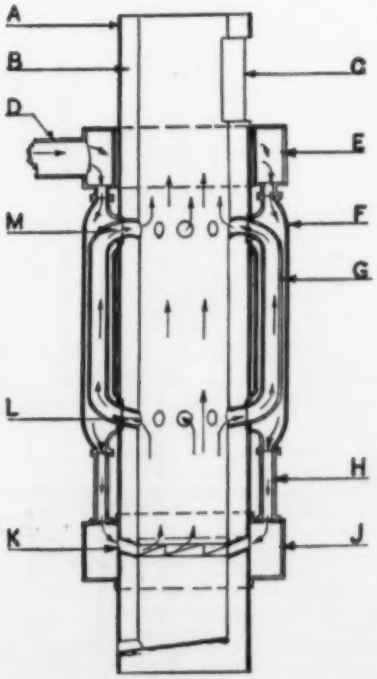


Fig. 35—Frauenknecht hot-blast cupola.

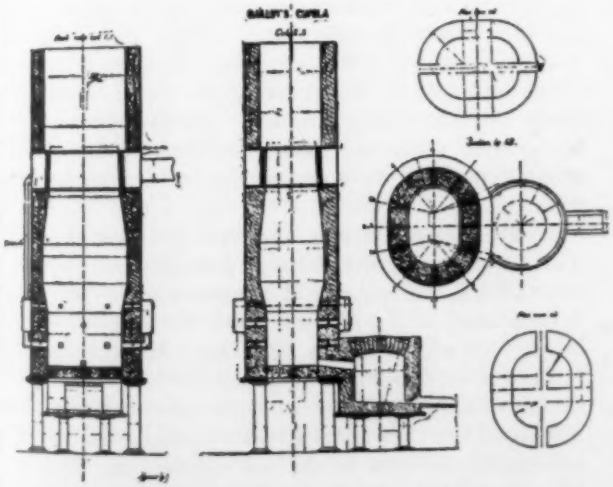


Fig. 36—Baillot hot-blast cupola.

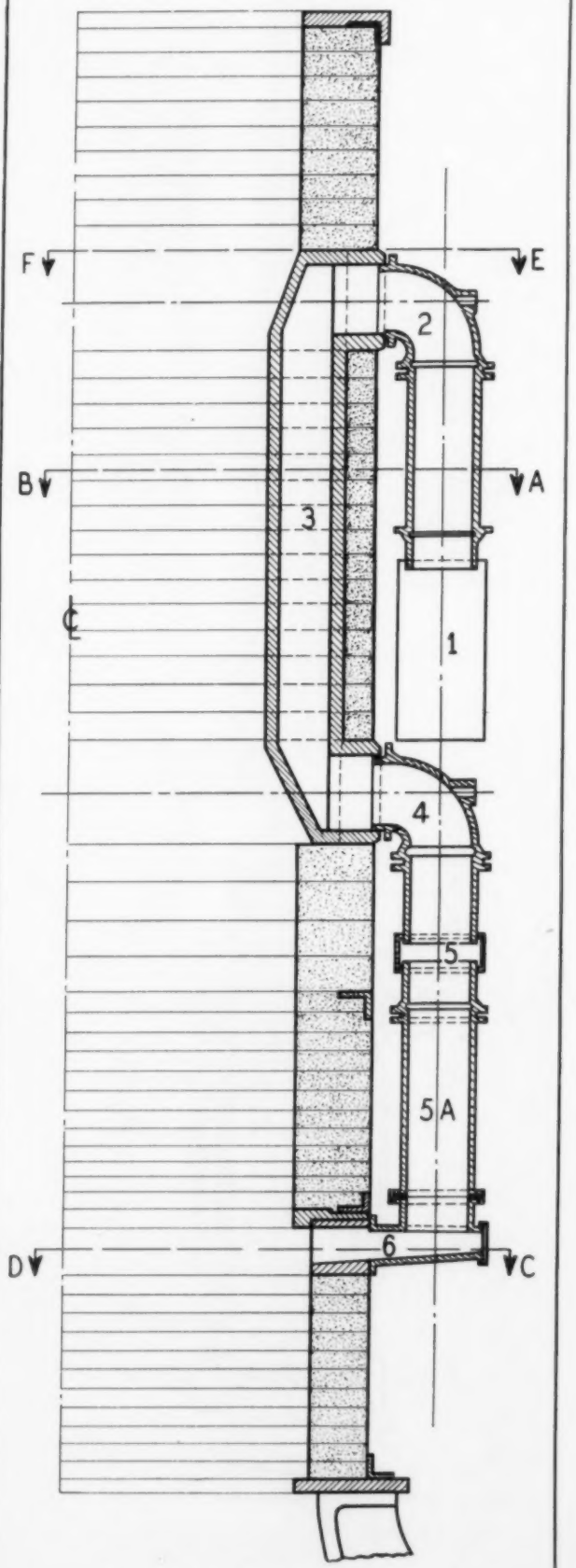


Fig. 37—Moore hot-blast cupola.

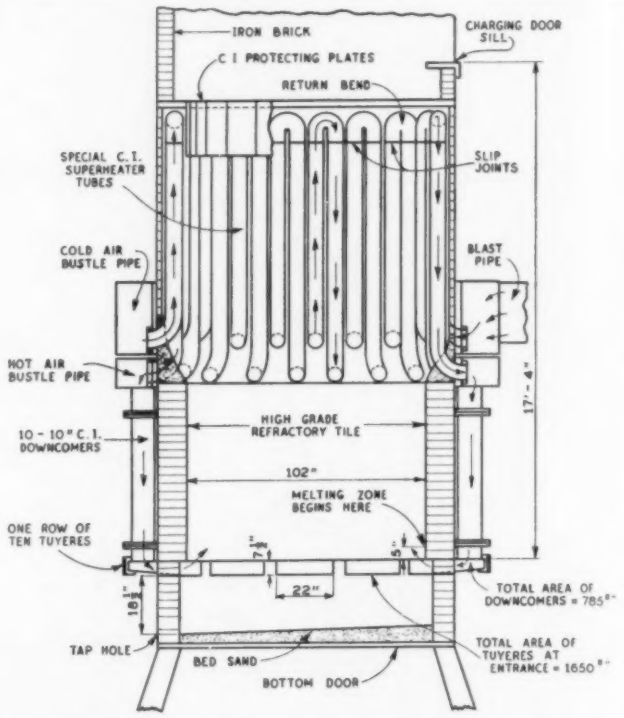


Fig. 38—Improved Moore system designed for a cupola.

Holland (Fig. 33) in 1890 tried one type of heat exchanger above the charging door, and Zoller tried another one (Fig. 34). There are several reasons for the failure of this type of cupola. There must be considerable carbon monoxide in the gas to give a flame hot enough to heat the blast; the flame is usually slow in developing; and the mechanical upkeep, due to oxidation of the structure and to stresses due to repeated heating and cooling is high—also, the structure is accessible only with difficulty.

Frauenknecht (Fig. 35) tried by-passing some of the hot gases around the preheating zone through the downcomer pipes, thus heating the blast instead of the stack—the upkeep must have been a problem. Baillot in 1908 (Fig. 36) ran the blast through a ring-shaped metal box in the stack but the heat from so small a contact must not have been considerable. Coplan improved the Baillot construction by increasing the size of the ring which he made of heat-resisting alloy and lowering it much closer to the melting zone. He also put vanes in the ring to make the blast pass three or four times around the box. Warping of the boxes was the chief problem.

Moore tried the vertical box type of casting (Fig. 37). These were of ingot-mold type gray iron and weighed over 3000 lb. They would gradually grow and crack (on cooling) at a point just about the level of the top bustle pipe (No. 1 in Fig. 37). They lasted from 25 to 125 heats of 16 to 20 hr. and the blast temperature was raised about 200° F. Two high-nickel-chrome boxes were tried but they warped badly during about the fifth heat and tore loose at the top connection. Barr and Holmes then designed the tubular construction shown in Fig. 38. These tubes are connected at the top by

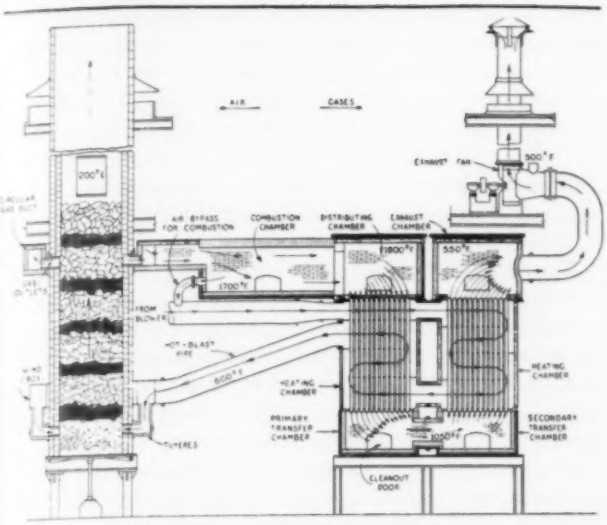


Fig. 39—Schematic view of Griffin hot blast equipment.

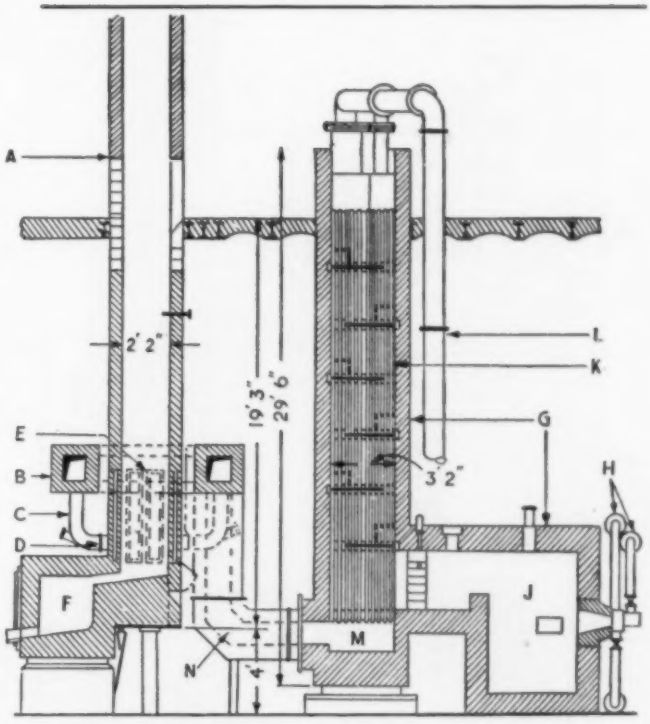


Fig. 40—Piwowarsky hot-blast cupola.

U-bends with a piston ring fit so there is no restraint lengthwise. Plain cast iron pipe was used at first but 25-20 chrome-nickel steel is used now with results of over one hundred 20-hr. heats to date with no signs of failure. The temperature rise on this type is of the order of 250° F.

The Griffin hot blast (Fig. 39) is the most widely used and generally successful scheme yet devised for using both sensible and the latent heat of the cupola gas. A large portion of the gases is exhausted at a point a few feet below the charging door and burned in the heat exchanger where the blast is heated to 600° F. The only trouble with this unit, that has come to the author's attention, is the upkeep of the heat exchanger, although due to the size and cost of an installation it would hardly be justified except for a large operation and long heats.

A fairly obvious way to get a hot blast is just to heat it. Practically any good heat exchanger and any kind of fuel can be used. One that has operated on malleable iron for some years in the installation at Fischers in Schaffhausen (Fig. 40) applied to small cupolas with 24 to 39-in. internal diameter and fired with gas to get 1100° F. The combination operates most efficiently on six to eight per cent coke with air heated to 1100° F. Below this amount of coke, not enough blast is required to bring in the necessary heat and if excess blast is used, the iron is severely oxidized. The efficiency of the whole unit is stated to be 59 per cent. The blast temperature is above that of the ignition point of the coke so there is no danger of blowing out the fire. The tonnage is therefore relatively high and due to the small amount of coke used, sulphur pick-up is low.

The Schürmann cupola (Fig. 41) was tried to some extent in Germany but it seems to be just another cupola for the history books now. It is a reversing regenerator, but the slag, coke, iron, etc. was destructive to the checker brick since it was blown right across the melting zone. The latest proposal is due to E. Longden (Fig. 42). This is a revival of the old idea of blowing fuel into the tuyeres with the blast. Part of the air goes through a gas producer *E* and the oxygen is con-

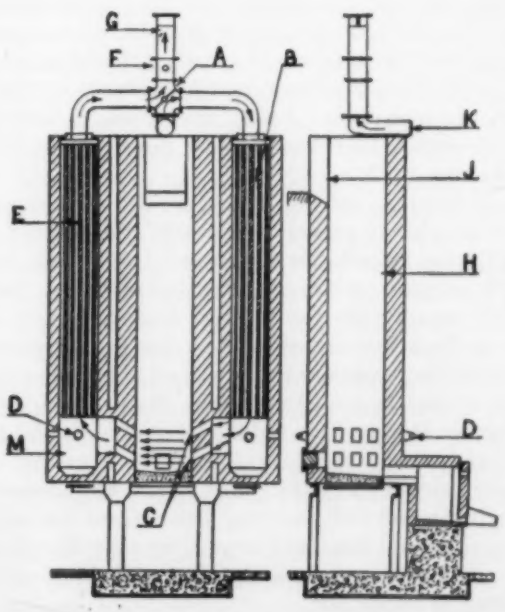


Fig. 41—Schurmann hot-blast cupola; H, cupola shaft; J, charging door; K, cold air from blower; A, reversing valve; B, preheater; E, preheater; C, main tuyeres; D, small secondary air boosting tuyeres to the chamber; M, distributing and secondary combustion chamber; F, throttle valve; G, outlet chimney.

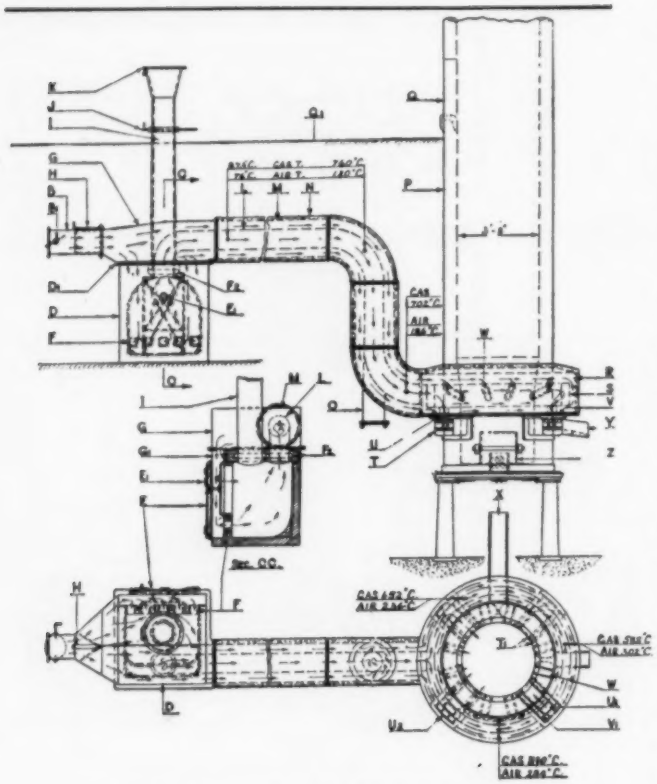


Fig. 42—Longden hot-blast cupola.

verted to carbon monoxide; this then passes through the inner pipe and is mixed with the rest of the blast at the tuyeres. It does not look promising for the same principle has been tried with gas, tar, oil, and powdered coal but all have been abandoned, insofar as the author knows. It was reported that an explosion destroyed the last cupola that was blowing powdered coal into the tuyeres.

In addition to heating the blast, the advantages of which are clearly proven practically and theoretically sound, other expedients have been tried, such as the utterly unsound scheme of recirculating the gas, and/or blowing steam into the tuyeres—both of which were done on the irrational assumption that the temperature in the melting zone was too high and that these devices would spread it out. Drying the blast, which is also reasonable from a heat balance standpoint, was tried early—soon after the experiments on the iron blast furnace proved successful. But it was abandoned for a long time and only recently was revived for making piston rings and has since been reported to be of value with other castings where the close control of chilling tendency is important.

However, some experiments seemed to show that water sprayed into the tuyeres did for a time increase the temperature of the molten metal. E. Piwowarsky explained this phenomenon on the basis of improved heat transfer due to the hydrogen in the gas. Undoubtedly, some moisture is necessary to catalyze combustion but the weight of evidence seems to show that about three grains per cu. ft. is the best value and that the cupola suffers if the moisture is in excess of this amount. It is probably true that most castings are not sufficiently sensitive to chill to justify the cost of dry blast and that the small amount of coke necessary to compensate for ordinary excess moisture is economical.

Pulsating blast has been stated superior to steady blast, according to some work done in France but in the author's plant where we use sometimes a fan with its steady blast and sometimes an epicycloidal blower with its pulsations, we have been unable to see any significant difference.

Wartime developments in cheap oxygen offer some hope of improvement by enrichment and some experiments are now under way along this line. It will depend on whether the higher temperature developed can be put into the iron or whether it will merely result in richer gas. It may turn out to be the best solution of the problem of the decline in coke quality.

In preparing this Charles Edgar Hoyt Lecture, the author spent several days browsing through the early volumes of TRANSACTIONS of the American Foundrymens' Association and the early issues of *The Foundry*. These were the days when W. J. Keep (Fig. 43) conducted a Question and Answer column in *The Foundry*, and E. Kirk was a frequent contributor. Thos. D. West advocated center tuyeres and Dr. Moldenke (Fig. 44) was Secretary of the American Foundrymens' Association and his whiskers were still black. Grand men of the foundry who did wonders for the development of the cupola and the understanding of its operation!

There were still many who derided the chemist in the foundry. Some good humorous verses passed through the pages of *The Foundry* only 50 years ago on that subject. Much activity was under way in the matter of standard samples for the analysis of pig iron—something probably none of the foundry executives here has ever thought about. Actual temperatures are notably absent



Fig. 43—Sketch of W. J. Keep.



Fig. 44 — Dr. Richard Moldenke, Past Secretary of A.F.A.

from their discussions of actual data, nothing about volume except that it was sometimes mentioned as blower capacity. The chief subjects of debate were coke ratios, upper tuyeres, the relative virtues of anthracite coal and coke, and later beehive and by-product coke, and the effect of the weather on melting.

Kirk understood the space theory of melting in 1877, long before Moldenke and Belden's tests proved it, and stated that the piece of coke-piece of iron theory of charging was based on the belief that melting took place all the way up the stack. He mentions the case of light coke; instead of increasing the coke you should reduce the amount of iron in the charge instead. This may be the reason for some of the problems with poor coke today. Outerbridge showed clearly in 1906 the advantage of silicon added to the ladle. W. J. Keep explained the reason for preferring wet coke after the bed was made; that the water prevented, to some extent, the formation of carbon monoxide and since there is always excess heat in the stack it would pay to have a shower bath for the coke pile.

Granted good construction, adequate blowing equipment, with pressure gages and volume control—all readily available today—a good refractory lining is the first concern of the melter. Long ago, the importance

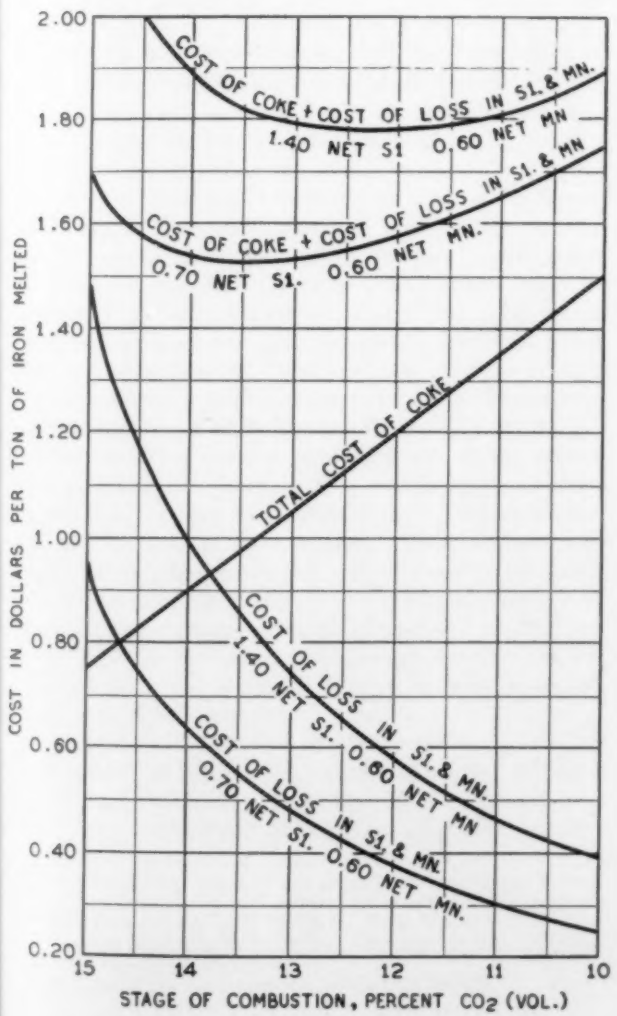


Fig. 45—Stage of combustion vs. melting cost.

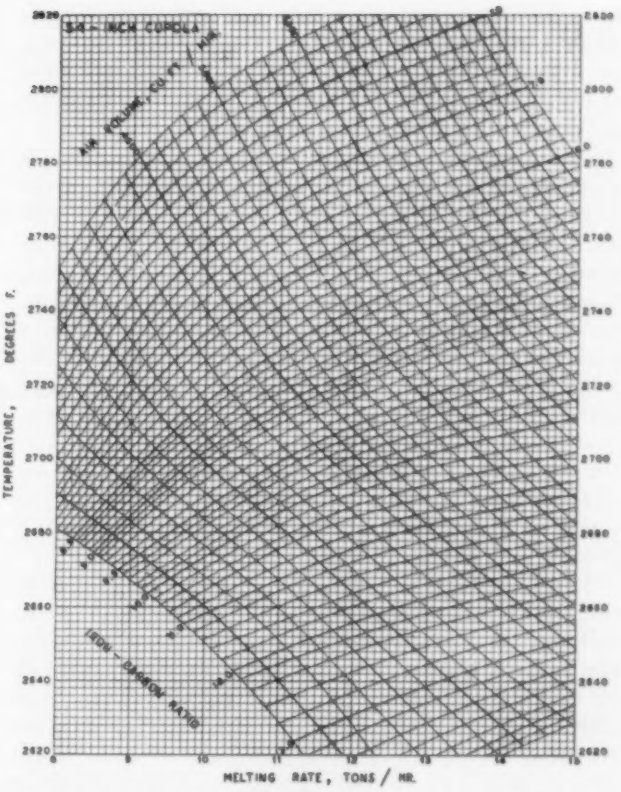


Fig. 46—Operating conditions for a 54-in. diameter cupola.

of good brick laid close together was appreciated but even so a new lining will sometimes leak at the joints and run a "hot spot." A good practice is to lay the melting zone of smaller size brick than the rest of the stack and after drying daub this for the first heat just as for subsequent heats.

Proper sizing of scrap and pig is important but this is solely in regard to the size of the stack, on the big side, and the size of the coke on the little side. Coke these days is coke but usually by varying the blast almost any coke can be made to produce hot iron, though not perhaps in the desired quantities. Clean, small stone is far better even for large cupolas than big stone and dolomite is in our experience better than limestone pound for pound since the atomic weight of magnesia is less than that of calcium. Plenty of stone is usually a saving both in operating smoothness and, by keeping the cupola clean, actually saves the lining.

The fundamental importance of the bed was recognized by all of the old masters and it can not be overemphasized today. If the bed is properly "lit-off" and of the correct height for the size and quality of the coke and the volume of blast, the subsequent charges of iron and coke control, mainly, the temperature of the iron and the melting rate. If the bed is too high a little time and coke is wasted but if the bed is too low, oxidation may be heavy and the whole heat lost. Subsequent charges of coke must maintain the correct height of bed but again if too much is used, the only result is waste of time, coke and power, but if it is not enough, iron begins to oxidize, the slag gets out of control,

tuyeres stop up, refractories burn out and, unless caught in time, the heat may be stopped. Poor coke, an error in weighing, forgetting a charge of coke, one-sided charging, or not enough flux to keep the slag fluid are the chief causes of such occurrences.

John Doyle said in 1905 (*The Foundry*) that "a high melting ratio is like the pot of gold at the end of the rainbow—it had better be let alone rather than searched for." Kirk said in 1904—it is "lots cheaper to buy scrap than to make it." Figure 45, taken from *The Cupola Handbook*, shows actual cost in iron by too much coke saving and Fig. 46 is one of the charts from the same excellent volume which illustrates the amount of the average good foundry coke needed for a given temperature, volume, and tonnage for 54-in. cupola. *The Cupola Handbook* gives a complete set of these charts and they should be used to check every operation. They are not valid for any but average good coke. It will be noted that only by employing high-coke burdens can one get high temperatures in a cold blast cupola.

Much stress has been laid since the Moldenke-Belden tests on the shape of the bed and the thickness of the melting zone found in those tests. Belden's tests were run at fairly low blast volume and P. H. Wilson proved by tests that the shape shown by Belden, an inverted cone, is gradually flattened out and then actually reversed as the blast volume is increased. F. K. Vial has proved that a 12-in. coke charge is not too thick for normal operation. This is not inconsistent with the 4-in. zone found by Belden for as would be expected, and lately proven by Rambush and Taylor, the size of the coke decreases rapidly as it goes down the stack—furthermore the coke used up as monoxide never gets there so the two figures probably mean the same thing.

The author should like to close with a few of the "Cupola Scraps" of E. Kirk:

"Often a melter 'don't know' why the cupola is working badly, because, if he knew he would discharged for carelessness."

"A bad light up makes a bad heat. The bed must be burned evenly or it will not melt evenly."

"Don't burn up the bed before charging the iron."

"A new cupola always effects a great saving in fuel, but it is often hard to find the fuel (saved) at the end of the year. A little more practical knowledge in managing the old cupola will often enable the foundrymen to find the fuel saved and the price of the new cupola besides."

"If you go into the foundry when the heat is being melted and find the tap hole almost closed, the spout all bunged and the melter picking at the spout with a tap bar and running a rod into the tap hole a yard or so, in his efforts to get the iron out, and remark to him: 'You are having some trouble with your cupola today,' he will say: 'Yes, we have some very bad coke today.' or, 'We are melting some dirty pig iron or scrap today, sir!' He never thinks: 'We have a very poor melter today, sir!' "

And last but not least—

"Less fuel is generally required to melt iron in the foundry office than is required to melt it in a cupola."

PRINCIPLES OF PRECISION INVESTMENT CASTINGS*

By

Kenneth Geist [1] and Robert M. Kerr, Jr.[2]

ABSTRACT

The authors thoroughly discuss procedures, materials and equipment for manufacturing investment precision castings. A brief history of investment precision casting relates experiences which preceded industrial application of this casting method. Expansion characteristics of several investment materials are illustrated.

I. History

PRECISION INVESTMENT CASTING, using the lost wax process, has come to us from three different sources. The first was art or sculpture. Probably the first recorded use of this process was made by the Chinese several thousand years B.C. With the exceptions of a few fleeting instances, little is known of the progress of this art until about the 16th century. During the 16th century Benvenuto Cellini cast many of his figures and objects of art using the lost wax process, or as it was known in those days, "cire perdue." Like other of his contemporary artistic countrymen such as Raphael and Stradivarius he kept his method secret, hence the process was lost for several hundred years. The lost wax process, however, was rediscovered in 1897 by a dentist, B. F. Philbrook of Iowa. Real recognition, however, usually is given to W. H. Taggart.

The dental profession provided the second source by supplying complete casting outfit and necessary materials to produce precision castings. Although materials and methods have been greatly improved and perfected, the original process is still essentially the same. The dental profession has greatly enlarged this second source by the constant research carried out by dentists, dental schools, the American Dental Association Research Benefit, the National Bureau of Standards, and the Dental Manufacturers.

In the early 1930's the jewelry trade supplied the third source of information and technique. It was a logical step that this process should be adapted to a related art, the casting of precious alloys for jewelry. The jeweler, while not so demanding in dimensional accuracy, does require intricate shapes with smooth surfaces and minute detail, with the further requirements of many similar castings from one master. They were interested primarily in production of engagement, signet, and wedding rings. The better grades of these rings have always been stamped in two pieces, formed into semi-circles, and then soldered together. This necessitated a very large investment in stamping dies.

The sale of rings is seasonal and new models are brought out each year. The manufacturing jeweler found himself in the unpleasant position of making up a great number of new models with a corresponding number of stamping dies, sending these out with his salesmen to the trade, and having these salesmen report that only two or three of the samples were acceptable to the customers. This necessitated the scrapping of many dies, so the manufacturing jeweler turned to the dentist's process.

The only thing he could not get from dentistry was the method of making thousands of castings all alike, so the manufacturing jeweler developed a permanent die; his contribution to the lost wax process.

Hence we have the information and technique, combined from three sources: The arts, contributing the fundamentals of the process, the dental profession, contributing accuracy, smoothness, and fine detail, the jewelry trade contributing the permanent die as a means of producing thousands of castings all alike.

We now step into the industrial precision investment casting. First, we are going to discuss the various steps in precision casting, and outline the various methods for accomplishing these steps.

II. Steps in the Process

Briefly, the sequence of operation to produce castings starting from the blueprints is as follows:

1. A master pattern is produced incorporating all the shrinkage allowances.

2. A die is produced which is a negative of the pattern.

2A. Frequently step 1 is eliminated and a metal die similar to a die cast and a plaster molding die is made by conventional methods.

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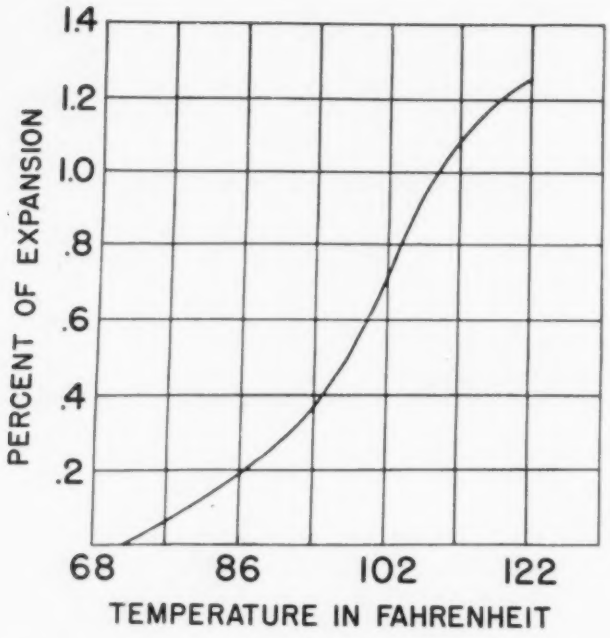


Fig. 1—Thermal expansion of a typical wax.

3. An expendable pattern is produced from the die.
4. A number of patterns are assembled together on an expendable gate to form a group for economical casting.
5. A flask is placed around the group and the investment is poured in.
6. The mold is allowed to set and is then placed in a low temperature oven to eliminate the pattern and help complete the chemical reaction.
7. The mold is placed in the furnace and fired at 800° F. to 2100° F.
8. The metal is melted and cast into the hot mold.
9. The castings are cleaned and given surface treatment.
10. Heat treatment or straightening operations are performed if necessary.

We will now discuss each phase of the process in detail. We shall describe the development of the various operations and the various shop practices.

1. *Production of the Master Pattern*

When a precision caster is considering an item to be made, one of his first decisions is whether to make a master pattern or to sink a die directly from the blueprint. The factors which influences this decision are—

a. Equipment of the tool room.
b. Complexity of the item.
c. Size of the order.

Normally, it takes a rather complete tool room to sink a die. It is usually easier to make a pattern and cast a die from it. However, if the order is large, it is probably more economical to sink a steel die because the die life is much greater than that of the cast die.

The majority of the shops make up the master pattern of die incorporating all the shrinkage allowances. These include shrinkage of the die material, expendable patterns, and metal. There usually is an expansion of the investment so that this must be deducted from the total shrinkage. Some shops make a master pattern to the exact size of the casting and depend upon the expansion of the investment to compensate for all the shrinkages. The pattern, when complete, includes all of the pins, cores, core prints, draft, etc., that are needed. The patterns may be made of brass, steel, cast stone, wood, or other materials. The most reliable, of course, are made from metal.

2. *Production of the Injection Die*

There are several ways to make the dies in which the expendable patterns are formed. These dies are the negative shape of the pattern. When making sample castings or checking on the feasibility of making such castings, there are several inexpensive die materials available. These include rubber, cast stone, and in some cases, cast metal. If accuracy in dimensions is not required, the first two may be used since they can be made in a few hours. The cast metal die is capable of considerable accuracy but will usually wear out after several thousand injections. The steel die is by far the most satisfactory if economically possible, as it will last indefinitely.

Because the soft metal cast die is easy to make and will still give good accuracy we will briefly describe a method to produce one. After the parting line has been decided upon, the master pattern is imbedded in stone or plaster to establish the parting line. It is then dried in a low temperature oven at approximately 250° F. for several hours. A flask is placed over this and the soft metal is poured in. When cold, the stone or plaster is cleaned away and the parting line is trimmed. Another flask is placed on top of the first and a parting compound is applied so that the two halves will not fuse together. The second half is then poured, and when cold, the two halves are separated. It is frequently desirable to apply pressure when casting the die in order to secure complete filling of all the details. This can be done by means of either mechanical pressure or air pressure. It may also be desirable to press the two halves together to cold flow the metal around the patterns. After the two halves are separated, the master

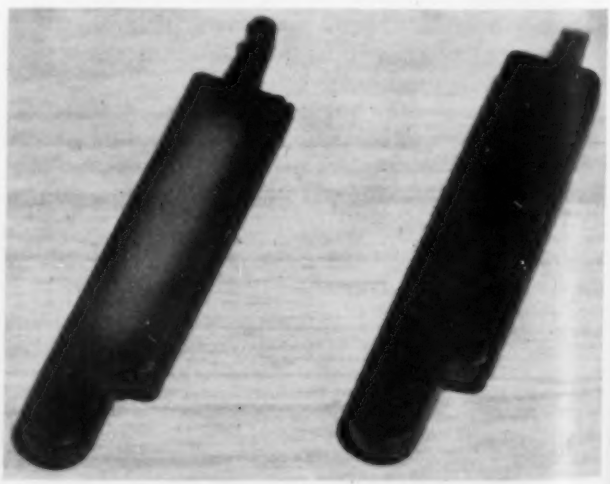


Fig. 2—Showing wax pattern poured without pressure (left) and with pressure (right)

patterns are removed and the gates and runners are cut in and the injection hole is drilled. It is desirable to arrange the die so that the gates used for injection are the same as those used for casting. If any pins or cores are to be used they must be fitted in.

3. *Production of the Expendable Pattern*

As explained in the historical section of this paper, the early sculptors carved their pieces out of an expendable material; the dentist forced the material into the cavities by hand; the jewelers made a die into which the wax was either poured or forced.

At the present time, two expendable materials enjoy the most usage; wax and plastics. Most of our experience has been with wax, so we will discuss the characteristics and use of wax, although many of the treatments will also apply to plastics. We prefer to use wax because of the lower injection pressures necessary, because it is easier to control shrinkage, because it melts out at lower temperatures, and wax does not have the danger of cracking the mold through excessive expansion over a long temperature range.

Properties of Wax

First let us consider some of the properties of wax. Figure 1 is a typical thermal expansion curve of a dental casting wax. This curve will apply equally to waxes used in industrial casting, although the numerical values may vary slightly. You will note that upon heating, the wax expands considerably. Likewise, after injection, the wax shrinks considerably on cooling to room temperature; this is called solid shrinkage. This shrinkage is one of those which must be compensated for when making up the master pattern. In addition, in a complex die there are sure to be distortions, either because certain portions of the die will resist wax shrinkage, or because of variable cooling rates due to the shape cause stresses set up in certain directions.

In addition to the above solid shrinkage, we have liquid shrinkage which manifests itself in dips or depressions in the surface of the wax pattern. This is due to the fact that the center portions of the wax pattern solidify after the outer walls do, permitting the atmospheric pressure to push in the walls. Figure 2, left hand view, shows the wax poured into the die with no added pressure. Note the dips or depressions in the surface.



Fig. 3—Manually-operated syringe for wax injection.

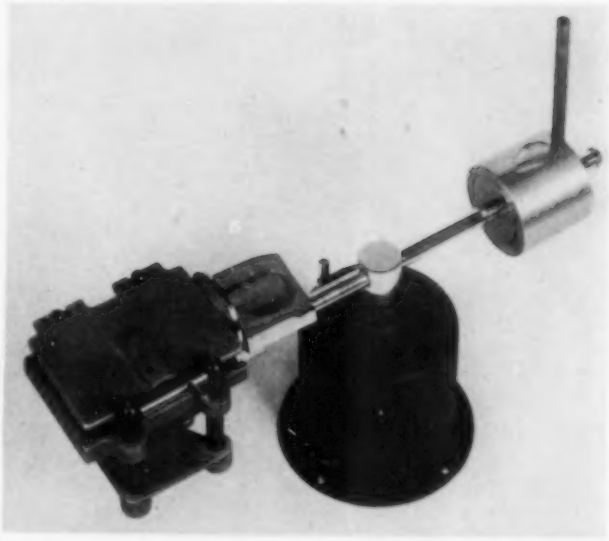


Fig. 4—Centrifugal casting machine used by jewelers.

The right hand picture shows a wax injected at 50,000 psi pressure. Note the absence of depressions.

There are three methods by which we can obviate the volume shrinkage. One is to make a "bastard" die and allow for the shrinkage. The second method is to place a piece of solid wax or internal chill in the die to take up the bulk of the space and thus not allow much liquid wax in the die. The third method is to use very high injection pressures to keep forcing in wax during solidification. Each of these methods has its place and must be used where most practical.

The first method used in filling the die was to pour the wax in. This was perfectly satisfactory until heavy sections were encountered with the subsequent shrinkage problem. The first improvement was the use of the hand or mechanically operated syringe with which 200 to 400 psi pressure could be obtained (Fig. 3).

The jewelers developed the method of centrifugally casting the wax into the dies, obtaining 25 to 50 psi pressure. Figure 4 shows one of these centrifugal casting machines.

Methods of Injecting Wax in Dies

In a majority of the industrial plants the most common method of filling the dies is to force the wax in with air or hydraulic pressure. Figure 5 shows a little device which can be easily built for laboratory or small production work. It is a small grease pump, surrounded by a water jacket, and mounted on a drill press for raising or lowering onto the die. This type of gun will develop pressures of around 1000 psi.

A unit such as shown in Fig. 6 was used for the production of many millions of turbo supercharger buckets. This unit consists of a vertically mounted air cylinder operating on a 1-in. I.D. grease gun. The die was held together in a hold down press and wax was injected at 500 to 1000 psi.

Figure 7 shows a wax injection machine manufactured by an industrial supplier. This machine produces pressures of 8000 psi. In addition to that, it has auto-

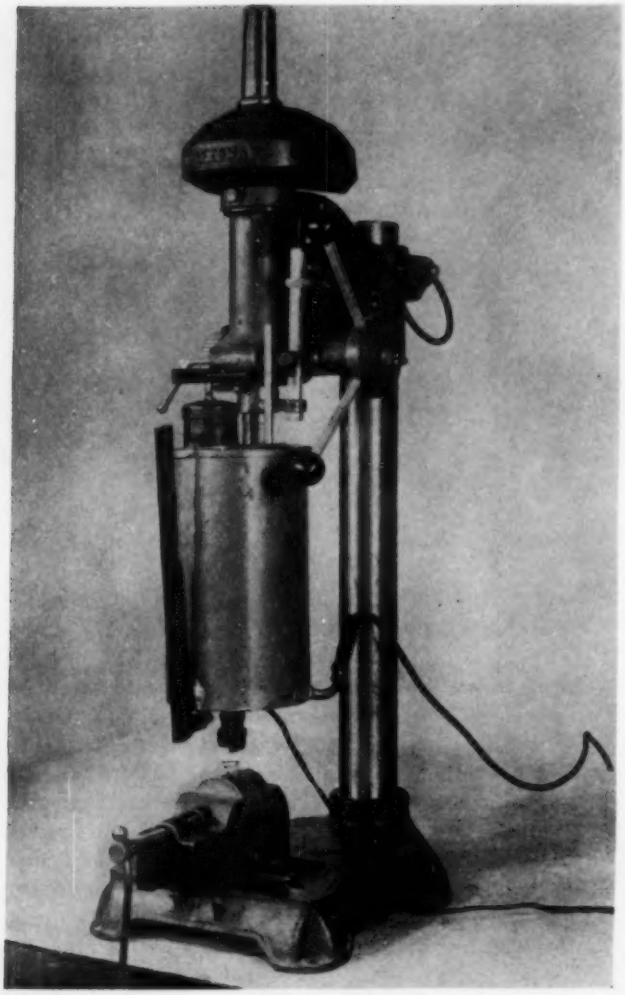


Fig. 5—Laboratory type of wax injector.

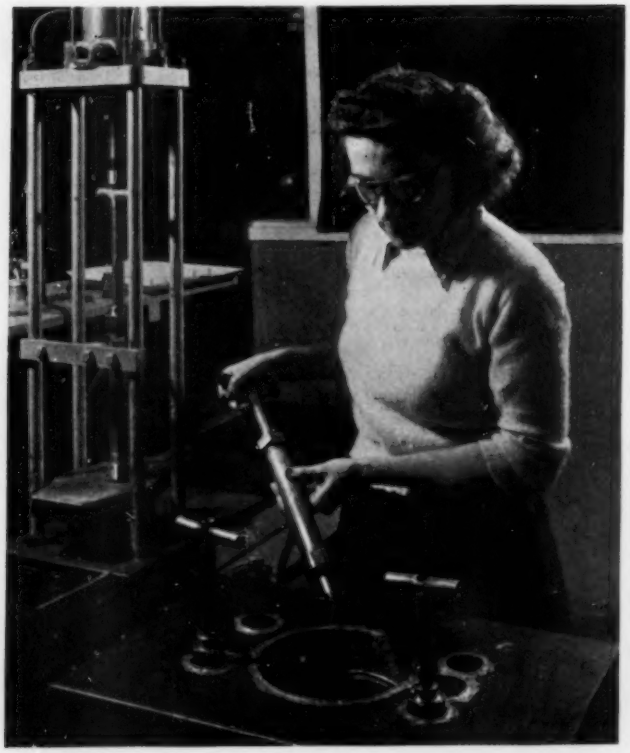


Fig. 6—Grease-pump type of wax injector.



Fig. 8—Hydraulically-operated wax injection machine.



Fig. 7—Industrial wax injection machine.

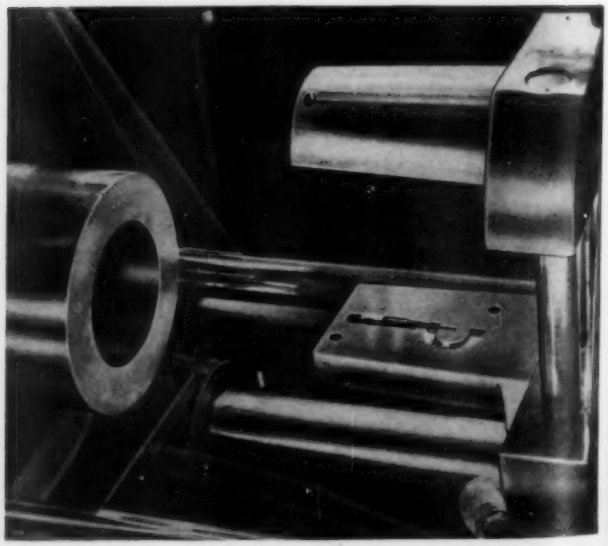


Fig. 9—Two tapered die halves for wax pattern making.



Fig. 10—Assembly of wax patterns.



Fig. 14—Precision investment dental castings.

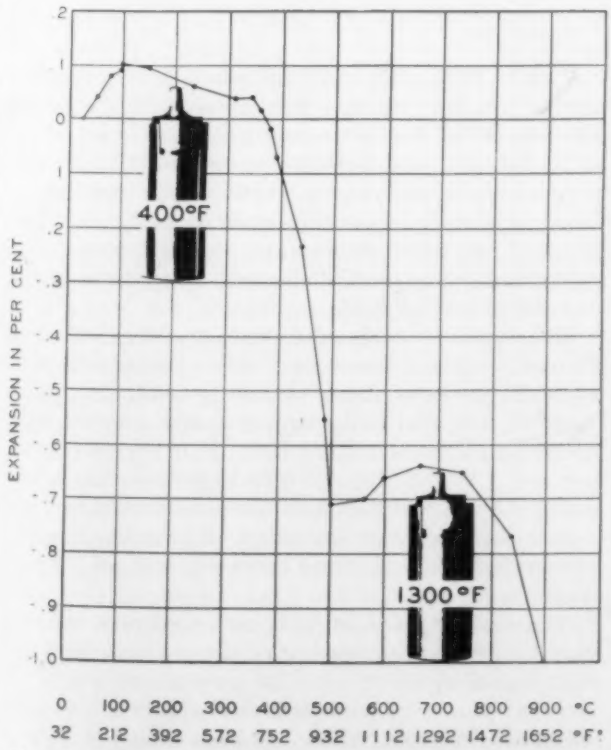


Fig. 11—Thermal expansion curve for 55 per cent plaster of paris investment.

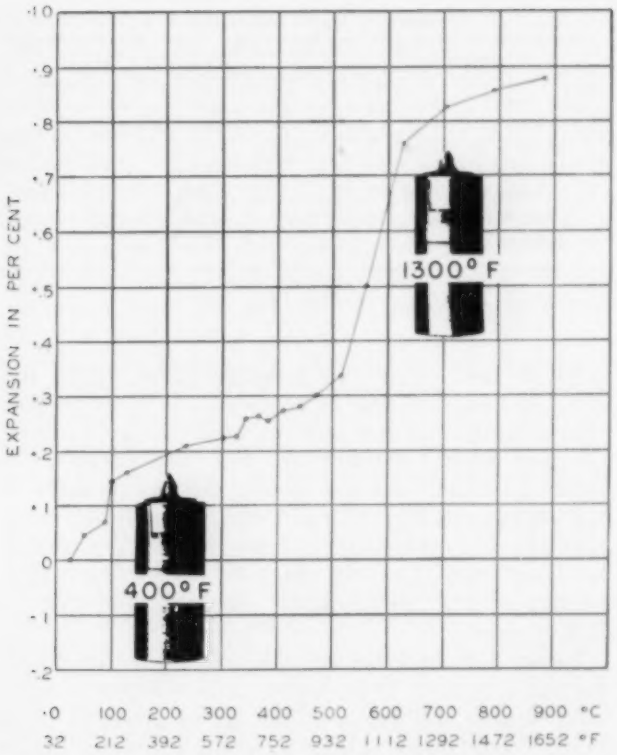


Fig. 12—Thermal expansion curve for 25 per cent plaster of paris investment.

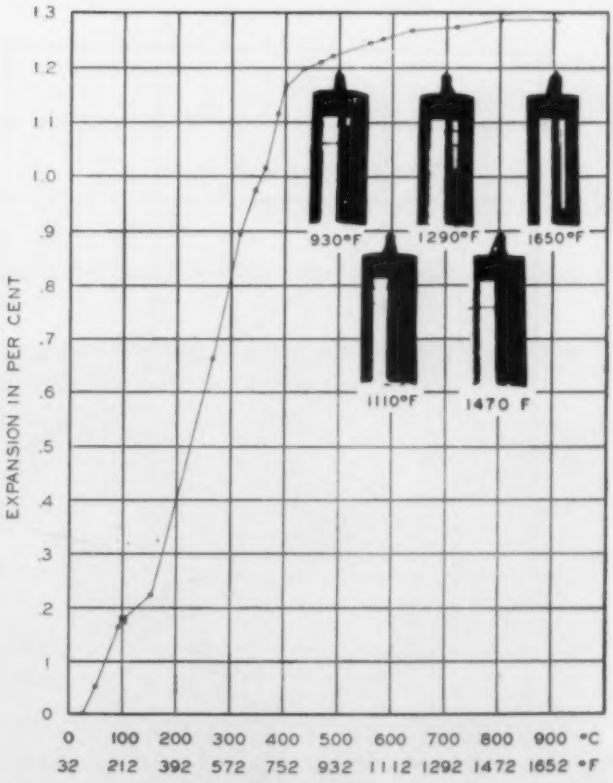


Fig. 13—Thermal expansion curve for cristobalite investment.

COMPOSITION OF INVESTMENTS

BINDER	REFRACTORY
Plaster of Paris	Quartz
Plaster of Paris	Cristobalite
Tetra Ethyl Silicate	Silica
Alumina Cement	Silica
Zirconium Silicate	Silica
Silica Gel	Silica
Sodium Silicate	Silica

Fig. 15

matic hydraulic devices for opening and closing the dies. In this particular arrangement there are six pattern making machines with a central source of heated wax sent under pressure to the dies. The patterns, depending on their size and cooling rate can be made in this equipment from five seconds to one minute each. As many as 20,000 patterns a day have been made. It has been found, however, that 8000 psi is still not high enough pressure to absolutely eliminate shrinkage in large pieces.

Figure 8 shows a machine which has been developed for that purpose. This machine is hydraulically operated feeding the wax into the die at pressures of as much as 100,000 psi in large volumes. Five cubic inches of wax per second are automatically injected under a pressure of 1,000 psi. As soon as the die is filled, a special small ram comes into play exerting up to 100,000 psi pressure on the wax in the die. In order to withstand these pressures, a positive clamping device has to be used. Figure 9 shows the two tapered halves of the die and the tapered ring into which they fit when closed.

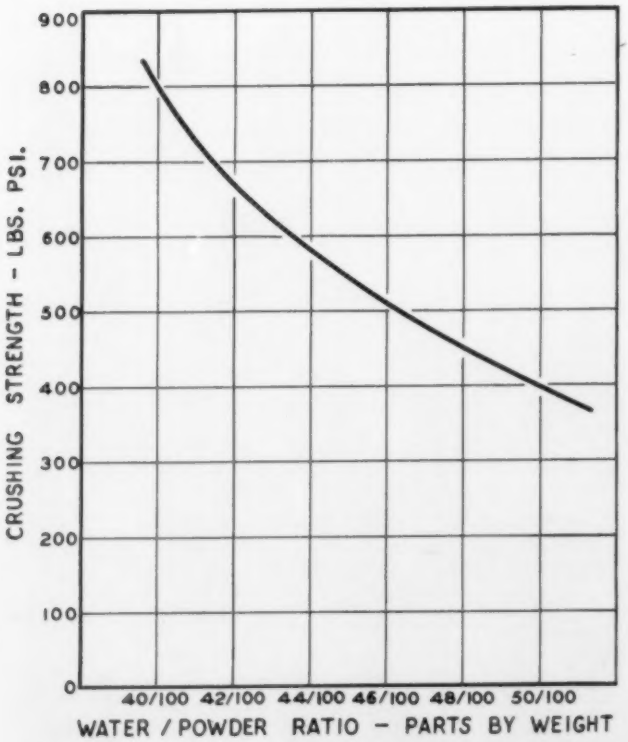


Fig. 16—Industrial cristobalite-water/powder ratio vs. crushing strength for 1-hr. set.

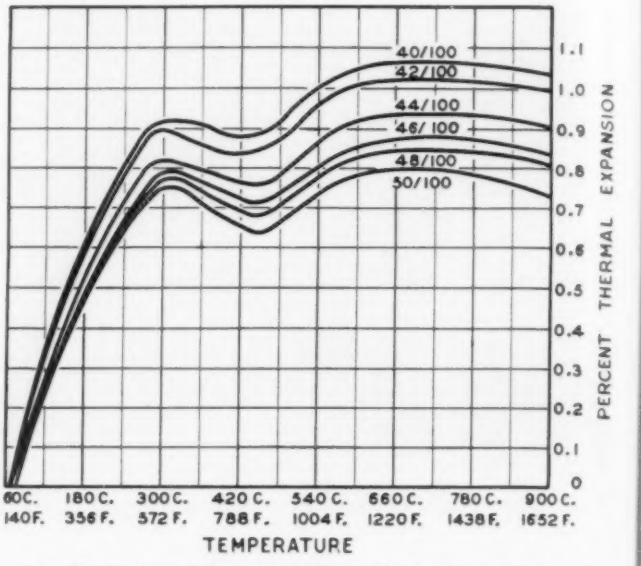


Fig. 17—Industrial cristobalite effect of water-powder ratio on thermal expansion.

This tapered ring provides the positive clamping pressure required.

4. *Assembly of Wax Patterns*

In order to make the process as economical as possible, the individual wax patterns are grouped together to form an assembly. Figure 10 shows a typical assembly of turbo-supercharger buckets. This grouping serves more than to produce an economical setup since it has been proven that the more patterns that can be packed into flask and the less investment that is used, the higher the quality of the castings obtained.

5. *Investing*

Back in 1907, 1908 and 1909, when the dentists first started making castings, they considered themselves fortunate when they got a casting at all. They were not at all familiar with investment compounds nor with proper melting and casting techniques, so they did not notice that their inlays did not fit properly until they began to lose teeth. As a result, a research commission was established in the U. S. Bureau of Standards to find out why inlays did not fit.

The explanation was the shrinkage chart shown in Figure 11. Dental investment of that period contained about 55 per cent plaster of paris as a binding agent. You will note that as the temperature is increased, the investment shrinks until at 1652° F. it has shrunk one per cent. Add to this the shrinkage occurring in the metal and you will find that there is a total of between two to three per cent shrinkage. Obviously, the casting would be too small and leakage would occur which would cause decay.

The next step was to make an investment material that did not have so much shrinkage. Figure 12 shows the expansion curve with investment containing 25 per cent plaster of paris and the balance a refractory material. In place of the shrinkage shown in Fig. 11 we see an expansion taking place.

In all modern investments the refractory material is



Fig. 18—Dipping of wax patterns in thin slurry.

a form of silica. The silica expands, the plaster shrinks, so in order to get higher percentages of expansion, it is necessary to use more silica. This curve (Fig. 12) represents about the maximum that can be expected with quartz, although certain modifiers such as chloride salts can be added to increase the expansion slightly.

In 1932 it was found that by substituting cristobalite for the quartz, an investment having high expansion characteristics could be made. Not only is the expansion high but it is obtained at the relatively low temperature of 572° F. By this research work the dentists were able to use an investment which would compensate for metal shrinkage and insure an accurate fit of inlays (Fig. 13).

Figure 14 shows the results obtained in the dental castings. Not only is accuracy necessary, but it can be seen that to maintain the delicate carvings and narrow knife edges, the investment must be easily removed from the casting. Hence, dentistry has contributed three things to the casting process; accuracy, smooth surface, and investment compounds which could be easily removed from the castings.

Classes of Modern Investments

The modern investments should be divided into two classes; those used for low temperature work, and those used for high temperature work. Low temperature investments are used for metal with pouring temperatures of 2000° F. or below, and which contain no iron. High temperature investments are primarily used for metals that are poured above 2000° F. although they may be used for low temperatures also.

Figure 15 contains a list of binders and refractories. Plaster of Paris is the binder used in about 95 per cent of all the investments used for low temperature casting.

The principal binders used in high temperature investment are silicagel which may be obtained from tetraethyl, silicate, aluminum cement, zirconium silicate, sodium silicate, and various phosphates. The principal refractory is silica, either in the form of quartz or cristobalite. Other refractories are aluminum oxide, magnesium oxide, or grog. In the use of most investment compounds, they are mixed with either water or some chemical, and then are allowed to set. The first property we want to consider is what happens to these materials when the proportions of water or liquid chemical are varied.

Figure 16 shows properties of a cristobalite investment mixed with water. The ratio of water to powder is plotted horizontally, and it varies from 40 parts water in 100 parts investment to 50 parts water in 100 parts of investment. We plot the crushing strength in psi vertically. You will note that by changing the ratio from 40 parts of water to 50 parts of water, we have cut the crushing strength in half.

Now, we might have done development work on the castings and used 40 parts of water having a crushing strength of 800 psi. If a centrifugal casting machine was used a force of 550 psi acting on the fluid metal might be developed. According to Fig. 16 this mixture would withstand that force. If, in production, however, no particular care was taken in measuring the water, the workmen might have used as much as 50 parts of water, thus reducing the crushing strength below that force exerted by the incoming metal. As a result, the mold would crack and the cores break and wash away. This illustrates the importance of sticking to a procedure once it is established.

Figure 17 shows what happened to the expansion curves when the liquid content varies from 40 to 50 parts per 100 parts of investment; that is, the expansion has varied 25 per cent. Hence, in order to maintain tolerances it is very important that the proportioning of the investment be closely controlled.

Methods of Investing Mold

There are three methods of investing that are commonly applied:

Number 1, primary coat and secondary coat, or



Fig. 19—Quantity production of precision castings.

double investment;

Number 2, single investment, which is vacuumed and poured; and

Number 3, single investment which is poured and vibrated.

In the first process a thin slurry of very finely ground refractories (200 + mesh) and binder is made into which the wax patterns are dipped (Fig. 18). This thin coating is allowed to air-dry after which a coarser slurry is poured into the flask around the coated patterns. In order to assure complete packing, the entire flask is placed on the vibrating table.

The advantage of this process is that a much cheaper secondary investment can be used. The disadvantages are that very small holes cannot be bored out, and that occasionally portions of the dip coat or primary investment peal off giving a rough surface at that spot and introducing loose foreign material into the mold. Since no vacuum is used it is necessary for the patterns to be arranged in the flask so that no air is entrapped, causing bubbles on the casting.

The second or mix, vacuum, and pour method is usually used for very fine detail and when small holes are required, since this is a single investment. The investment is mixed in a relatively low speed mechanical mixer, such as a commercial dough mixer, to avoid beating air into the mix. As soon as the mixing is completed the bowl is covered by a bell jar and vacuum is applied until the air is drawn out of the mixture. The deaerated investment is then poured into the flask, and if desired, revacuumed to remove any air trapped during the pouring operation. A smooth pattern is assured by this method, because there will be no air bubbles against the wax pattern to be later converted into nodules of metal when the metal is poured.

Figure 19 shows this process being done on the production basis. The mixing is shown at the left. The mix is then placed under the bell jar and vacuumed. It is then poured and allowed to stand until set.



Fig. 20—Investment vacuum processing.

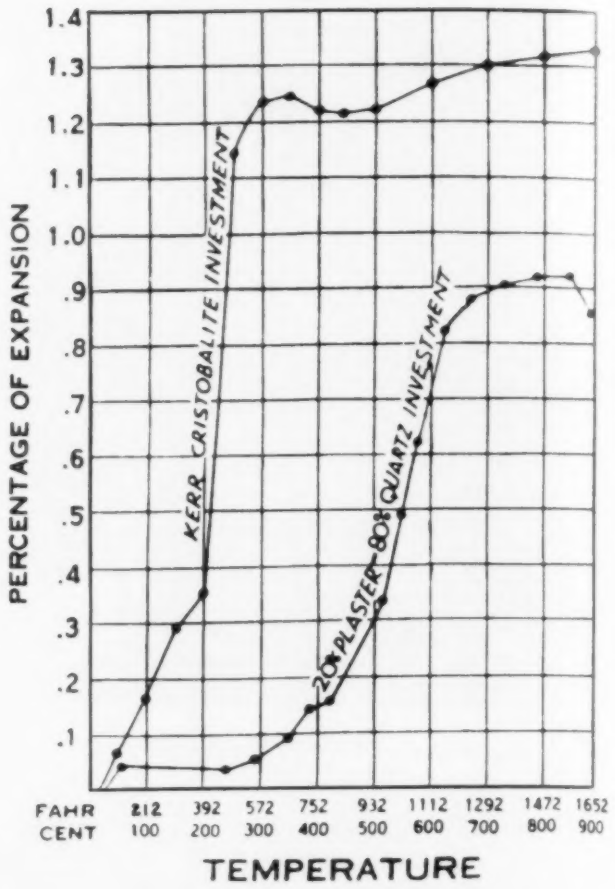


Fig. 21—Comparative thermal expansion for Kerr cristobalite inlay investment and ordinary high expanding investment.

Figure 20 shows the air bubbles rising out of the mix during the vacuuming process.

The third method of investing also employs a single type investment, but it is used in a much thicker consistency than is used in a mixed, vacuum, and pour method. Hence, in order to insure a good pack, it is necessary to vibrate the flask containing the investment. Little more need be said about this type of investment.

It is highly important that the physical properties of the investment being used is thoroughly understood because not only are the tolerances affected but many defects in the casting are caused by improper handling of the flask. In Fig. 21 are shown expansion curves for two low temperature non-ferrous investments both having high expansion qualities. If we should choose a mold temperature of 400° F. on the cristobalite curve, we would have an expansion of 0.35 per cent. Now, if by some mistake or failure in the controls, we should heat our molds to 570° F., we would have an expansion of 1.25 per cent or an increase in expansion of 0.90 per cent.

On the other hand, if we were to choose a mold temperature of 1300° F. we would have an expansion of 1.3 per cent. If, by a similar mistake, we heated the mold to 1450° F. we would only have a change in expansion

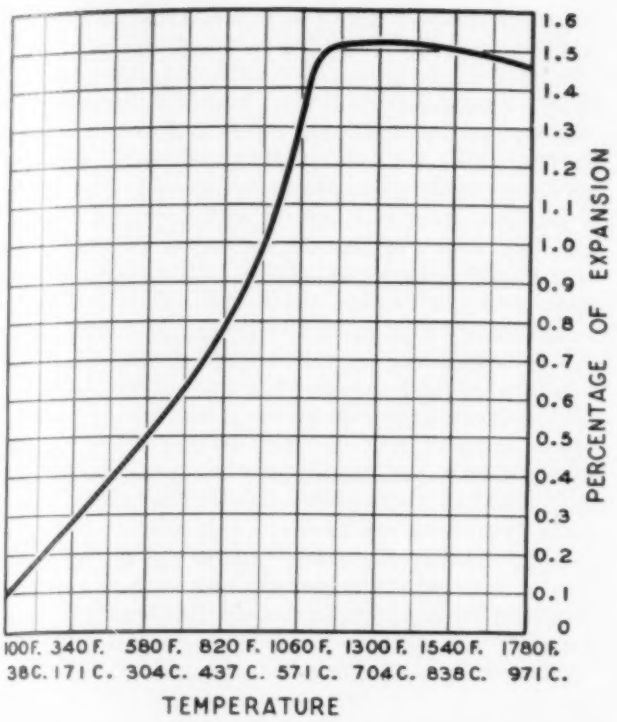


Fig. 22—Thermal expansion of ferrolite investment for ferrous and high-temperature alloys.

of about 0.02 of 1 per cent. Obviously, in the production setup, good results could not be obtained in the temperature ranges where the expansion curve is rising rapidly. A flat section of the curve should be chosen. This is also advantageous because it is sometimes necessary to hold the mold at casting temperature for longer periods than scheduled, due to mechanical trouble in the casting process. If the curve is flat, there is little danger from prolonged heating.

The rapidly rising expansion curve points out another danger which must be avoided; namely, too rapid an increase in temperature. Since nearly all the investment compounds are poor conductors there would, of necessity, be a very steep temperature gradient between the outside and the center of the molds during rapid heating. Hence, due to the rapid changes in expansion, there would be stresses set up in the investment which would tend to crack and warp the mold. There is also the possibility of generating steam faster than it can escape, resulting in spalled surfaces due to excessive steam pressure.

The above properties were those of a non-ferrous investment, but the principles apply to all the investments. The expansion curves are all quite similar (Fig. 22). Those investments having silica-base refractories have the added hazard of inversion points through which the heating rate must be slow. The magnesia- or zirconia-base refractories do not have these inversion points.

Various formulas for investments and binders are shown in Table 1.

6. *Wax Elimination*

Subsequent to the investing operation and prior to firing, most of the wax is eliminated from the mold by any one of several methods. Those investments containing water as a carrier can be placed in a low temperature circulating air oven. Those investments containing inflammable carriers such as alcohol should be dewaxed by a low pressure steam jet applied to the wax sprue. It is quite important to eliminate a good portion of the wax before firing because the wax usually expands more than the investment, and hence, when it soaks into the investment it can generate enough pressure to crack or spall the mold or cause the surfaces to buckle.

7. *Firing the Mold*

There are several reasons why the mold is fired. The first reason is to eliminate the last vestiges of the expendable pattern, either wax, plastic, or metal. There is always some wax left in the mold after it has come off the steam tables or out of the low temperature oven. This can only be eliminated by burning completely which is difficult to do on a production basis at less than 1200° F.

The second reason is to provide a hot mold so that the metal will run into all the farthermost corners. Since many pieces cast have very thin webs or fine detail the mold must provide every advantage for the metal to run.

The third reason is that part of the linear shrinkage of the metal and wax can be compensated for by the expansion of the investment. As a matter of fact, some investments are compounded so that minor dimensional errors may be corrected by varying the mold temperature.

There are two general methods for firing the mold. The first is the batch type of furnace. Batch furnaces are quite versatile since the heating cycle can be varied quite easily to suit the particular mold. They must be cooled down to 200 to 300° F. each time they are loaded,

TABLE 1—INVESTMENT FORMULAS

	Per Cent
No. 1 [8]	
Plaster of Paris	30.0
Silex (powdered)	70.0
No. 3 [8]	
Paster of Paris	50.0
Powdered mica	25.0
Marble dust	25.0
No. 5 [10]	
Plaster of Paris	30.0
Cristobalite	50.0
Tridymite (and silica)	20.0
No. 7 [12]	
Calcined gypsum	80.0
Asbestos fiber	20.0
No. 9 [16]	
Silica	67.0
Liquid (tetraethyl silicate 8 vol; water 1 vol.)	33.0
Liquid (alcohol 1-2vol.; hydrochloric acid (few drops))	
No. 2 [9]	
Plaster of Paris	52.0
Marble dust	16.0
Graphite	16.0
Soapstone (powdered)	16.0
No. 4 [9]	
Alpha gypsum (21)	30.0
Silica (200-400 mesh)	62.0
Andalusite (fine)	5.0
Alundum (fine)	1.0
Boric acid	2.0
No. 6 [11]	
Calcined gypsum	30.0
Silica	69.0
Strontium chloride	1.0
No. 8 [14]	
Plaster of Paris	60.0
Silica	25.0
Talc	15.0
No. 10 [17]	
Silica	90.0
Magnesia	6.0
Monobasic ammonium phosphate	3.0
Monobasic sodium phosphate	1.0
Liquid—water or 10 per cent hydrochloric or nitric acid	

NOTES: Numbers of above formulas refer to type.
Formulas 1 to 3 are old types of dental investment of low expansion.
Formulas 4 to 6 are new types dental investment of high expansion.
Formulas 7 and 8 are mixtures for plaster casting. Formulas 9 and 10 are investments for stainless high fusing alloys.



Fig. 23—Pusher type preheating furnace for firing mold.



Fig. 24—Push through tube furnace for firing mold.

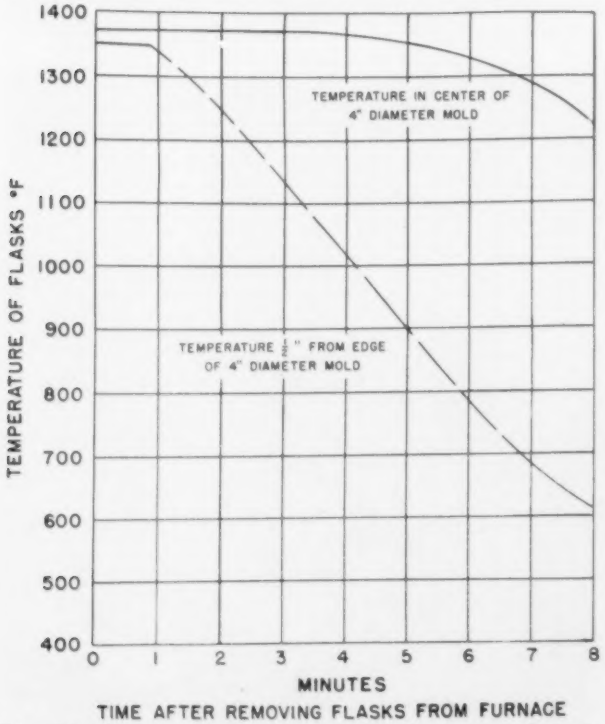


Fig. 25—Temperature decrease in Kerr casting flasks upon removal from furnace.

hence considerable time and fuel is wasted in heating and cooling the furnace. The refractory life is less, too, because of the temperature variations. These furnaces may be heated by gas, oil, or electricity.

The second method is the continuous or pusher type furnace. This is the ideal production furnace if the sizes of the molds do not vary too much. If production is sufficiently high this furnace will operate 24 hours a day with no loss of time for cooling or loading. The cold molds are loaded at one end, progress through the furnace in three to five hours, and emerge at casting temperature. The big disadvantage of this furnace is that it cannot handle mixed molds varying greatly in size, because the heating cycle cannot be varied easily. It is obvious that it requires more time to raise an 8-in. square mold to 1800° F. than it does a 4-in. square mold. Hence, the most ideal setup for a production shop would be a pusher type furnace with two or three batch furnaces as auxiliaries.

Figure 23 shows a gas fired pusher type furnace used very successfully. The pushing arm is pneumatically operated. Figure 24 shows a specialized type of furnace which is somewhat on the conveyor type except the small sizes of molds allow the operator to push them through.

When the molds are up to temperature they should be cast immediately upon removal from the furnace. Figure 25 illustrates the difference in temperature between the center and ½ in. from the surface of the mold during an interval of 8 min. exposure to the air. Recalling the results obtained by too rapid heating during the rapid expansion stage of the cycle, the same results can be obtained through rapid cooling. Stresses are set up due to the contraction of the outer portion of the mold resulting in cracking of spalling. Hence it is important to cast within a minute or two after removal from the furnace.



Fig. 26—Blow-pipe type of metal melting.

There are some instances, however, when it is necessary to cool the mold to a lower temperature. Some of the non-ferrous metals cannot be poured at high temperatures, so that it may be necessary to cool the mold to less than 400° F. In this case due allowance must be made for the shrinkage by cooling the mold slowly to reduce the temperature gradient as much as possible.

8. *Melting and Casting*

The earliest castings of the dental industry were made by melting with a blow pipe. Many dental and some jewelry castings are still made this way. Figure 26 illustrates the blow pipe type of melting, using a dental type centrifugal casting machine. This melting is good only for a very small melt and for metals which do not readily pick up gases from the flame.

Actually, the metal can be melted in any one of these conventional methods, or in any of the specialized melting machines, developed for this industry. Figure 27 shows crucible melting which may be done in either gas or electric furnaces. In this case the metal is poured from the crucible either into the mold or into the casting machine. Figure 28 shows melting by high frequency. This, of course, has the advantage of *accurate* control and high temperature metals can easily be melted. In any method of melting, good metallurgical procedure should always be followed, such as control of atmosphere, introduction of inert gases, and other recognized good procedure.

The actual casting of the mold varies greatly from shop to shop. In some cases the molds are poured statically; in other instances centrifugal casting machines, air pressure machines, or vacuum machines are used. The first dental castings were made in pressure machines; by pressure we mean air or gas pressure. Figure 29 shows an air pressure machine with an air tank on the bottom, and a hand pump for pumping up the pressure. About 40 psi pressure was produced by this machine.

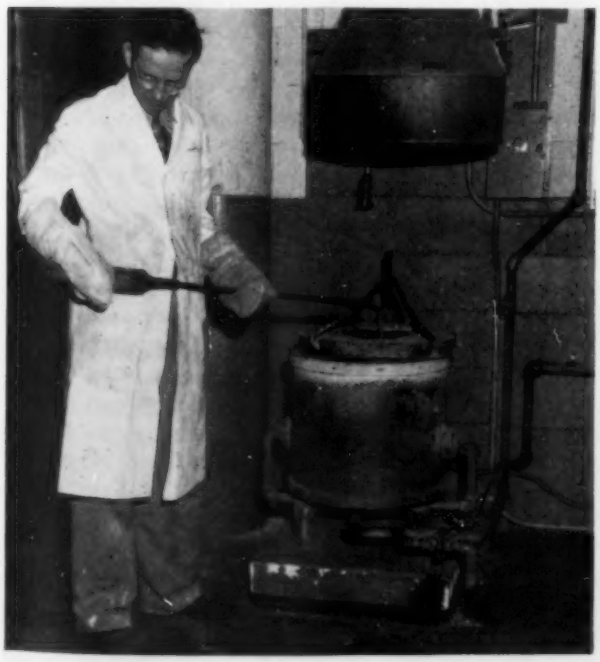


Fig. 27—Melting metal in crucible.

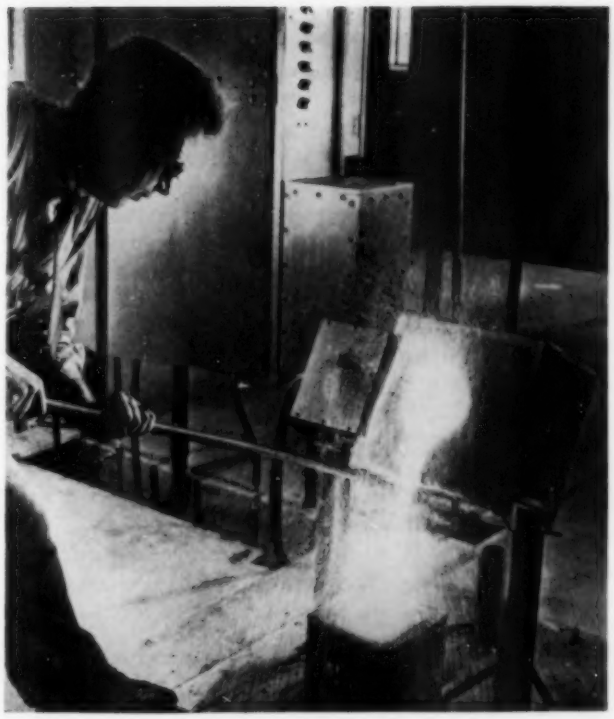


Fig. 28—Pouring molten metal into mold from high frequency electric furnace.

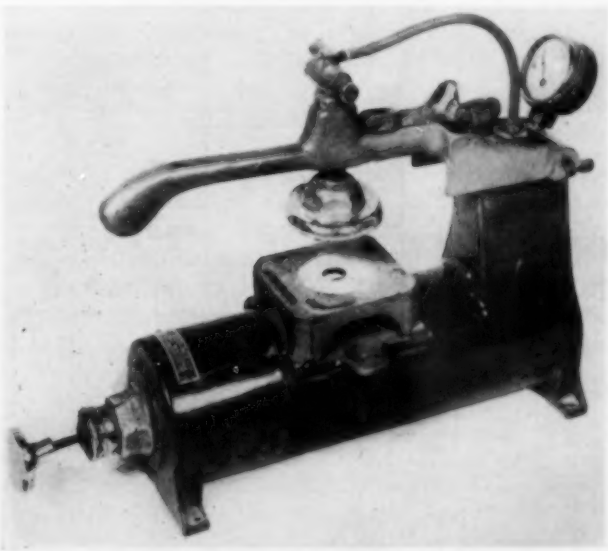


Fig. 29—Air pressure casting machine.

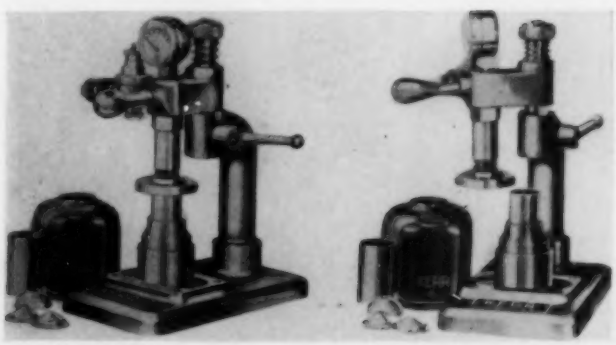


Fig. 30—Second type of air pressure casting machine.

The mold is placed upright on the platform, metal melted on top of the investment, and machine closed, causing the cap to seal on top of the flask, at the same time allowing the valve to open allowing the air pressure to act on the molten metal forcing it into the mold.

Figure 30 shows another type of air pressure machine using air directly from the air line, giving up to 100 psi pressure. The machine is more automatic in operation. As soon as the metal is melted, the arm is swung over to automatically drop the cap into position, opening the air valve.

Of course, in any type of pressure machine, a good seal must be maintained between the flask and the source of pressure, and in large diameter molds, this seal is a problem, because as the area increases, the amount of force necessary to keep the cap clamped onto the flask also increases, and very high pressures may be necessary. For this reason, in large castings, only a very low gas pressure is used.

Figure 31 shows another type of machine using air pressure. In this case the metal is melted in the crucible, completely enclosed and melted by an arc. A small opening leads to the top of the enclosure and the mold is clamped in an inverted position at the top of the enclosed crucible. When the metal is melted, the whole device is inverted, the metal runs down the small lead

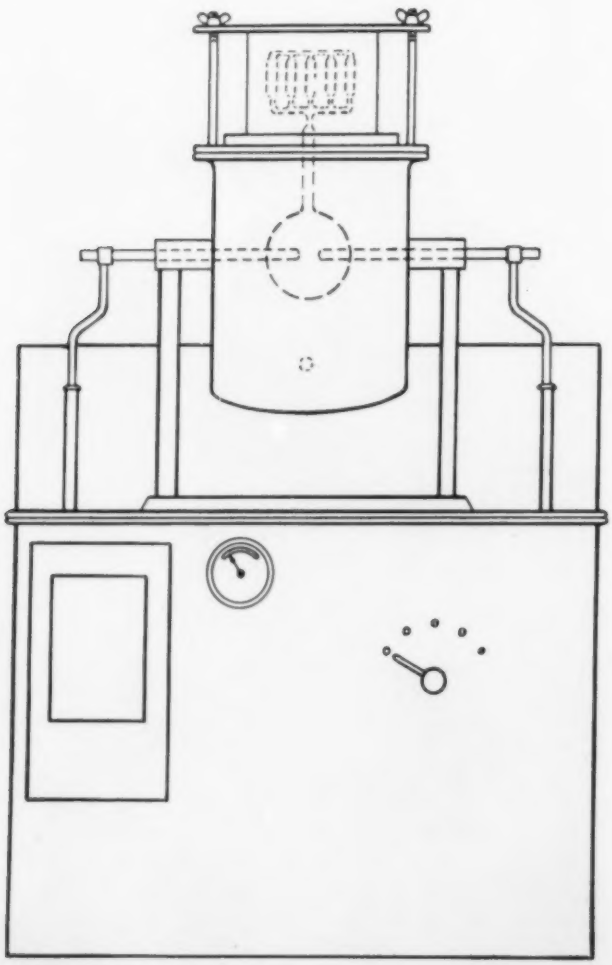


Fig. 31—Third type of air pressure casting machine.



Fig. 32—Vacuum type casting machine.

to the mold and air or gas pressure is introduced behind the metal completing the casting. Three to fifty pounds would probably be the maximum amount of pressure used in this type of machine.

In Fig. 32 we have a machine employing vacuum in the casting. Of course this can be used in combination with air or gas pressure on top. Obviously air pressure makes the casting, because when the vacuum is created, the atmospheric pressure forces the metal into place. We have a maximum pressure of say 14 psi. Here again a good seal must be obtained.

The second type of machine to be used is the centrifugal casting machine. Figure 33 shows a dental type of machine, in which very small castings may be made. The large arm with a jointed secondary arm attached to it is spun by spring pressure, the metal being thrown from the crucible into the mold. A motor may be substituted for the spring pressure.

Figure 34 shows a very common type of production centrifugal casting machine. In this case, the crucible is located in the center and usually four or more molds are placed around the crucible. The whole device spins, and as it is spinning the metal is poured into the crucible and from the crucible it is thrown out into the mold. Now this may seem like a very logical way of centrifugal casting, but it has many disadvantages.

In the first place, a tight seal must be obtained between the crucible and the flask, otherwise metal will escape. Secondly, as the machine is loaded, the flasks cool rather rapidly and a careless workman could allow them to cool considerably causing inaccuracy in dimensions. It is usually rather difficult to get these molds into place without jarring them, and damage to the investment may result. Finally, it is almost impossible

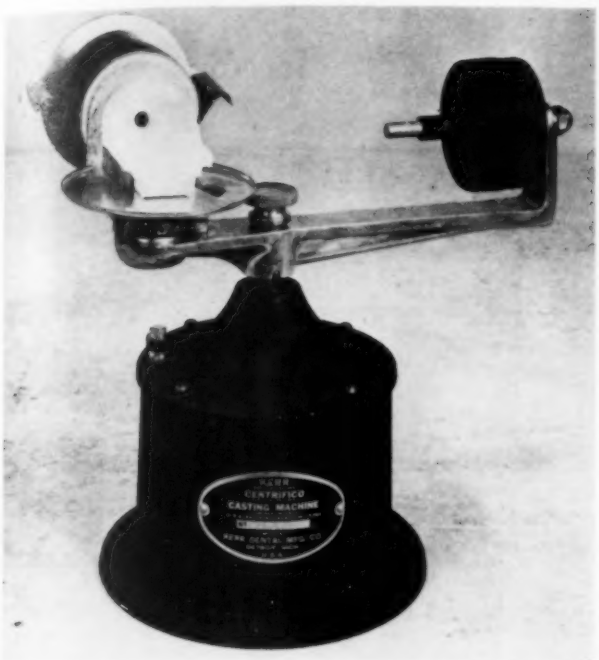


Fig. 33—Dental type centrifugal casting machine.

to balance this type of casting machine because of the unequal speed with which the four molds are usually filled due to the way the metal is poured, the shape of the crucible, and the fact that usually the cavities of the four molds are not exactly the same, and more metal will enter one than the others. This throws the machine off-balance, causes vibration, and the vibration may cause hot tears in the casting, as well as dirt from the broken parts of the mold.

Centrifugal casting machines are used primarily because higher pressure can be developed than are possible in the air or gas type pressure machines available. As a matter of fact, the pressure in any centrifugal casting machine is limited only by the strength of the investment. Also the force is in the metal itself rather than being applied from behind, so that as long as there is molten metal there is pressure being applied. In a pressure type casting if a leak should occur then all the pressure would be lost.

Figure 35 shows a schematic diagram of a vertical tubular type of a production centrifugal casting machine. Here we have a tube with a shaft around which it rotates; *b* is a crucible full of molten metal, *a* is a mold inverted on top of the crucible, *c* is the retaining cap to hold the mold in place, *f* is an adjustable weight to counter-balance the crucible and mold so that the machine will be in balance.

This machine operates by revolving, and obviously the first action is not centrifugal but pouring as the tube inverts. In other words, *t* goes down to where *g* is on the first half revolution. The metal is poured from the crucible *b* into the opening of the mold *a* so that a very gentle action is obtained. As the tube continues to revolve and picks up speed, the centrifugal force is brought to bear with the least possible disturbance to the metal. The result, of course, is a sound casting with no vibration.

In some machine shops no casting machines of any sort are used. These shops depend upon the static pressure of the metal to complete the casting and to feed the shrinkage.

10. *Cleaning and Surface Treating*

When the molds are cold the castings are knocked out and cut off from the sprues. The gates are cut down flush with the castings according to accepted foundry practice. After the gates are cut off and ground the surface of the castings is frequently treated in order to give a better appearance. This is done by either treating with a very fine sand blast, tumbling, or wheelabrating.

III. Plant Layout

Since precision casting is a tool, in fact a precision tool, it should not be entered into with little capital. Like all precision work, a well equipped plant is necessary. By well equipped, we mean the plant must not only have proper machinery and personnel for making castings, but should also have a good engineering department, a good estimating department, proper cost accounting personnel, and at least one experienced metallurgist. It has been estimated that even for a small plant, the installation cost would be $25,000 to $50,000 and at least that amount in working capital.

Many of the troubles of precision casters are traceable to firms that have tried to get along on a shoe string and have not done true precision work. Many of them were doing little better than glorified sand casting.

Figure 36 shows a typical flow sheet with the layout of most of the equipment required.

IV. Design Factors

It is not possible at present to set forth definite formulas for casting design. It is, however, possible to state that castings made in conformity with certain basic principles will be capable of economical production.

The first basic principle is; the piece should be designed so that progressive solidification can take place. This means that the casting section should be so pro-

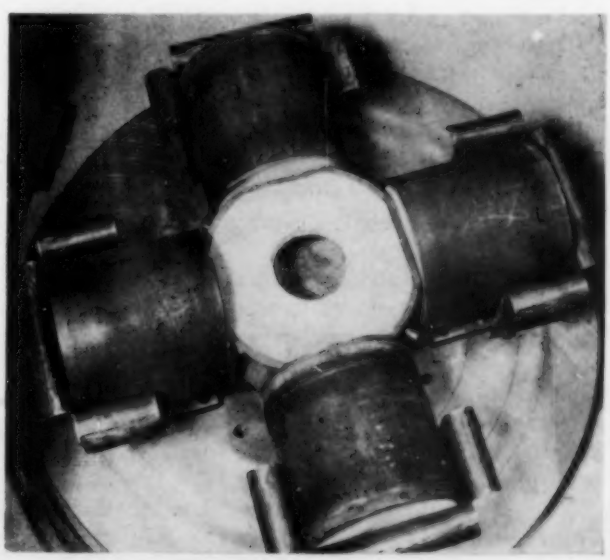


Fig. 34—Commercial type centrifugal casting machine.

portioned that the sections most distant from the point where the casting is to be fed, will solidify first, with the solidification progressing toward the gate or risers where the hottest metal is located. With this in mind, the designer should be extremely hesitant to isolate any appreciably heavy sections or locate them where they cannot be adequately fed, since the use of chills, or any other such artifices, as are commonly used in the regular foundries, are either difficult or impossible to use in the lost wax process. Further discussion on this problem will be found in the section covering metallurgical problems.

The second basic pronciple to be observed is the linear shrinkage or the contraction occurring during cooling from the solidification temperature to room temperature. This is the contraction that ordinarily is compensated for by the pattern makers shrinkage allowance. Casting should be so designed that essential mold rigidity and resistance due to irregular casting shapes will not result in severe contraction stresses which, due to the different cooling rates of thin and heavy sections could easily result in hot tears or severe residual stresses.

The best practice in designing castings is to use the minimum section thickness that will provide the necessary strength. These sections must not, however, be so thin as to cause difficulty in filling out the casting. The excessive temperatures required in this case will cause poor physical properties.

The best time to think about utilizing the precision investment casting process is before the piece is on the drafting board. When it is in the planning stage, a part can be modified to fit whatever production method is selected for it. Changes leading to reduction in cost and improvement in its performance are easier to make. Since the same laws of physical metallurgy which apply to large castings, apply to precision castings, it is a good idea to make the drawing 5 or 10 times actual size and treat the part just as you would treat a large casting. Design it into the strongest and lightest weight and thinnest section casting adequate for its service factors, using ribs, fillets, and all other strengthening design members in it.

Since the cost of the materials is almost negligible, the piece should be designed so as to utilize the strongest obtainable alloys, or the corrosion and abrasion resistant factors of the most resistant metals in accordance with the service conditions which the piece is to meet. It is also a good policy to draw in the gating system on the enlarged print, since the gating and risering problems of precision casting are almost identical to those of the larger castings.

When the enlarged drawing appears to be satisfactory, it should be reduced to actual size and rechecked to make sure that the casting is within the limitations of the process. If found to be outside of those limitations of the contracting shop, the drawing should be corrected. Following this procedure, anyone who can design for regular castings, can probably design for precision casting.

At the present time the average shop seems to be set up to cast no dimensions larger than 5 in., no weight of a single casting greater than 5 lb., and no section continuously thicker than ½ in. or thinner than 0.030 in. The typical precision investment casting weighs ¼ lb. or less, has no dimension longer than 2 in., and has no thickness greater than ½ in. Prices per piece or per lb. of completed castings seem to be lowest in this range, the price rising out of proportion to the increase in size in larger pieces. The dimensions given above are by no means the limit since many larger castings are being made.

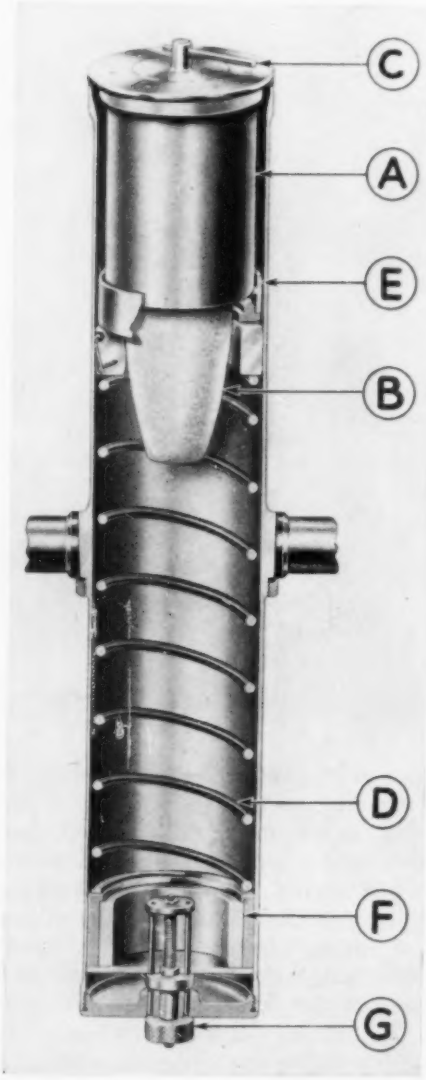


Fig. 35—Vertical tubular type centrifugal casting machine.

There has been considerable misunderstanding as to the thickness of section which is castable by this process. Briefly, it can be stated that sections 0.25 in. long x 0.125 in. wide have been cast 0.005 in. thick. Sections 1 in. long x 1 in. wide have been cast 0.025 in. thick. Sections 0.040 in. thick seem to be castable to any length and width that the investment flask will take. When a section is not a continuous thickness, but is in successive gradient of thickness, it is practical to bring the thinnest edge down to 0.015 in. or even less. The steam turbine blade is a good example of this.

Holes, recesses, and chambers of almost any size and shape can be cast as long as the pins or loose pieces

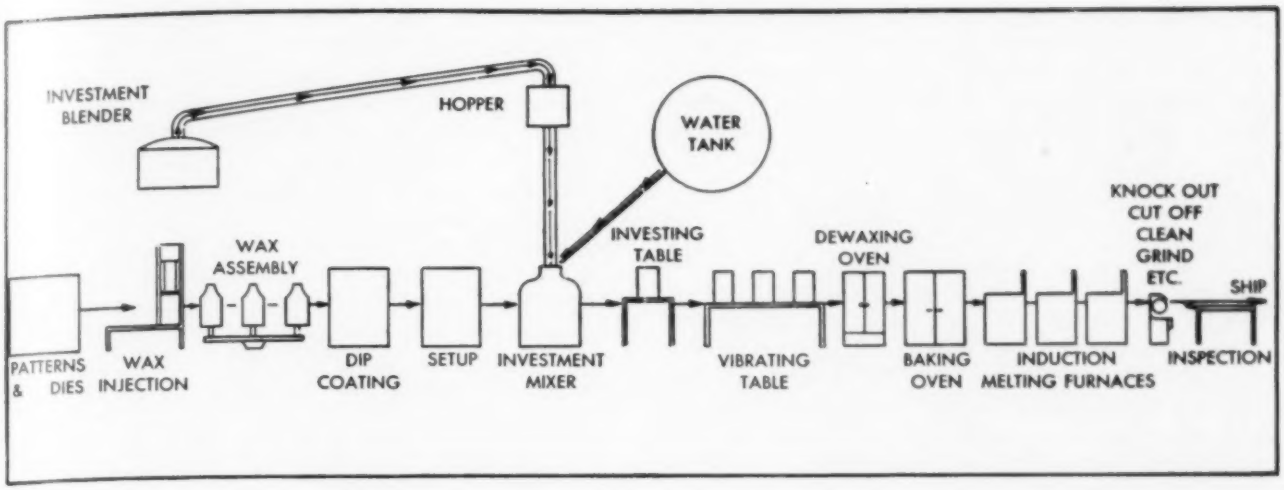


Fig. 36—Flow sheet for production of precision investment castings.

can be removed from the wax pattern. Small holes down to 0.050 in. diameter can be cored into castings by almost any shop. Even smaller diameters can be produced, depending upon their lengths, their locations, their depths, and whether or not they are open at both ends so the tiny rods of investment, which will keep them open as the castings are poured, will be supported at both ends. Threads, either internal or external can be cast, although it is rather difficult to cast threads less than 12 threads per inch, or smaller than ¼ in. thick at the roots. Very seldom are there accuracies closer than a Class 2 fit. Since the process is generally limited to accuracy in order of plus or minus 0.003 in. per inch of dimension, it is easier to cast contours of thread, and of gear teeth of similar shape than it is to hold their accuracy.

There seems to be little doubt as to the details of contours and finishes which this process can produce. Dimensional accuracy of these contours is another matter, however, accuracy can be of several kinds, dimensional, parallel, angular, contour, and surface finish. Since most of the accuracies on current blueprints are those to which the highly accurate machine tools automatically cut the casting producers are very wary of the whole subject of accuracy. The problem is not that of providing necessary accuracy, but of meeting customer's specifications of unnecessary accuracy.

The question of what tolerances can be held in casting is probably the most frequently asked question a precision caster must answer. The average of the whole process would work out to "as cast" tolerances of + .003 in. to + .006 in. per inch with plus or minus 0.001 in. on the small dimensions the very finest tolerance, but with plus or minus 0.0005 in. or even 0.00025 in. obtainable on some dimensions made of certain alloys. The non-ferrous alloys generally seem to be castable to finer tolerances than the ferrous alloys. No tolerance finer than plus or minus 0.003 in. should be demanded unless there is a compelling reason for it. If a blueprint calls for a few very close tolerances that cannot be held during the casting operation, a secondary operation migh be feasible and still make the casting economically profitable.

An important factor in considering tolerances is the factor called size relation. Figure 37 showed two simple geometric figures, a 1-in. cube and a 12-in. bar. Normally, in machining, it would be just as easy to hold a tolerance of plus or minus 0.010 in. on either one of these figures, but in casting it is not. Let us assume that the metal we are to cast has a 2 per cent shrinkage and no provision is made for this shrinkage in the casting. If the 12-in. piece would shrink 0.240 the 1-in. piece would shrink 0.020, so it is going to be a much more difficult task to hold the tolerances on the long piece than it is on the short piece. This is what we mean by size relationship. Figure 38 illustrates this same relationship. Dimension *A* would present no problem since it is very small. Dimension *B* would, however, and so would the other dimensions on this piece. This is because the blueprint states "unless otherwise shown, tolerances on decimal dimensions are

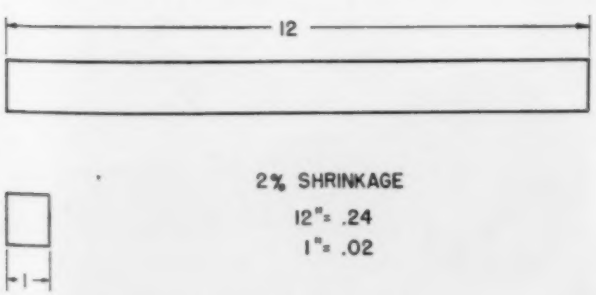


Fig. 37—Illustrating size relationship.

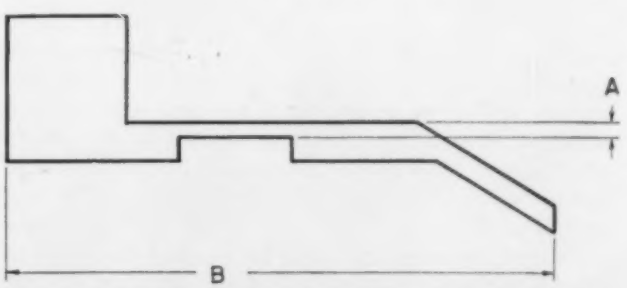


Fig. 38 — Dimensional tolerances (unless otherwise shown, tolerances on fractional dimensions are ± 0.010).

plus or minus 0.010 in. That is easy to hold on dimension *A*, very difficult on *B*, so that if one can allow greater tolerances on *B*, be sure and indicate that to precision casting plant. If close tolerances are necessary in *B*, expect higher castings costs than if they are not.

V. Metallurgical and Foundry Factors Affecting Design

Probably one of the most important metallurgical problems is the determining of the proper gating system for the casting. This problem is very complicated in some cases, especially if wax is used as an expendable pattern. What frequently may be the most ideal gating system as far as solidification of the metal is concerned, may be entirely impractical as far as assembling the wax patterns and the investing is concerned. One of the chief disadvantages of using wax as a pattern material is its fragility. If investments requiring vibrating in order to secure a pack are used, it is quite necessary that the wax assembly be very substantial to resist the tendency to break during the vibrating period; hence, many types of bottom gating and small gates are not practical because they are not strong enough in the wax assembly. If an investment requiring vibration to eliminate air and provide the necessary packing is used, it is necessary to arrange the pattern in the mold so as to avoid entrapping any air which might rise from the mixture. This positioning in the mold frequently forces the metallurgist to compromise on his gating system.

The most important function of the gating system is to provide entry for the metal into the mold so that the progressive solidification can take place. The metal in the farthest part of the casting should solidify first and then solidify progressively toward the gate so that any porosity due to shrinkage will be confined to the gates and risers rather than in the casting. If the metal is fed through a thin section into a thick section, the thin section would solidify first leaving nothing for the thick section to draw from, hence the thick section would be porous. Therefore, the thick section should be closest to the gate or to the sprue. If this is impossible, blind buttons or risers should be placed on the heavy portion. The gates into the casting should be as short and bulky as possible so that they will solidify last producing a solid casting. One of the great advantages of this process is that almost any metal which is castable can be cast into one or more of the molds employed by the process.

There are some problems, however, which are not found in other methods of casting which must be watched. First of these is that certain metals attack the surface of the molds. Those molds containing considerable amounts of plaster are frequently attacked by metals containing more than 5 per cent lead. A lead sulphide film is formed on the surface of the casting which, when sand blasted off, leaves a relatively rough finish. Some of the low alloy, low carbon steels such as SAE-1010, or SAE-1020 also frequently attack the surface of the mold resulting in a pitted and very irregular surface. It appears that the addition of only 1 or 2 per cent alloys to this steel alleviates this condition permitting the obtaining of a fine surface. The surface of the mold is also attacked by some waxes and any quantity of iron in the alloy will be badly attacked by sulphur if investment contains plaster. Pouring at extremely high temperature also results in a pitted and slagged mold surface due to the fusing in of the mold surface to the casting.

In some cases the slow cooling of the mold also produces a problem. This slow cooling results in a very coarse grain structure of the metals, and in cases where a fine grain structure is necessary, additional heat treatments such as normalizing are required, in order to refine the grain size.

Another factor which must be dealt with, when using metals dependent upon carbon to produce hardness, is the decarburized layer of the surface of the casting. This decarburized layer may be anywhere from 0.005 in. to 0.020 in. in depth. There are several ways to overcome this problem. The first is to inject an inert gas or a reducing gas into the mold just prior to the pouring operation. The second method is to restore the carbon in the decarburized layer of the casting, either in a salt bath or by some carburizing treatment. If the casting is to be machined it is also possible that the decarburized layer may be removed during the machining operation.

VI. Design

In the next three figures we wish to discuss the feasibility of casting three different types of parts. Figure 39 shows a part which was normally made on a screw machine with a secondary drilling operation. In any sizeable production, that would not be a feasible precision casting proposition, because it could be turned out very cheaply by a screw machine operation, but if you wanted 50 of them, you might do them more cheaply by precision casting using a soft metal die, rather than to take up the time to set up a hand screw machine or lathe. So, for a limited production or model making a sample to try out a new design, precision casting would be satisfactory. The cheapest method with any sizeable production quantity would be on the screw machine. Figure 40 shows a forging and a screw machine part. Precision casting would not make the forging cheaper, except in very limited production. The screw machine parts obviously, could not be made cheaper except in very limited production. However, if the two were combined and cast in one piece, there is a possibility of making the piece more cheaply by precision casting than by the combination of forging and screw machine parts. Hence, it might be possible to make this part cheaper by precision casting, simply because of eliminating assembly operation (Fig. 41).



Fig. 39—Precision casting replacing machined part.



Fig. 40—Forging and screw machine part.

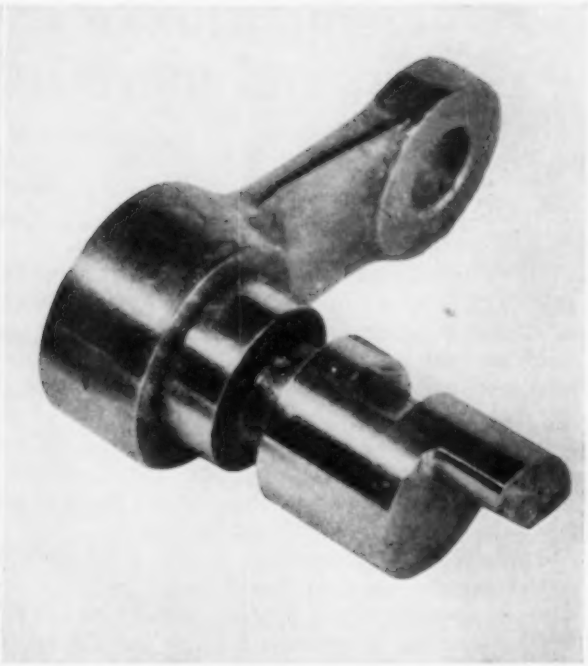


Fig. 41—Components in Fig. 40 made integrally by precision casting.

Figure 42 shows an agitator vane and clamp for a paper stock pump, which is ideal as a precision casting, either in small or large quantities. According to the design of the vane, it is obvious that to machine these splines would be a costly operation, even in fairly high production. As a precision casting, the two parts can be fitted with no machine operations at all.

VII. Summary

To summarize, precision casting is an ideal process for producing intricate parts in either small or large quantities, with smooth surfaces, with metal and physical properties characteristics equivalent to machined parts, and to fairly close tolerances. It is especially useful in the higher temperature metals such as steel, and the refractory metals used in the gas turbine and jet turbine fields. The harder the metal the more advantageous is the application of precision casting. Likewise, the more machine operations which can be eliminated, the more advantageous is the application of precision casting. It is not an inexpensive process such as die casting.

Remember that in die casting, the die casting plant has the casting completed in the same manner of steps as the precision plant takes to make a wax pattern. A precision caster injects wax into a metal mold and then starts from there. A die caster injects metal into a metal mold and the casting is completed. Precision casting can handle practically any metal that can be cast at all, and handles with ease metals that are difficult or even impossible to fabricate economically by other methods. It is not a process that just anyone can operate, either in his garage or in a factory. In order to produce quantities of precision parts, elaborate and expensive controls must be set up and maintained. It is these controls which account for the major portion of the costs on precision castings.

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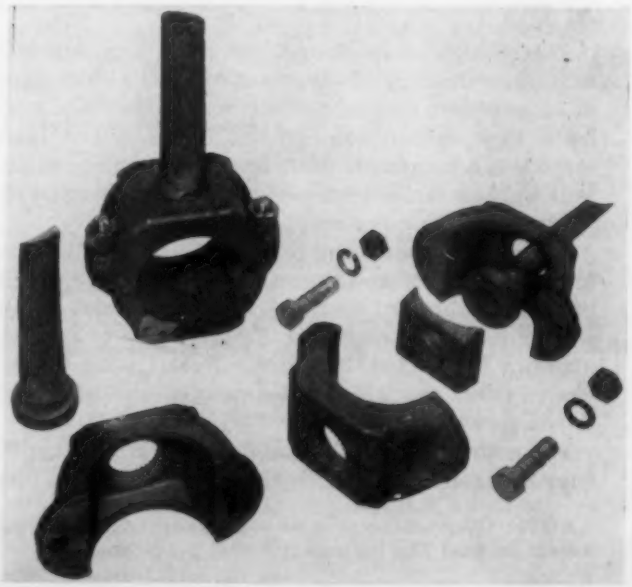


Fig. 42—Agitator vane and clamp for paper stock pump.

PRODUCTION OF CARBON AND ALLOY STEELS BY THE SIDE-BLOWN CONVERTER PROCESS

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THE PAST FEW YEARS HAVE WITNESSED a marked revival of interest in the side-blown converter method of steel making, and much research and development work has been carried out. In the years just prior to the war, steel founders in the United Kingdom were able to give special attention to the equipment of their foundries. Several steel foundries replaced their old side-blown converters with better plants of modern design and construction, and thought was given to possible improvements in operating technique.

Little reliable information on the side-blown converter process was to be found in the technical press, and, in consequence, a fresh approach was made. A serious shortage of hematite pig iron and the necessity for the utmost economy in the use of imported ferrosilicon during the war stimulated more general interest by all converter users in the possible improvements and economies in operation. The use of the immersion pyrometer for the determination of the bath temperature of liquid steel has proved to be of great assistance in the recent development work.

Molten Metal for Subsequent Conversion

Hot molten metal of regular chemical composition is essential for the successful production of steel by the side-blown converter process. The cupola is the normal melting unit employed, and in a general way the melting practice calls for little comment. The cupola charge normally consists of hematite pig iron and steel scrap. A scrap content of 60 per cent was, until recently, looked upon as a good figure, but now figures of 80 or even 100 per cent (where the plant is suitable) are being obtained.

Where high scrap charges are used, it is desirable to deepen the well of the cupola in order that metal of an adequate carbon content be obtained; the need for a high carbon content will be explained later. Higher sulphurs may result from a deepening of the well, but this can be remedied by desulphurizing by the sodium carbonate process.

Double desulphurizing reduces the sulphur content to very low limits, and figures of 0.010 to 0.015 per cent have been obtained in this way. The normal silicon of cupola melted metal for converter use is approximately 1.5 per cent, but recent research has demonstrated that a much lower figure can be worked to with satisfactory results.

For many years P. C. Fassotte (who is well known among American and British steel founders) has stressed the importance of carbon in obtaining a satisfactory temperature increment during conversion of metal into steel in the side-blown Bessemer process, and during the last war important plants were laid down in Great Britain which made use of this important function of carbon.

Steel scrap charges were melted in cupolas, and the extremely low-silicon molten metal after desulphurization was transferred to pulverized-coal-fired rotary furnaces. The metal was superheated to approximately 1,500 C, and was then transferred to side-blown converters. The resultant blows gave satisfactory temperature increments and the steel produced was of high quality and fluidity, and was suitable for the mass production of light, thin walled steel castings.

In the orthodox operation, molten metal at a temperature of 1,250 to 1,350 C, containing approximate percentages of C, 3.00 to 3.50; Si, 1.50; Mn, 0.50, is used in the converter. The oxygen of the blast, which impinges on the surface of the metal, reacts with the metal according to the laws (a) mass and (b) chemical affinity. As soon as the blast is put on, heavy brown fumes of iron oxide are seen leaving the mouth of the converter along with metal projections.

Conversion Process

Iron oxide formed on the surface of metal then reacts with silicon, manganese and carbon according to their relative chemical affinities at the temperature prevailing. The slag which is first formed is high in iron oxide, but it becomes increasingly silicious as the blow proceeds, and the iron oxide (FeO) in the slag at the end of the blow is generally within the range 20 to 25 per cent. The normal blow lasts 15 to 18 min.

In the case of the Fassotte operation, which only lasts 9 to 12 min, the molten metal at a temperature of 1,450 to 1,500 C and containing C, 3.00, Si, 2.4, Mn, 0.50 per cent, reacts immediately with the oxygen of the blast, forming excess iron oxide which escapes as fume, the remainder reacting immediately with the carbon. The small percentage of silicon present is insufficient to provide silicon to form a slag, and unless precautions are taken the free iron oxide rapidly attacks the converter lining materials. Silica sand thrown on the surface of the metal prior to putting on the blast provides the free iron oxide with silica with which to combine.

The temperature increment obtained from a normal blow (1.5 per cent silicon metal) generally is 350 to 450 C, whereas in the case of the low-silicon iron it generally averages 200 C. It is obvious that the modified blowing procedure using superheated low-silicon

NOTE: Official exchange paper to the American Foundrymen's Association from The Institute of British Foundrymen, 1947.

metal is more economical owing to the saving in silicon, in the duration of the blows and in lower chemical and mechanical losses. For plant reasons many founders are unable to take advantage of these potential savings, but it has been found possible even when using ordinary cupola melted metal to work with lower silicons in the metal than are generally used and still produce hot fluid steel of high quality.

In the case of cupola melted metal poured to and in the converter at a temperature of 1,250 to 1,350 C, it would appear that the function of the silicon is that of a kindling agent, the heat produced by its oxidation raises the bath temperature to that at which the carbon reaction commences. It therefore needs to be determined what percentage of silicon is essential to give the temperature increment necessary to allow the carbon reactions to proceed. The results of the following experiment may be of assistance in this connection.

A blow of 48 cwt was stopped after 8 min and the changes in the chemical composition and temperature were noted. The heat was poured into a ladle and allowed to cool to 1,460 C. The metal, after a slagging, was poured back into the vessel and the blow recommenced. The bath lit off immediately, blew steadily and finished in a normal manner. The heat was slagged, the ferro alloys were added and the steel cast quite satisfactorily from a stoppered ladle. The chemical changes are shown in Table 1.

TABLE 1—CHEMICAL CHANGES IN EXPERIMENTAL BLOW

	Elements, per cent				
	C	Si	S	P	Mn
Metal in vessel.....	3.16	1.03	0.034	0.033	0.43
Metal after 8-min blow.	2.56	0.14	0.033	0.035	0.14
Losses.....	0.60	0.89	——	——	0.29
	(Increase in temperature, 195C)				
Metal poured to vessel and blow recommenced	2.56	0.14	0.033	0.035	0.14
End of blow 3 min later.	0.08	0.05	——	——	0.04
Losses.....	2.48	0.09	——	——	0.10
	(Increase in temperature, 180C)				

Temperature Increment

It will be seen that in general terms and under the conditions of the experiment, the oxidation of 0.90 per cent silicon, etc., gave an initial temperature increment of 190 to 200 C, and from 2.50 per cent carbon a further increment of 180 C was obtained. The reason why the qualification "under the conditions of the experiment" has been made is that the test was made in a plant where the volume of air per minute to the converter was less than that generally used.

It was found possible to check over these results in a different plant and in a modified form. A series of heats was blown in an acid-lined side-blown converter and the metal and temperature changes were noted. A fresh series was blown for 3 min, and the original metal composition and temperature together with slag and metal compositions and bath temperature after the 3-min blow were noted. A further series was blown for 6 min and similar data obtained. A series of 9 and 12 min and end of blow determinations were also made. A study of the data obtained reveals the general information shown in Table 2.

It will be noted that the rate of temperature change is highest during the second 3 min of the blows, and thereafter tends to fall off rather sharply. The rate of oxidation of silicon is reasonably uniform over the first 6 min, whereas the rate of oxidation of carbon is highest at about 9 min of blowing. Heats of metal of the chemical composition and weight given above generally finish in approximately 15 min blowing time, and during the blows a temperature increment of 360 to 370 C is obtained.

A study of the foregoing results again demonstrates the economy in the use of silicon and of reduction in blowing time that can be effected if cupola metal of high initial temperature be used, and the results also demonstrate that lower silicons in the bath than are generally used can give good temperature increments.

It should be the aim of side-blown converter users to bring down metal in the cupola of standard chemical composition and of as high a temperature as possible. If the metal temperature be determined by means of an immersion pyrometer it should be possible, knowing the chemical composition of the metal, to predict the final bath temperature at the completion of the blow.

This makes possible the production of standard temperature steel by the side-blown converter process. If it should be necessary to obtain temperature increments higher than normal, controlled additions of ferrosilicon (based on metal composition and weight) can be made during the correct part of the blow, or by addition to the bath prior to putting on the blast.

During the past few years efforts have been made to use photoelectric cell control units to determine the end point of heats made by the side-blown converter process.[2] The quality of product made by the process

TABLE 2—SECOND EXPERIMENT

Weight of metal blown: 45 cwt (approx.)
Initial composition of metal blown: C, 3.00 to 3.20; Si, 0.70 to 0.90; Mn, 0.40 to 0.60.
Temperature of bath, 1,240 to 1,280 C

After 3 min blowing: 2,500 cu ft free air per min (approx.)
Composition of metal: C, 3.00 to 3.30; Si, 0.35 to 0.40; Mn, 0.25 to 0.30.
Composition of slag: SiO_2, 37 to 47; FeO, 40 to 45, MnO, 9 to 12 per cent.
Temperature increment: 80 to 105 C

After 6 min blowing: 2,000 cu ft free air per min (approx.)
Composition of metal: C, 2.60 to 2.70; Si, 0.10 to 0.15; Mn, 0.07 to 0.10 per cent
Composition of slag; SiO_2, 35 to 40 per cent; FeO 42 to 46 per cent; MnO, 9 to 11 per cent
Temperature increment: 250 to 260 C

After 9 min blowing: 2,500 cu ft of free air per min (approx.)
Composition of metal: C, 1.50 to 1.70; Si, 0.05 to 0.10; Mn, 0.04 to 0.08 per cent
Composition of slag: SiO_2, 50 to 55; FeO, 30 to 35; MnO, 10 to 11 per cent
Temperature increment: 275 to 305 C

After 12 min blowing: 2,000 cu ft of free air per min
Composition of metal: C, 1.15 to 1.35; Si, 0.05 to 0.10; Mn, 0.03 to 0.08 per cent
Composition of slag: SiO_2 54 to 58; FeO, 26 to 30; MnO, 9 to 11 per cent
Temperature increment: 315 to 330 C

TABLE 3—COMPOSITION AND INCLUSION COUNTS OF THE CONVERTER STEEL

	Chemical Analyses, per cent							
	1	2	3	4	5	6	7	8
C	0.225	0.20	0.21	0.20	0.20	0.20	0.215	0.20
Si	0.37	0.33	0.38	0.34	0.38	0.35	0.38	0.36
Mn	0.94	0.86	0.90	0.85	0.99	0.86	0.82	0.85
S	0.036	0.033	0.035	0.042	0.029	0.037	0.034	0.028
P	0.043	0.045	0.042	0.042	0.042	0.051	0.040	0.041
	Inclusion Counts (Fox Method):							
	88	78	72	87	67	85	77	75

depends broadly upon two factors—the working temperature and the skill of the operator in turning heats down at the proper time.

If heats be turned down too early incorrect carbon results may be obtained, and if too late excessive losses of ferro alloys occur during deoxidation. A series of heats was blown and the end point of the heats was determined by a photoelectric cell with a series of filters. A statistical study of the carbons in the blown metals was made, and it can be stated that the use of photoelectric cell control offers distinct possibilities of success in giving uniform chemical composition in the bath at the end of the blow. The British Iron and Steel Research Association has a comprehensive program of work in hand in this connection.

There are also indications of a relationship between the bath temperature and the intensity of the flame. It has been found necessary in connection with this investigation to maintain a close control over the size of the vessel mouth, and within the range of bath temperature of 1,640 to 1,700 C (immersion readings) the cell fitted with a red filter has given deflection readings of 2.1 to 3.1, and on plotting the results almost a straight line relationship has been noted. The work should be continued with a complete range of filters and it is hoped, in due course, to submit further details of the results.

Degree of Control

The side-blown converter process is capable of producing regularly steel heats of uniform chemical composition, temperature and fluidity. Statistical studies of the chemical analysis of steel made by this process have demonstrated that, as far as carbon content is concerned, it is possible to obtain as uniform results as those of any other steelmaking process.

Figures for silicon and manganese are not as good unless the slag formed as the result of the conversion operation is removed before the alloys are added. It is realized that the slagging operation is an additional one, but, bearing in mind the economy in the use of ferro alloys for deoxidation when this is done, the extra work is fully justified.

Heats of steel which have been so handled reveal close control over silicon and manganese contents in the finished product. It is, of course, essential to weigh all metal prior to charging to the converter, and it is desirable to weigh all liquid steel produced. This gives a clear picture of blowing losses, and in this connection losses of from 7.0 to 8.0 per cent are being obtained regularly. In considering this problem of losses it is necessary to consider the causes, (a) chemical and (b) mechanical. With cupola metal of C, 3.0, Si, 0.8 and Mn, 0.5 per cent, there is a chemical loss of 4.3 per cent without taking losses of iron due to oxidation into account.

If hot metal be used the use of low-silicon iron helps reduce the chemical losses to a minimum. The design of the converter has an important bearing upon the problem of mechanical losses. Experience has shown that a long converter body with a small mouth helps to keep the mechanical losses at a low figure.

Some of the metal which might be projected from the converter as the result of the impingement of the blast upon the metal surface falls back into the vessel if a small mouth be used. In blowing low-silicon irons it has been found advantageous to add silica sand on the metal surface prior to putting on the blast, to protect the vessel lining against the attack of iron oxide, etc. This point has been mentioned earlier, but in view of its value it is again stressed.

The quality of steel made by the acid side-blown converter can be much higher than is generally appreciated. The possible regularity and uniformity of chemical composition has already been mentioned. The cleanliness and low gas contents are other factors which ought to be stressed. Eight heats of steel show the chemical analyses and inclusion counts set out in Table 3.

The analyses (Table 4) of samples taken during a heat of steel will support the claim that the steel com-

TABLE 4—GAS ANALYSES OF CONVERTER STEEL

Cupola metal before desulphurizing, per cent			
C	2.99	2.94	
Si	1.08	1.05	
Mn	0.67	0.68	
S	0.084	0.090	Temperature of metal, 1,340 C
P	0.043	0.042	by the immersion pyrometer
O	0.006	0.014	
H	0.0002	0.0006	
N	0.009	0.008	
Cupola metal after desulphurizing, per cent			
C	3.13	3.08	
Si	0.82	0.81	
Mn	0.61	0.61	
S	0.057	0.041	Temperature of metal, 1,267 C
P	0.042	0.042	by the immersion pyrometer
O	0.0025	0.003	
N	0.004	0.004	
H	0.00005	0.00005	
Blown metal, per cent			
C	0.08	0.07	
Si	0.03	0.04	
Mn	0.12	0.08	
S	0.037	0.041	Temperature of blown, 1,682 C
P	0.039	0.042	by the immersion pyrometer
O	0.035	0.050	
H	0.0007	0.0007	
N	0.010	0.009	
Final steel, per cent			
C	0.25	0.24	0.24	
Si	0.36	0.36	0.36	
Mn	1.52	1.52	1.52	Temperature of steel in
S	0.047	0.044	0.047	ladle, 1,562 C by
P	0.048	0.047	0.053	the immersion
O	0.0025	0.0025	0.0025	pyrometer
H	0.00015	0.00015	0.00015	
N	0.005	0.005	0.005	

pares favorably with that made by other processes as far as gas content is concerned, and will be of interest as indicating the changes in gas content which occur during the conversion process.

Physical and Mechanical Properties

The side-blown converter process is ideal for the production of carbon steels of any carbon content. Liquid cupola metal can be used to recarburize heats requiring a high carbon content in the final steel. Low-carbon steel produced by this process for high permeability properties gave the results shown in Table 5.

For many general engineering purposes steel castings possessing a tensile strength of approximately 30 tons per sq. in. are preferred, and large quantities of this quality are produced in Great Britain by the acid side-blown converter process. The properties shown in Table 6 are obtained from this class of material.

The 1 x ¾-in. bend tests gave a 120 degree bend without fracture. The results obtained on a Wohler machine were: Tested at ± 15.0 tons per sq in., unbroken at 11,740,380 revs.; tested at ± 16.0 tons per sq. in., broken at 1,979,460 revs. Short-time tests at elevated temperatures gave the figures shown in Table 7.

TABLE 5—PROPERTIES OF CONVERTER STEEL

Chemical analysis: C, 0.10; Si, 0.18; Mn, 0.34; S, 0.045; and P, 0.034 per cent

Mechanical properties: Yield point, 12.5 tons per sq. in.; maximum stress, 22.7 tons per sq. in.; elongation, 37 per cent; reduction of area, 57 per cent

Magnetic properties

H Values: 5, 10, 20, 40, 50, 60, 100, 150, 200

B Values: 10,900, 13,650, 15,300, 16,500, 16,850, 17,100, 18,100, 19,000, 19,600.

Some makers prefer the composition: C, 0.25, Si, 0.30, Mn, 1.50 per cent for service conditions requiring resistance to wear and abrasion, while others use C, 0.40, Si, 0.30, Mn, 0.80. Both of these steels are regularly made by the side-blown converter process, and the values shown in Table 8 give an indication of the properties obtained from such steels.

The 0.40 per cent carbon type of steel is suitable for gears surface hardened by the "Shorter" process, and many castings of this type are made annually by the converter process. Typical test results, using the Vickers hardness machine, gave surface hardness of 700 to 750, while sections showed the total depth affected to be 0.128 in., the depth of martensite being 0.076 in. while that of troostite was 0.052 in.

TABLE 6—PROPERTIES OF 30-TON CONVERTER STEEL

Chemical analysis: C, 0.21; Si, 0.28; Mn, 0.90; S, 0.033; and P, 0.037 per cent

Yield Point, tons/sq. in.	Max. Stress, tons/sq. in.	Elongation, per cent in 2 in.	Reduction of Area, per cent
17.40	30.12	33	57
15.12	30.00	32	52
15.32	30.00	32	57
15.64	30.60	33	59
17.60	31.00	33	57
16.72	30.92	32	54
17.20	30.80	33	54
15.72	31.24	33	57

The fluidity of the two steels when made by the side-blown converter process with the proper degree of temperature control is excellent, and the steels are well suited for the production of light, thin-walled castings. Other general types of carbon steels made by the same process include 0.60 per cent carbon, and 0.4 carbon and 1.5 per cent manganese steels.

Alloy Steels

Nickel and molybdenum steels can be made easily by this process, the alloys (nickel and ferromolybdenum) being added to the metal before the blow commences. In the case of chrome and vanadium steels, the alloys are added to the bath after slagging and after deoxidation. Austenitic manganese steel can be made by adding molten ferromanganese to the bath of killed carbon steel, any adjustments required to keep the carbon-manganese ratio being made with low-carbon ferromanganese. The following physical properties of low alloy content and of austenitic manganese steel castings made by the side-blown converter are given to indicate the quality of the product.

TABLE 7—STRENGTH DATA AT ELEVATED TEMPERATURES

Tested at	300C	400C	450C
0.05 per cent press stress, tons per sq. in...	11.0	8.9	8.5
0.10 per cent proof stress, tons per sq. in..	11.35	9.6	9.3
Yield point, tons per sq. in.....	12.9	12.5	11.5
Maximum stress, tons per sq. in.....	34.6	29.6	25.0
Elongation, per cent.....	30.0	35.0	36.0
Reduction of area, per cent.....	45.0	50.0	60.0

TABLE 8—COMPOSITION AND PROPERTIES OF LOW AND MEDIUM CARBON STEELS

Chemical Analysis: C, 0.24; Si, 0.26; Mn, 1.47; S, 0.037; and P, 0.037 per cent.

Yield Point, tons/sq. in.	Max. Stress, tons/sq. in.	Elongation, per cent in 2 in.	Reduction of Area, per cent
22.32	39.12	26	44
22.40	39.38	24	43
23.20	39.04	27	50
22.80	38.40	28	52

Chemical Analysis: C, 0.39; Si, 0.36; Mn, 0.84; S, 0.037; and P, 0.057 per cent.

23.08	42.08	19	24
24.20	39.52	22	28

Nickel Steel (3 Per Cent Nickel)

Chemical Analysis: C, 0.20, Si, 0.35, Mn, 0.80, Ni, 3.00, S, 0.035 and P, 0.040 per cent.

Mechanical Properties—Yield point, 24.0 tons; maximum stress, 36.4 tons; elongation, 27 per cent; reduction of area, 47 per cent. The fluidity of this steel is extremely high and it is well suited for running castings having thin sections.

Molybdenum Steels

Chemical Analysis: C, 0.22, Si, 0.35, Mn, 0.76, S, 0.037, P, 0.042, Mo, 0.64 per cent.

Tested at*	Room temperature	300C	400C	450C	500C	550C
Limit of proportionality	19.5	11	10	9	9	6.5
Yield point, tons/sq. in.	21.2–24.8	20.75	18	18	17.5	16.75
Maximum stress, tons/sq. in.	33.0–34.8	41.25	34.25	33.75	29.75	25.2
Elongation, per cent	30.0–31.5	14.5	24.75	31.75	34.5	36
Reduction of area, per cent	52–57	24	40.25	54.75	69	77.75

* Tests at elevated temperatures were short-time tensile tests.

Chrome-Molybdenum Steels

Chemical Analysis: C, 0.25, Si, 0.36, Mn, 0.96, S, 0.010, Cr, 1.05, Mo, 0.46 per cent.

Attention is drawn to the low sulphur content in this steel, the heat being doubly desulphurized. Original cupola metal, 0.068 per cent S; after first desulphurizing, 0.025 per cent S; and after second treatment, 0.014 per cent S.

Water Quench, °C	Temperature °C	Yield Point, tons/sq. in.	Max. Stress, tons/sq. in.	Elongation, per cent	Reduction of Area, per cent
900	450	49.52	72.72	9.5	33
900	500	43.72	66.76	13	36
900	550	43.64	62.56	15	47

Copper-Manganese-Molybdenum Steels

Chemical Analysis: C, 0.24, Si, 0.52, Mn, 1.44, S, 0.041, P, 0.045, Mo, 0.39, Cu, 0.62 per cent.

Water Quench, °C	Temperature °C	Yield Point, tons/sq. in.	Max. Stress, tons/sq. in.	Elongation, per cent	Reduction of Area, per cent
900	580	50.36(a)	57.52	14	30
900	650	36.04(b)	45.16	24	52

Impacts
(a) 40, 41, 41
(b) 49, 47, 55

This steel was used to produce castings 0.375 in. thick and little trouble was encountered due to short-run edges, etc., the steel being quite fluid.

Austenitic Manganese Steel

C, per cent	Si, per cent	Mn, per cent	S, per cent	P, per cent	Max. Stress, Tons/sq. in.	Elongation, per cent	Reduction of Area, per cent
1.02	0.82	13.75	0.020	0.068	59.3	64	38
1.02	1.09	13.10	0.020	0.069	58.06	52	41
1.16	0.78	13.75	0.020	0.076	58.38	59	43

Chemical Analysis: C, 1.20, Si, 0.83, Mn, 12.8, S, 0.020, P, 0.066, Cr, 1.28 per cent.

Maximum Stress, tons/sq. in.	Elongation, per cent	Reduction of Area, per cent
60.34	55.5	Unbroken
61.32	62	42
62.34	62	41

The addition of chromium to this steel slightly increases the hardness and reduces the tendency of the steel to "spread" or "flow" in service.

The foregoing figures are intended to demonstrate the excellent properties of the steel that can be made by this process. Statistical studies of the test results taken over a long period have shown that the properties of steels produced by the process compare favorably with those of steels of similar chemical analysis produced by any other process and used for steel casting production. From the steelfounder's point of view, the process is flexible and gives hot fluid steel in suitable quantities (depending, of course upon plant facilities) and at regular intervals.

This makes the process an ideal one for the mass production of green-sand steel castings, and it is the author's considered view that, with the aid of the new technical control methods and instruments, the process will regain its former popularity. The real test, of course, will be the quality of castings made, and their subsequent behavior in service.

Future Developments

Excellent figures for gas content which have been obtained and quoted in this paper, and confirmed by other workers, have convinced bulk steel producers of the possibilities in the side-blown operation. In Germany during the recent war, 25-ton basic side-blown converters were in use at the Mannesmann Works at Huckingen, and these proved very successful, steel being made to a regular specification of 0.008 per cent nitrogen. The steel made from the side-blown converter is hotter than that from the bottom-blown vessels.[4]

Liquid steel can be produced at as low a cost by the side-blown converter process in plants of modern construction and with the necessary degree of technical control as that by any other steelmaking process. It is, therefore, safe to predict a good future for the process, for the production of steel for castings and possibly for ingot production.

It is felt that development will follow (a) the increased use of superheated low-silicon metal, or at least a greatly increased tonnage of lower-silicon metal than is at present used; (b) more control over desulphurization and possibly of dephosphorization; (c) a general use of immersion pyrometer equipment as a means of accurate temperature control; (d) the use of photoelectric cell units for controlling the end point of the process and as an additional means of temperature control; (e) better converter design as a means of reducing metal losses due to projections, and (f) greater appreciation of the high quality of steel that can be produced by the process if proper precautions are taken.

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CHEMICALLY TREATED SAND*

A MOLDING SAND PROCESS WITH A RESIN BOND

By

T. W. Curry †

ABSTRACT

The author discusses a process for chemically coating foundry sands with a micro-thin film of carbon resin for refractory and bonding properties. The chemical is a viscous liquid containing an almost pure carbon resin, a solvent and water. The sand grains are brownish-black after treatment herein discussed.

The chemically treated sand has better flowability characteristics than any synthetic sand the author used previously. This results in a mold with more uniform hardness with less ramming.

Better surface finish of the casting and possible substantial improvement in cleaning costs led to the author's experiments with this chemical bonding sand process.

THE PROBLEM OF DEVELOPING a versatile molding sand for the author's mechanized molding units and controlling the sand properties within good working limits was the initial reason for the experimental work on the chemically treated sand. In view of a moderate success with the application of some of the synthetic sands, we were desirous of improving the quality and manufacturing costs of our castings, and though the chemically treated sand was a radical change from the preparation and materials currently used, it has worked out successfully and has had a pronounced effect upon all the operations performed in the making of gray iron castings.

Most foundrymen, producing gray iron, have experienced some difficulties at one time or another with their molding sand. Reliable technical information concerning all the materials used in molding sands and the best known procedures for combining, preparing and controlling them for use has been made available by able men of the industry. The successful application of a molding sand requires the use of this information in its entirety, but the combination of the most desirable properties is sometimes influenced, or limited, by circumstances which do not allow the foundryman to employ his complete store of knowledge. Following, some of these are listed for consideration:

1. The mechanized equipment may have been designed with the emphasis on the speed of molding without due consideration of the necessary preparation of a molding sand to develop its best working properties.
2. Molding systems installed at the time of a single shift operation are now working double shifts, or continuously for five and six 24-hr. working days. Higher temperatures in the system sands have influenced the sand properties.
3. Refinement of molding methods and increased production have been introduced to a mechanized unit while the sand handling equipment has remained at its original capacity and design.
4. Elimination of dry sand cores, either ram-up or assembly, has been adopted in the manufacture of many castings and this has required better properties in the molding sand.
5. The ventilation and exhaust equipment, an integral part of most mechanized systems, has been designed to remove fine material from the sand and, in some instances, to an extent where it is a definite disadvantage.
6. The range of castings made in a synthetic sand has been spread beyond the limits of any one sand mixture. The flexibility of the sand has promoted this production thought and some of the properties of the sand are influenced when it is sorely overtaxed.
7. In mechanized molding systems where it is possible to develop the best working properties of a particular sand mixture by proper conditioning, the present synthetic sands are not flexible enough to be used to produce the desired range of castings satisfactorily.
8. Cleaning costs, no matter the degree of efficiency of the operation, are a source of expense which should be eliminated, if possible, or reduced to a minimum below that which the foundry industry, is now paying.

Whether or not a foundryman is confronted with anything detrimental in his mechanized molding and sand handling equipment which might influence his molding sand properties, he is not likely to refute the claim that the synthetic sands as they are now made are not flexible enough to produce his particular line of

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TABLE 1—GRAIN DISTRIBUTION TABLE FOR BASE SANDS AND SAND MIXTURES. WASHED AND SCREENED SILICA SANDS USED ON CHEMICALLY BONDED SAND EXPERIMENTAL WORK AND IN PREVIOUS SYNTHETIC SAND MIXES

	Per cent Retained on Mesh Number											
Grade	30	40	50	70	100	140	200	270	Pan	AFA Clay	AFA F. No.	Grain Shape
#60	T	2.0	8.8	37.2	42.0	9.6	T	T	—	T	61	RS
#70	—	0.1	2.1	28.4	52.4	15.3	1.4	0.2	T	T	73	SA
#90	—	T	0.4	7.2	44.0	28.2	13.6	3.2	2.4	T	95	SA
#C	T	5.0	24.0	41.8	22.2	6.0	0.8	T	T	T	54	RS
#111	T	T	T	1.6	11.0	47.0	31.4	4.8	2.2	2.0	118	S
#10M	0.6	2.0	4.2	11.2	19.6	19.6	15.8	3.8	7.4	15.4	110	RS

T— Trace or negligible amount as shown in routine testing
R— Round-RS (round to subangular)
S— Subangular—SA (Subangular to Angular)
A— Angular (Angularity designated because of the shape of some of the very fine grains in the finer sands)
NOTE: The trace of AFA clay indicated in the table for the washed silica sands is negligible in that it could have been pan material or the technique in the performance of the washing operation. There is very little discoloration of the washing water, when these sands are analyzed for clay content.
These sands are supplied in large quantities and are subject to some variation on each screen, but the grain fineness number will be consistent within ±2½. All are mined and prepared in the New Jersey area.
#111 Sand is a bank sand with an average clay content ranging between 1½ and 2½ per cent.
#60 and #C, washed and dried, are mixed by unloading alternate railroad cars into the sand silo and are used as the base sand for all the core sand mixtures.

castings to his complete satisfaction. It does not alter the problem to consider that the mechanical equipment is affecting the sand adversely, or that the sand mixture is not versatile enough to be used with a particular design of sand handling equipment and molding machines.

Since the mechanized equipment in most foundries is fixed and major changes would involve large capital expenditures, work was initiated on the problem of finding some sand mixture with working properties which would produce better castings with the equipment as it existed. It is the purpose in this report to show the results of the work on some of the synthetic sands, and point out our experiences with them, and to present a comparison of a chemically treated or resin-bonded molding sand in the same application. See Tables 1, 2 and 3.

Quoting a description, "Permi Bond is composed of a specially prepared pure hydrocarbon which is completely volatile at elevated temperatures. Total ash is 0.2 per cent maximum and on decomposition does not produce excessive free carbon, consequently all sand contamination is eliminated."

In sands C, D and F, (Table 2 and 3) the sea coal and pitch were used in a mixture of 5 parts sea coal to 1 part of pitch. Various other combinations were attempted, but it was necessary to watch carefully with even this low pitch content or the sand would become water-proofed very rapidly. The more concentrated pitch additions caused misruns in the lighter metal sections and introduced shakeout difficulties because of the high dry strength and an increase in heavy, dense fumes.

Sands A, B, C and D caused difficulty at the shakeout with the higher dry and hot strengths of the cereal-bentonite bonded sands.

Sands E, F and G had better shakeout properties but difficulty was experienced in making deep or intricate lifts.

Hot strengths of all the sands shown in Table 2 were regulated by a selection of bond, such as various combinations of the bentonites or an increase in the green compression strength. Cutting or washing in the heavier metal sections was corrected in this manner.

TABLE 2—SYNTHETIC SAND MIXTURES USED ON PRODUCTION BASIS PRIOR TO USE OF CHEMICALLY BONDED SAND

Sand	Base Sand (Table 1)	Corn Flour	Sea Coal	Pitch	Western Bentonite	Southern Bentonite	Bond Clay	Wheat Flour	Wood Flour	Permi-bond
A	60 or C	X	X			X				
B	60 or C	X	X		X	X				
C	60 or C	X	X	X	X	X				
D	60 or C	X	X	X	X			X		
E	60 or C		X				X	X		
F	60 or C		X	X	X				X	
G	60 or C				X				X	X

NOTE: The sand mixtures in Table No. 2 were used as the system sand with the components as shown. Special facings for some particular applications were used but are not shown because they were not in enough quantity to influence the entire system.
In sand B, the two bentonites used for bond were in the ratio of 1 part Western bentonite to 3 parts Southern bentonite.
In sand C, the bentonite bond was used in the ratio of 3 parts Western bentonite to 1 part Southern bentonite.
Bond clay indicated in mixture E was a known scientifically prepared bond of the fire clay type. The use of the mixture E was limited to the shortest production run of any of the sands shown (about four 16-hr. shifts) because of an increase in mulling time necessary to develop good working properties.
All the sands shown in Table No. 1 were introduced to the system in maintenance amounts at one time or another. The selection depended upon the current grain fineness or the type and section of castings to be produced. The 10M sand was not used as an addition at the shakeout.

TABLE 3—RANGE OF PROPERTIES OF SYNTHETIC SAND MIXTURES USED ON PRODUCTION BASIS AS SHOWN IN TABLE 2

	Sand A	Sand B	Sand C	Sand D	Sand E	Sand F	Sand G
Moisture	3.0–4.5	3.0–4.5	5.0–5.5	5.0–5.5	4.5–5.0	3.0–4.0	3.0–4.0
Permeability	60–80	60–80	70–90	70–90	85–110	85–100	90–110
Green Compression	9.0–11.5	9.0–11.5	6.5–9.0	6.5–9.0	10.5–12.0	12.0–16.0	12.0–16.0
Deformation	0.020–0.023	0.027–0.030	0.025–0.028	0.025–0.028	0.018–0.021	0.015–0.018	0.015–0.018

GENERAL NOTES: During the times when all these sands were in operation, the expansion-contraction characteristics were checked at 2500°F. Pilot castings, which would also indicate certain system sand conditions, were checked frequently in conjunction with the results obtained in the dilatometer. Best results in the elimination of scabbing and buckling were obtained when the stability factor (the difference between the maximum expansion and the maximum contraction expressed in inches per inch) was +0.007 in., and the maximum expansion of the sand was not attained for one and one-half minutes after plunging into the 2500°F. zone. Carbonaceous materials as shown in Table 2 were added in amounts to regulate the stability factor of the sand and the time for the maximum expansion under heat to develop. For the various castings produced in these sands, the carbonaceous material analyzed between 5½ to 6½ per cent by weight, and was usually maintained at the lowest possible minimum to facilitate shakeout and favor lighter sectional castings.

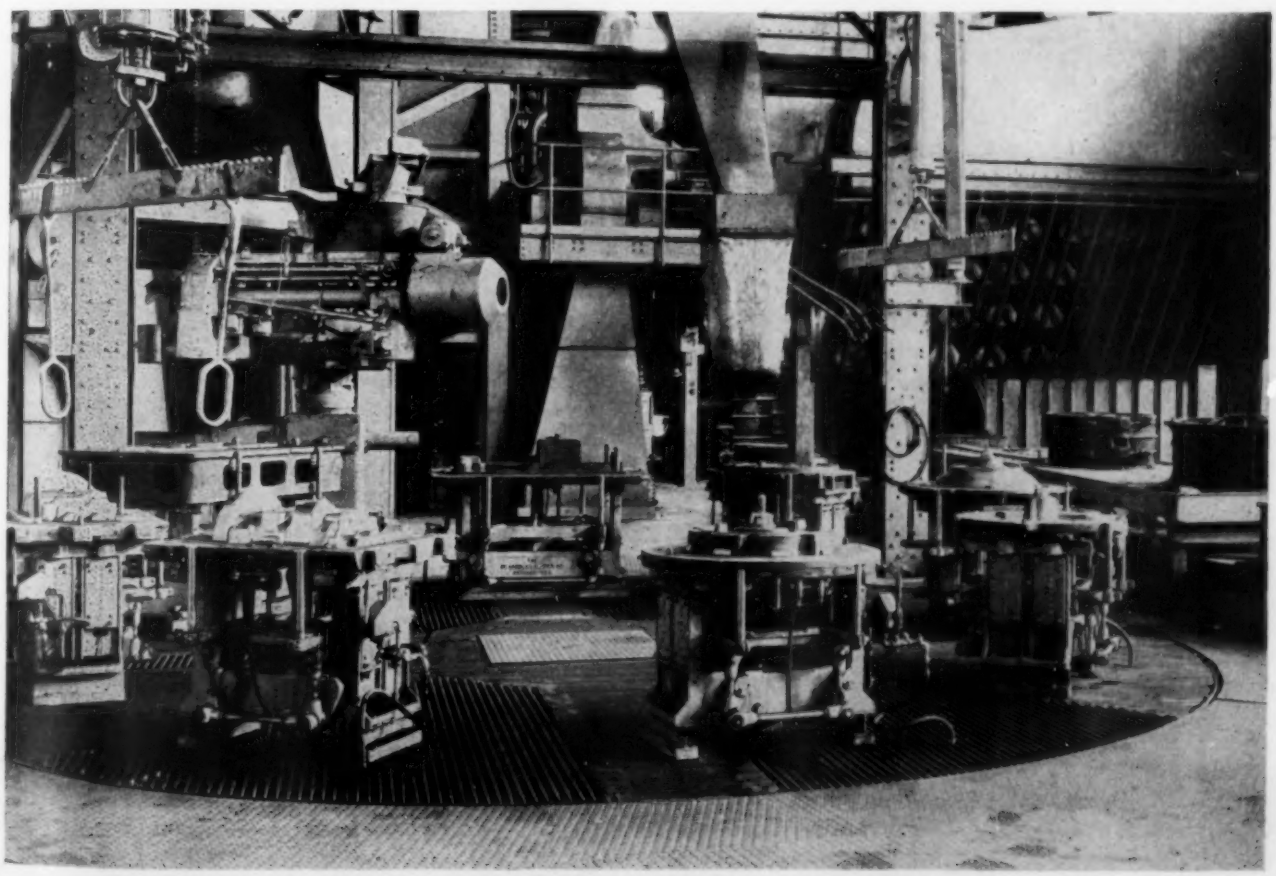
Sand C and Sand D were the most satisfactory sand mixtures mainly because of their toughness and ability to make the difficult lifts on the jolt-squeeze-strip molding machines.

In the design of the synthetic sand mixtures, we believe that first consideration must be given to the rate and amount of expansion of the silica grain under heat and that it is necessary to counteract this expansion by sufficient clay or carbonaceous materials to make the sand stable under thermal shock. Since the percentage of clay (4 to 5½ per cent) in Sands A, B, C and D shown in Table 2 and 8 to 9½ percent in Sands F and G) is low due to the use of the stronger bentonite bonds, the probability of scabbing by having a high contracting sand was comparatively low. Scabbing, buckling, cope burn-down, and rat-tail were treated as a function of the expansion property and adjustments in the percentage by weight of carbonaceous materials, or pouring time, were made accordingly.

Fig. 1—Sand slinger turntable with pin-push-up machines operating with chemically treated sand.

Need for Improvement of Synthetic Sands for Molding Units

It is well to point out that the synthetic sand mixtures as outlined in Table 2 were probably not up to their greatest efficiency because of circumstances incident to the author's shop. We are cognizant of the fact that the proper mulling time and time for the sand to temper improved all the sand properties, but every effort was made to try to provide sand in sufficient quantities to allow the greatest mold production, notwithstanding the fact that the sand was not properly mulled.



Other problems, which accompany a wide variety of castings made in jobbing lots, were present. The necessity for rapid changes in the system sand properties; short, fast molding conveyors with castings of heavy section; the minimum allowed time between pouring and shakeout, and a less than desirable tonnage of sand in the system had their individual influences on the sand properties. These were the motivating reasons why the different synthetic sands were tried and, when found lacking in some of the working properties with the existing equipment and methods, pointed to the need for some further improvement, and an investigation as to the merits of the chemical process was started.

We had the rather unusual case of having two separate foundries, working side by side in the same plant, and making entirely different types of castings. It afforded us an opportunity to observe the same synthetic sand mixture at work on two applications with one singular difference; namely, longer mulling time and time to temper properly. This was accomplished by making the sand in the molding unit of one foundry interchangeable with the "snap" floors of the other foundry, so that every 16 hours there was a complete change in the molding unit sand. All the sands shown in Table 2 were handled in this manner and we obtained better working properties in each case by mulling longer and allowing time to temper before use.

The procedure of making the sands for the two foundries interchangeable served one purpose at least—that of demonstrating that the selection of the components of the mixture and its working properties would do a good job in the making of a casting if the proper method of preparing the sand mixture was utilized.

By far, the one sand property which presented the most difficulty to obtain in all the sand mixtures, and that only to a degree that was not completely satisfactory, was that of flowability. Good shakeout was also a problem, but the lack of flowability influenced the surface finish, and consequently the cleaning costs, and caused a scrap and production loss because of a low mold density with the normal ramming operations. Sands F and G had better flowable qualities than any of the other sands listed in Table 2.

It was the better finish of the casting and the possible substantial improvement in cleaning costs which led to our experiments with this process.

Chemically Bonded Sands

The process for the preparation of chemically bonded molding sands is an entirely new conception of the function of the grains of sand in the making of a mold. It is concerned with the surface of the grain, rather than the grain as a whole, both from the standpoint of refractoriness and all functional properties. A good working knowledge of the process can be had when just the mechanical aspects, which may or may not be the result of a chemical reaction, are considered.

Consideration must first be given to the fact that the process (the chemical used is secondary to this) uses a carbon coating on the sand grain for refractory and working properties. With this in mind, the chemical, for explanatory purposes, can be considered a physical mixture, instead of a complex chemical composition which it really is. We can, therefore, define the chemical that we are using at the present time as a viscous liquid containing an almost pure carbon resin, a solvent and water.

Originally the process and its chemicals were designed to produce molding properties in a sand without the use of bentonite or clays, cereal, or water. All plasticity was derived from the action of the solvent on the carbon resin coating; hence, the frequent reference to non-water soluble chemical, in the early stages of this development. The chemically bonded sands, using the non-water soluble material, were found to be of an inflammable nature because of the solvent, which is a petroleum derivative, and were temporarily set aside in favor of the chemical used with water in the sand mixture.

Initial operations of the process consists of coating the sand grain with a micro-thin film of the carbon resin. This is accomplished by mixing a washed and damp silica sand with a proportionate amount of the chemical, (approximately 48 gal. per 2000 lb. of sand) in the ordinary foundry mixer. The mixture is then passed through a rotary drier, operating at 300 to 400° F. to evaporate the solvent and water vapor and set the resin coating on the grain. In this phase of the procedure the solvent is only a vehicle to convey the carbon resin around the grain, and may be recovered from the vapors coming from the drying operation with the use of a condenser and separator.

The sand, as it is discharged from the drier, has no plasticity or molding qualities and could be compared with washed and dry silica sand, with the exception of the color. The sand grains are now brownish-black instead of translucent to white. Under a microscope, the newly coated sand appears to be a glistening black, and the thin coating has not obliterated the original contour and angular formation of the sand grains (Fig. 2).



Fig. 2—A.F.A. Fineness No. 60 sand shown at 20 diameters. Note how the micro-thin coating of an almost pure carbon resin is spread and maintained around sand grains. Resin coating is hard and durable.

The carbon resin, or coating, is a hard durable material, insoluble in water and common solvents, tenaciously attached to the grain so that it is not apt to spall or break off, but will become plastic in contact with the solvent of the chemical. It is the latter property of the coating, the function of its becoming plastic, that is the primary source of all the desirable qualities we have found in the chemically-treated sand.

The coated sand is then ready to be introduced to the molding unit, where it is subsequently mixed with bentonite, cereal, chemical and water to develop the desired working properties necessary to make its first mold. In the first production unit in which the sand was used, 100 per cent coated sand was introduced as the basis for the sand mixture, and in the second molding unit (the sandslinger) eight parts of coated sand and two parts of No. 60 fineness silica sand were used with good success. Continued re-use of the chemically-treated sand requires only maintenance additions of bentonite, cereal and chemical to hold the desired specifications.

Uncoated sand, that is, new silica sand, core sand, and the grains adjacent to the molten metal have had the coating burned off, enter the sand system and are coated in the cycle of mulling with the chemical in the mixture, and using the heat of the molten metal, back of the face of the mold, to evaporate the solvent and water vapor. Hence, during the making, pouring and shakeout of a mold, the coating is burned off some sand grains and replaced on others, so that the percentage of coated grains can be kept in good balance by the chemical addition in the mullers.

Some Experimental Notes on Chemically Bonded Sand Before Production Trial

Bearing in mind that the decision to study the use of chemical bonding in the molding sands was made with the idea of reducing the cleaning costs, eliminating the sand blasting operation if possible, some interesting results were noted in the work of getting sufficient information to adapt the process for production. These findings were the basis for the first production trial run and they have not been found to deviate too much on a large scale operation.

All experimental work was based on the surface finish of the casting with the idea in mind that if the surface was good, and the casting was correct dimensionally, then all the sand properties were good. Consequently, the major portion of the experimental work was done in the foundry and the sand was adapted to the existing methods and equipment rather than to try to compensate for a lack of some particular inefficiency found in the sand. Trial molds and castings from almost all the current patterns were made and studied before a recommendation for a trial production run was made. (January 20, 1947, to present time).

The following information was found experimentally by practical shop application of chemical bonding:

1. First tests were made with the chemical S.A. (the non-water soluble material) and the possibilities of the use of this material to produce gray iron castings at some future date cannot be disregarded. It is possible to produce castings up to three-inch section which come from the mold at the shakeout free of all molding sand and ready for the chipping and grinding operations. In this work, it was found that the sand would glaze and generate dense white fumes if the casting had any red heat at the shakeout and it was necessary to bring the casting to an impractical low black heat before the volume of these fumes was reduced enough for general shop use. There was also the tendency for the sand to "chill" the metal in the mold sufficiently to cause misruns in the lighter sections and the heat transfer was rapid enough in some instances to produce a white iron. Some foundrymen have suggested the use of the chemical SA for applications in the malleable iron field, or in gray iron in the form of facing, or a face core, where localized shrinkage is present due to the design of a particular section.
2. These characteristics of the chemical SA (blaze, fumes, chilling) indicated a change in the chemical itself so that it could be worked in conjunction with water, and consequently with the currently used bonding materials, to eliminate the hazard of the flash and blaze at the shakeout and reduce the volume of the fumes. Hence, the chemical HO (water in the chemical and used with water in the sand mixture) became the basis for the study of the resin bonded sands.
3. The question of grain distribution of the base sand became apparent early in the investigation because of the variation in the working properties obtained in coated sand mixtures made from different carloads of the same fineness sand. The cause of this variation was checked in detail and it was found by making up sand mixtures from the sands shown in Table 1, in different proportions that a wide distribution of the screen sizes of the sand grains produced the best and most consistent working properties. Details of all the experimental coated sand mixtures cannot be reported here, but it is well to consider the following results:
   - A—50 per cent No. 60 and 50 per cent No. 90 better than 100 per cent No. 70
   - B—50 per cent No. 70 and 50 per cent No. 90 better than 100 per cent No. 90
   - C—50 per cent No. 60 and 50 per cent No. 111 better than 100 per cent No. 70
   - D—All mixtures where the grain distribution was spread over more screens, no matter the percentages of each sand used, the working properties of the sand mixture were improved.

   There is a very noticeable improvement in the density of the mold, and correspondingly the hardness of the mold surfaces, when the grain size is well distributed over a wider range of screens.
4. In every test conducted, the carbon resin coated sand mixtures produced better finish and cleaning characteristics than the synthetic sand mixtures containing sea coal, or sea coal and pitch combinations. An attempt was made to mix synthetic heap sands containing sea coal with the chemical coated sands in a sand mixture, but the working properties of this sand were not as good as the 100 per cent coated sand mixtures and the finish of the casting was definitely not as good.

TABLE 4—QUANTITY OF CHEMICAL SOLUTION USED

	Chemical Solution in Qt. per 1150 lb. Batch		Chemical Solution in Qt. per 1600 lb. Batch	
	Start of Trial	Present	Start of Trial	Present
Jolt Squeeze Machine unit Sand Slinger Unit	5–6	1½–3	4–5	½-1½

NOTE: The chemical solution is a mixture of one part chemical and one part water which is kept under a slight agitation with a small amount of air exhaust or manually stirring ocasionally. (One quart of the chemical weighs two pounds). With the reduction in the maintenance amounts of bentonite and cereal required in the chemically bonded sand, as compared with the synthetic sands shown in Table 2, and this new reduction in the amount of chemical used, the cost of the present chemically-treated sand mixture is approximately the same or somewhat less than the cost of Sand C or D.

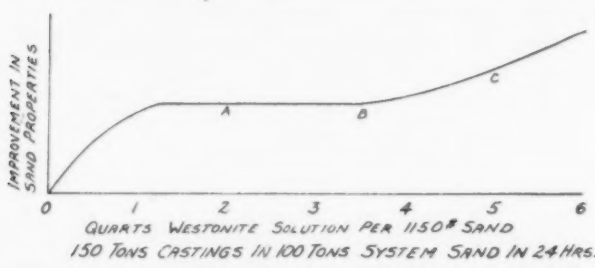


Fig. 3—Effect of chemical on sand properties used in molding machine unit.

Conclusions Based Upon Observations of An Eight-Month Production Run

We would like to point out to the best of our ability the numerous advantages of chemical bonding as well as the trouble we have experienced from time to time on some of the castings. Generally, we believe that the advantages far offset the trouble encountered and each passing day teaches us more about the use of the chemically treated sand and more of the pitfalls can be avoided. An exchange of information from other installations of the process is also aiding in the application of this new project in molding sands.

Comparative Cost of Chemically Bonded Sand Mixtures

Observations of various sand mixtures made with the chemical bond have, over a period of eight months, indicated that the percentage of the chemical used as a maintenance addition has been in larger amounts than that which has been required for best results. The resin coating on the grain and the chemical addition at the mullers have been made with the thought in mind to further improve cleaning costs by an even greater saving in sand blast time, but there is a definite limit where the improved 'peeling' qualities of the sand causes cutting in front or under the in-gates. A reduction in the amount of the chemical, previously reported, has been made in both mechanized molding units and this has resulted in a reduction in cost of the chemically bonded sand mixture. Table 4 and Fig. 3 show the trend of this change.

In Table 4, a rather wide range of chemical additions is shown under the present mixes. Conventional core sand mixtures of core oil, cereal, iron oxide and silica flour are used in the foundry and certain molding schedules collapse an appreciable amount of uncoated sand into the system. Heavier castings also require more cooling of the sand at the mullers and this in turn causes greater losses in fine sand grains which are replaced by adding uncoated sand at the shakeout operation (Fig. 4).

In our experience with this process on many different types of castings, observations of the reduction in the use of the chemical can be explained by Fig. 3.

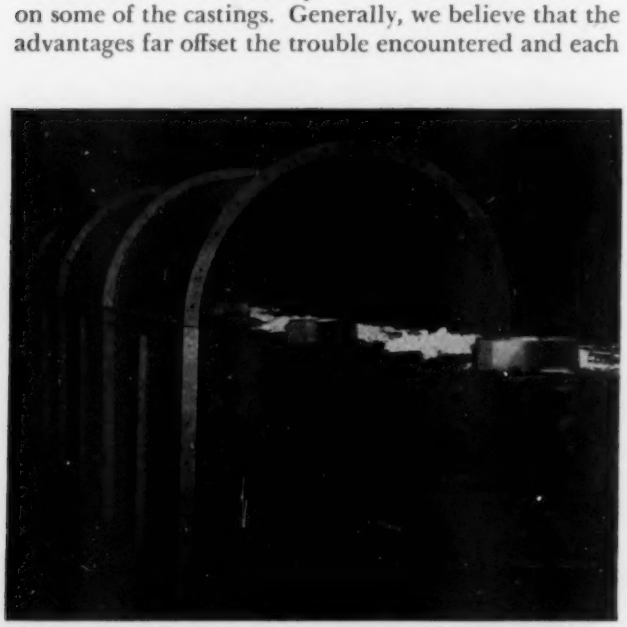


Fig. 4—Tractor wheel mold entering the fume tunnel going to the shakeout station. A measured amount of uncoated silica sand (white material on mold top) is added to mold to maintain grain fineness and the amount of sand carried in the unit system.

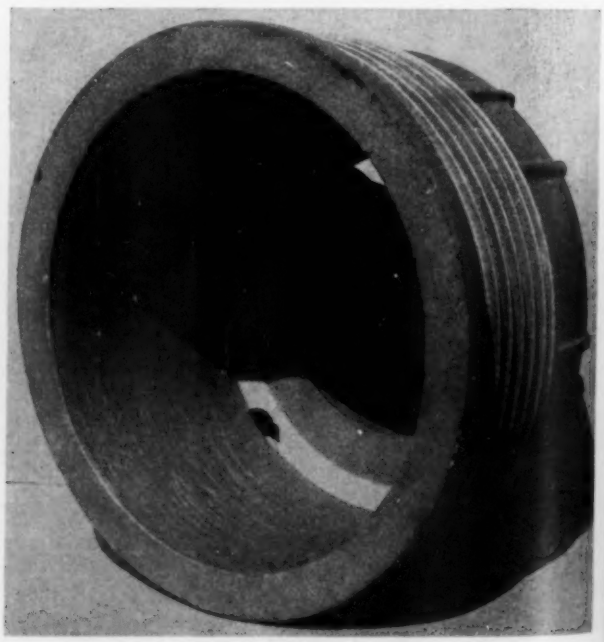


Fig. 5—Drag view of automotive brake drum casting weighing 88 lb. Molding, gating and finish factors require a sand having good hot strength, toughness, flowability.

There is one exception to the improvement in the properties of the sand as shown by Fig. 3; namely, the ability of the sand to withstand cutting or washing in the mold in areas directly under the in-gates. A sustained addition of 4 to 6 qt. of chemical solution to each 1150 lb. of mixed sand will cause a scrap loss by cutting under the in-gates of castings made in portions of the drag and cope. Castings made all in the cope of the mold and gated from the joint line will not be affected by cutting when the chemical is in the higher percentages, but it is necessary in most cases to maintain an all purpose sand. In one case only, we have found a fibrous carbon deposit, which when sand blasted appears as if a slag inclusion had been present, as a result of having too much chemical in the sand. This defect was consistently in the same area and we believed that the carbon resin was melted from the sand grain by the radiant heat coming from the in-gate and caused a surface imperfection when the metal in the mold raised up against it. The casting described is shown in Fig. 5.

Because of the limit set by the tendency to "cut" (Point C, Fig. 3) on the possibility of producing a clean casting at the shakeout, it is obvious from a consideration of cost that it is better to keep the chemical addition at Point A rather than Point B on the empirical curve shown in Fig. 3.

Better Shakeout Properties

The shakeout properties of the chemically treated sand on both the machine and sand slinger units are superior to any synthetic sand mixture which has been used (Fig. 8). It is this property which makes the sand more versatile than any other sand in our experience because the working properties can be adjusted by choice of bonding materials or variation of the specifications without being accompanied by the excessive lumping normally resulting from the higher hot strength or tougher sands in the synthetic mixtures.

The improvement in the shakeout properties has had a threefold effect:

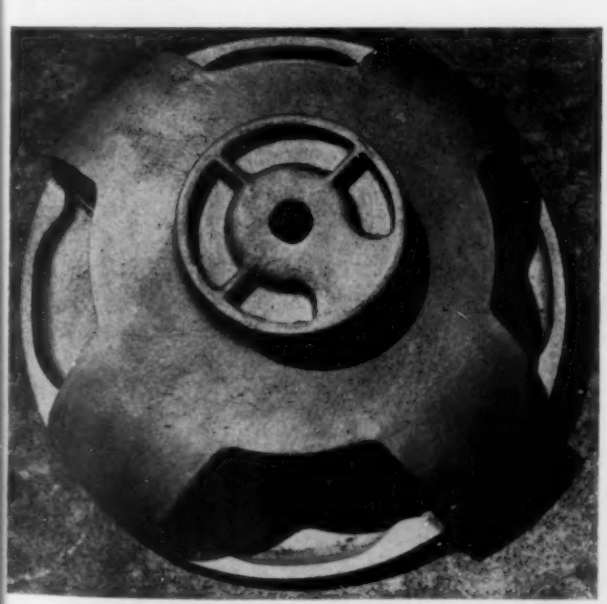


Fig. 6—Drag side of motor end weighing 90 lb. The diameter is 25 in.



Fig. 7—Mold for an oil casting ready for closing.

1. The shakeout sand returning to the mullers is in a much better condition for remixing;
2. The refuse sand has been limited to pieces of broken cores, which can be considered as a normal waste unless special reclamation equipment is available; and
3. The surface finish of the casting remains better.

In the synthetic sand mixtures (Table 2) insufficient mulling time and the resulting excess of bond and ce-



Fig. 8—Sand slinger shakeout operation. Notice the amount of sand actually on the surface of the casting at the moment, the atmosphere over the shakeout, and the general texture of the sand coming from the flask.

real flour, over and above that amount which would normally develop the required working properties, caused lumping, poor finish, and increased the sand costs by the waste when these were screened out of the shakeout sand. Other obviously undesirable features also accompanied this condition of the synthetic sands in our former system.

Toughness or Strength At Low Moisture with Good Flowability

The chemically treated sand has better flowability characteristics than any synthetic sand we had used previously. The improvement in this property enables the mold to be made with a higher and more uniform hardness throughout, with fewer jolts of the molding machine and less ramming operations. A reduction in rammed areas, with the reduction in the resulting metal penetration and the loss of correct contour, has materially improved the surface finish and held the overall dimensions more closely within the specified tolerances.

The toughness of the chemically-treated sand can be increased to a specification which will mold satisfactorily on all the current patterns without seriously affecting its good flowability properties. In other words, a tough, highly flowable sand is now possible; two properties which in most sands are not compatible. Most of our work involves deep, intricate lifts, as both the cope and the drag are made on jolt-squeeze-strip molding machines, or on pin-push-up machines on the sand slinger unit, and one of the major problems was to provide a sand which would lift from a deep pocket and, at the same time, ram uniformly hard on all mold surfaces. In the synthetic sands, it was necessary to keep the toughness of the sand as low as possible to maintain a good surface, and the loss of molds, due to stick-downs coming from the molding machines, was high. The combination of toughness and good flowability of the chemically treated sand has reduced this loss of molds appreciably, and a very satisfactory increase in production has resulted.

High strength, or high toughness, whichever is desired, can be developed in the chemically treated sands at much lower moisture contents. Here again, a third "incompatible" arises; namely, a tough, highly flowable sand with a low moisture content. The synthetic sands (C and D, Table 2) were discharged from the mullers between 5.0 and 5.5 per cent moisture, and the chemically treated sand has very good temper at 3.0 to 3.3 per cent. A reduction in the water vapor during the pouring operation is a definite advantage. The solvent in the chemical begins to evaporate from the face of the mold when the mold comes from the pattern, and the gases, incident to the burning of the carbon resin and the solvent are slower in forming than in the synthetic sands. (See Table 6 for the total of the carbonaceous materials and moisture in the sand). Hence, less water vapor and slower forming mold gases, during pouring, require less permeable sands and a finer finish for larger castings can be obtained.

Control of Chemically Treated Sand in Shop

A more accurate control of the sand properties at the mullers (Fig. 9) has been made possible with the chemically treated sand. Reduced moisture content, smaller

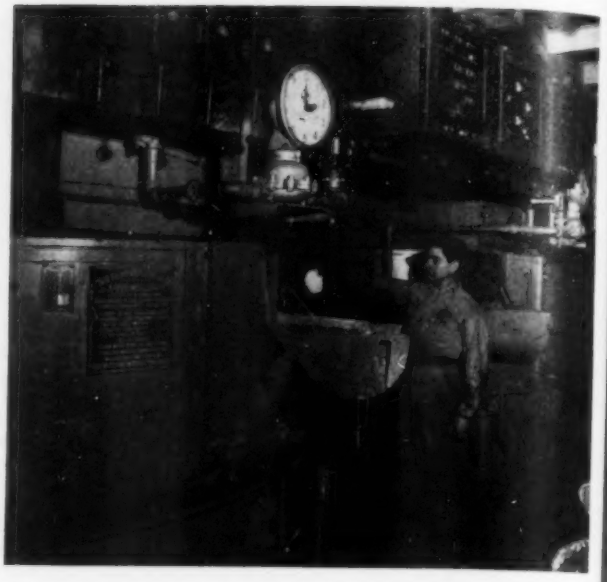


Fig. 9—Sand muller for sand slinger unit equipped with full automatic control for measuring water, time for washing muller bowl, return sand addition, new sand addition, bond addition time, mulling and cooling time, and discharge time.

maintenance amounts of bentonite and cereal and the elimination of sea coal and pitch, with their troublesome residues and water-proofing effects, make the sand easier for the muller operators to control within a given set of specifications.

The sand supervisor has a more difficult job of control because all the working properties are dependent upon the chemical coating on the sand grain and the grain distribution, which materially affects the working surface area. It is imperative that the coating around the sand grain be maintained by sufficient additions of the chemical solution to counteract any circumstances which allow uncoated sand grains to enter the sand system, and control the grain distribution for the most efficient working and refractory properties.

The most important item of control is the amount of chemical solution to be added and this is fixed by the supervisor. He is guided by the A.F.A. Fineness tests and the total carbon analyses from the laboratory, but the amount of chemical to be added is selected on the basis of the actual working properties of the sand in the shop. It has been found that, with a given grain distribution, as the chemical coating on the sand grains is reduced, there will be an increase in the amount of bond additions and a higher percentage of moisture necessary to develop the desired working properties. Conversely, if the resin coating is held constant, and the grain fineness is reduced, the bond additions will increase and the percentage of moisture will be higher for good plasticity. The "peeling action" on certain pilot castings is also used to determine the percentage of resin coated grains in the sand.

The sand specifications, the addition of chemical, wheat flour and the type of bond is fixed by the supervisor. The amount of bond and cereal is varied by the muller operators to meet the specifications and they are

guided by sand tests directly at the mullers. (Seven per hour on machine unit and five per hour on the sand slinger unit). The operators are instructed to add enough bond, corn flour and water to make a sand which will have good plasticity and strength, and it is the supervisor's responsibility to see that the resin coating and grain distribution are held within set limits so that the percentage of moisture will be within the specifications.

In general more control work is necessary to keep the chemically bonded sands within a given set of specifications. Intelligent and cooperative personnel to prepare the sand is a requisite, and their supervisor must be a good all around foundryman to direct them efficiently.

Reduction in Cleaning Costs

In general, there has been a reduction in cleaning time with the use of chemically-treated sand, some castings being affected more than others. The good shakeout and refractory properties deliver castings to the cleaning room with a minimum of molding sand, and this has greatly improved the work stations where the castings are prepared for subsequent cleaning operations, by reducing the amount of sand to be handled. Sand blast time has been reduced as much as 50 per cent in some cases, and substantially reduced for all castings. See Fig. 8 for illustration of this property.

One of the most notable improvements has been on the finish of the casting surface which had been a vertical surface in the mold. Grinding, for appearance, of areas having metal penetration caused by a lack of flowability has been greatly reduced.

Reduction in Preparation Time and Materials

The time necessary to prepare the sand in the mullers has been reduced and more sand can be supplied to the molding machines. With the synthetic sands, the mixing cycle was 2.2 min., which consisted of 1.5 min. of mulling, after all components of the mixture had been added to the muller, and 0.7 min. for loading, adding bonds, and unloading. The chemically treated sand requires only a 1.4 min. cycle with 50 sec. mulling of all components.

It was quite well established that the 1.5 min. mulling time of the synthetic sands (Table 2) was not adequate and that the excess of bond and cereal used to develop working properties caused a bad shakeout condition and reduced the flowability to a point where the finish was not good. Additional strength and toughness could be obtained by additional mulling and the sand would be smoother and free of lumps of bonding material.

The chemically treated sand is at the maximum strength and toughness, which can be developed by a bond addition, after 50 sec. of mulling. Prolonged mulling does not detract or improve the working properties. On two different occasions, a muller breakdown occurred and we mulled the sand only 30 sec. for a period of 12 production hours with no detrimental effects at the shakeout or in the working properties, but there is excess bonding material in the sand, when this cycle is used and, in time, may cause difficulty.

Fig. 10 (left)—Drag sides of oil pan castings—weights are 120 lb. and 110 lb. with 5/8-in. metal sections. Fig. 11 (right)—Cope sides of oil pan castings.

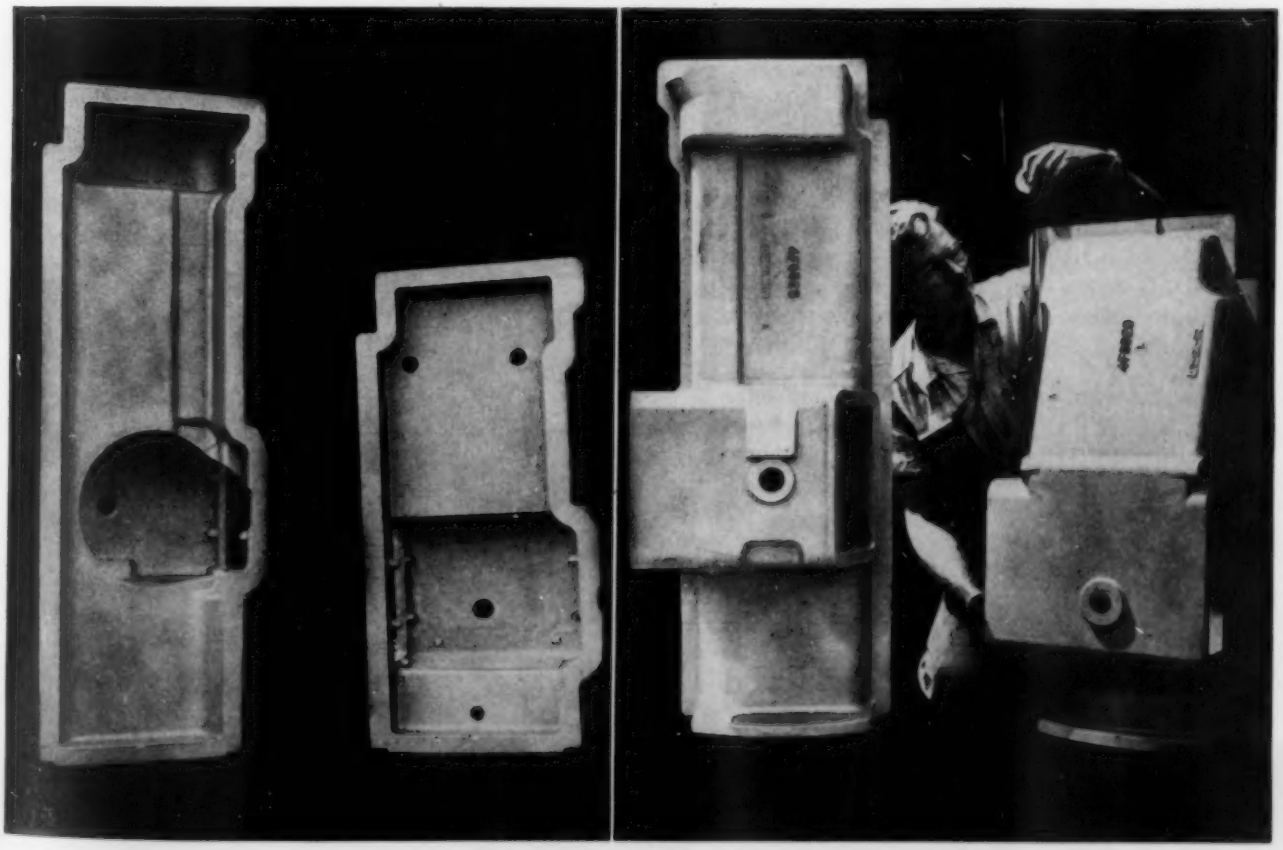




Fig. 12—Flywheel casting weighing 460 lb. It is 33 in. in diameter.

The additional supply of sand from the same mulling equipment increased the potential mold production and sharply decreased the time lost waiting for sand.

Bond and cereal additions at the mullers have been reduced with the chemically bonded sand. Average additions of the bentonite of 3.4 lb. and cereal flour of 1.4 lb. to a 1150-lb. batch of synthetic sand have been reduced to 1.7 lb. of bentonite and 0 to 0.3 lb. maximum of cereal flour. Sea coal, pitch and other dry carbonaceous materials have been entirely eliminated.

The materials, which the foundry has been using to develop working properties in the molding sands, are interchangeable in the chemically treated sands. The exceptions would be those materials, such as some of the carbonaceous materials, which would absorb the chemical and develop no plasticity, or increase the carbon content of the sand beyond a safe limit.

Extended Range of Castings in One Sand

By virtue of the use of a finer grain sand with good flowability and lower permeability requirements, it is possible to extend the use of one chemically treated sand mixture over a wider range of castings. The three molding machine units are serviced by a common distribution belt and shakeout conveyor, and since the larger loop (Fig. 13) uses the larger flasks, and usually makes the heavier castings, it becomes the dominating loop with its influence on the properties of the sand. It was, therefore, necessary with the synthetic sands, to maintain good finish and, in some cases, prevent misruns, to use facing sands on most of the smaller castings. The manner in which the refractory carbon resin is distributed around each grain imparts almost the same cleaning action to a thin, light casting as is obtained on a heavy, thick-sectioned casting. This property has provided the means to eliminate facing operations entirely and increase mold production proportionately.

Compare the illustrations of the oil pan (Fig. 10 and 11) and the flywheel (Fig. 12) which are made in the same sand on the sand slinger unit, and the sprocket wheel (Fig. 16 and 17) and the motor ends (Fig. 15) which are made on the same production schedule on the molding machine unit.

Personnel Reaction to Chemically Treated Sand

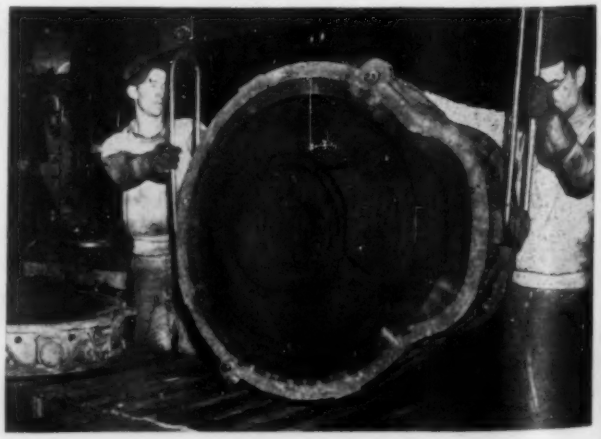
The men engaged at work in the molding machine unit are in hearty accord with the change to the chemically treated sand. The reduction in dry materials and the elimination of sea coal and pitch in the sand mixtures has elicited favorable comment from the sand mixing crews. The reduction in ramming operations, less time lost waiting for sand, and fewer losses of molds coming from the machines, could only be favorable in its reaction. There are more fumes created at the shakeout, but they are not toxic, are very much lighter, and seem to pass out through the exhausts much more rapidly than those formerly associated with synthetic sand containing sea coal. The volume of fumes does not seem to affect the shakeout crews, and, as a whole, the shakeout operation has been simplified somewhat by the sand leaving the castings much easier. The general shop atmosphere, we believe, is cleaner.

Chemically Treated Sand in Sand Equipment

The Engineering Department at the author's plant made a careful and thorough check of the use of the chemically treated sand insofar as sand handling equipment is concerned. They report as follows:

1. Cleaner discharge from rubber belts and metal conveyors.
2. Cleaner storage tanks over the molding machines.
3. Bridging eliminated in pant-leg hoppers over mullers.

Fig. 13—Drag mold of tractor wheel showing size and depth of one of the sand lifts made on the jolt-squeeze-strip molding machines. In the synthetic sand, there was the problem of making the sand sufficiently tough to lift out of the wide deep pocket and ram hard enough in the close space between rim of the wheel and the flask.



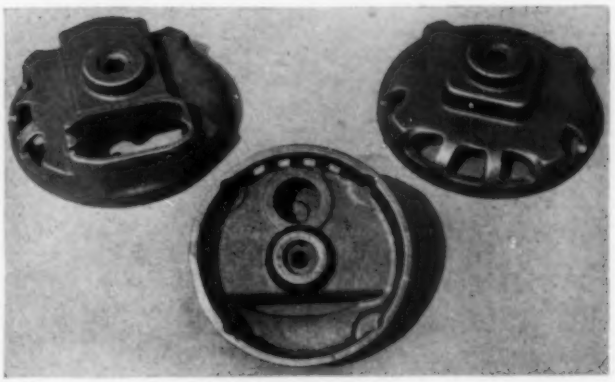


Fig. 15—Fractional horsepower motor end castings weighing 3½ to 4½ lb. with 5/16-in. metal section thickness.

4. Elevator cups discharge more completely.
5. Less sticking on the sides of elevators.
6. Better discharge of prepared sand from conveyor to elevator.
7. Three times as abrasive on the cup and liner in the sand slinger head as the synthetic sands.
8. Twenty times more replacements on muller plows in the speed mullers.

The selection of a sand mixture is wholly dependent upon certain castings entering into the foundry production schedule. For example, on the sand slinger unit, the flywheel (Fig. 12) might replace the oil pan (Fig. 10 and 11), or on the molding machine unit, the sprocket wheel, (Fig. 16 and 17), might replace the small motor ends (Fig. 15).

The grain fineness of the sands of both units should be higher, particularly on the machine unit where the smaller castings are made and the finish is of more importance. The exhaust system of the machine unit discharges into the atmosphere and creates a problem in keeping the proper grain distribution. The comparatively hot shakeout of the castings and some compounding of the sand grains also influence grain fineness.

TABLE 5—CHART OF CHEMICALLY BONDED SAND MIXTURES NOW IN USE

	Jolt-Squeeze Machine Unit Per 1150 Pound System Sand Mixture Symbol							Sand Slinger Unit Per 1600 Pound System Sand Mixture Symbol						
	A	B	C	D	E	F	G	A	B	C	D	E	F	G
Western Bentonite	0.50	1.00	0.75	0.00	1.50	0.25	1.75	0.50	1.00	1.00	0.50	0.50	1.00	2.50
Southern Bentonite	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00
Wheat Flour	0.50	0.50	3.00	5.00	0.25	0.00	0.50	0.00	0.00	0.00	1.00	0.50	0.50	0.50
Corn Flour	1.25	1.50	2.00	0.50	0.25	0.25	1.50	0.00	1.50	1.00	0.50	0.50	0.25	2.00
Fireclay	0.00	0.00	2.00	0.00	0.00	0.00	0.50	0.00	0.00	0.50	0.50	0.00	0.50	0.00
Chemical	4.00	2.00	3.00	3.00	3.00	3.00	1.50	2.00	2.00	2.00	2.00	1.00	1.00	1.50
10-M Sand	0.00	0.00	0.00	10.00	0.00	2.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00	0.00
Sludge	0.00	0.00	0.00	0.00	0.00	0.00	0.00	10 pounds per 1000 pounds shakeout sand each mixture						

NOTE: All figures shown in Table 5 are in quarts of material. The chemical is used as a solution of one part chemical and one part water. The chart records are the sand mixtures used between June 15 and August 23, 1946.

At the shakeout, measured amounts of No. 60, No. 90 or No. 111 sand as shown in Table 1, are added to maintain the amount of sand in the system or adjust the grain fineness. Because of the loss of the fine grains through the exhaust system, No. 90 or No. 111 is usually added to control fineness and No. 60 for bulk (Fig. 4).

The sludge, indicated as an addition, is the material collected from the exhaust system of the sand slinger unit. It will have an average fineness of 70 and will consist of fine coated grains, some bond and cereal, and water.

Cereal flour of the weaker type is preferred because more by volume can be used to provide a better counteraction of the expansion of the silica grain during the pouring operation.

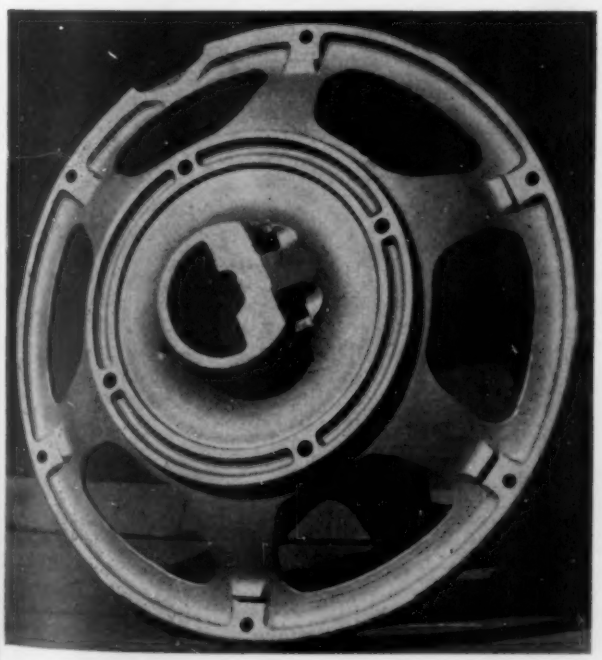
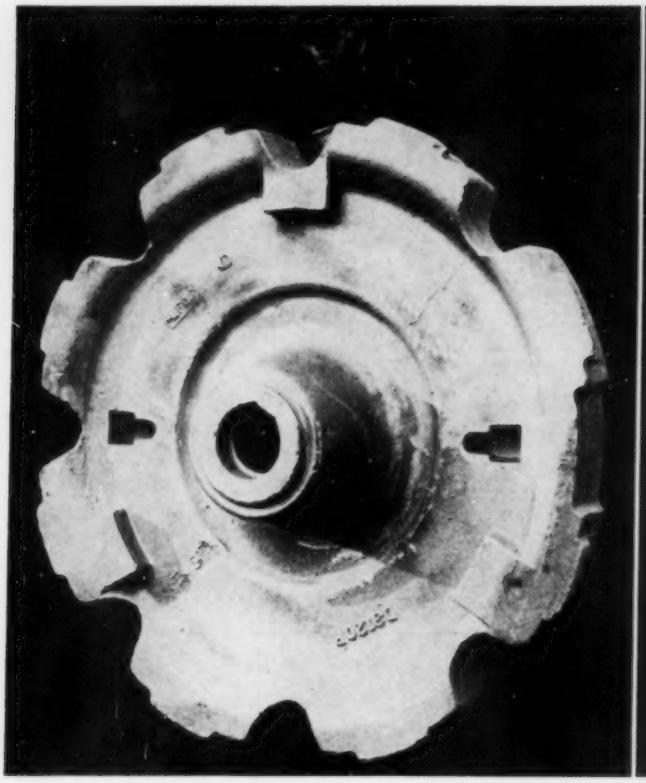


Fig. 14—Cope side of 257-lb. tractor wheel casting. There was a marked improvement in production of this casting in that the chemically-treated sand drastically reduced the number of "dropped" molds coming from the machines reduced the cleaning time.

One feature of the sand mixtures and their properties as shown in Tables 5 and 6 is that the same green sand properties can be maintained and the high temperature properties varied to meet conditions without an ill effect upon the shakeout.

At the beginning of operations with the chemically bonded sands, Southern bentonite was used in conjunction with Western bentonite but was discontinued in favor of the Western bentonite-fireclay mixtures as long as the higher hot strength properties did not cause excessive lumping at the shakeout.

Recent tests (See Sand F for machine unit, Table 5) have indicated that the 10-M molding sand, added in



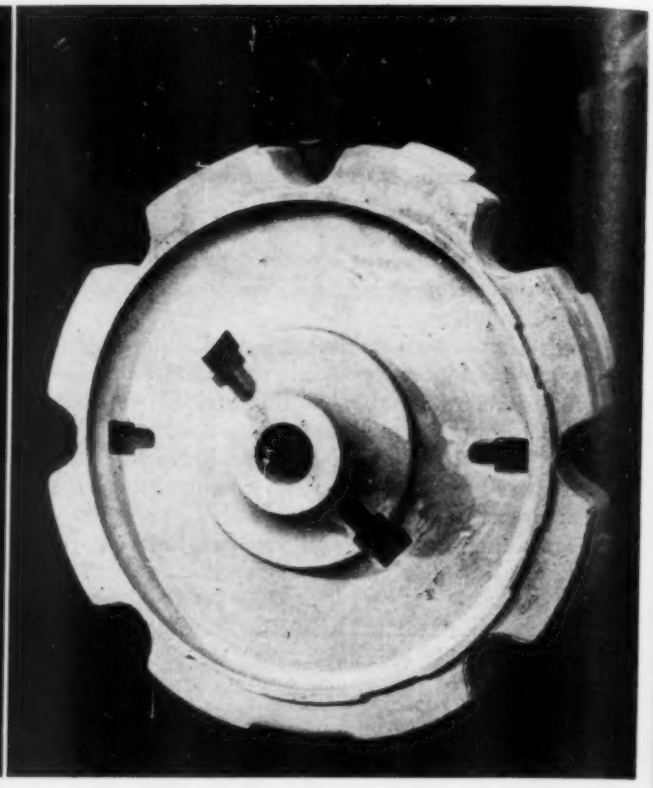


Fig. 16 (left)—Drag side of a 335-lb. sprocket wheel (5 in. in thickness at hub and web) poured through pop gates over the top of the center hub core. The chemically treated sand requires a high cereal addition to make this casting. Fig. 17 (right)—Cope side of tractor sprocket wheel.





Fig. 18 (left)—Cope side of an automotive flywheel weighing 107 lb. with metal sections ranging from 2 to 3½ in. in thickness. Fig. 19 (right)—Drag side of automotive flywheel.



Fig. 20—Core-setting operation for transmission case core shown in Fig. 21.

TABLE 6—PROPERTIES OF CHEMICALLY BONDED SAND MIXTURES NOW IN USE

Joint-Squeeze Machine Unit Mixtures A, B, C, D, E, F, G		Sand Slinger Unit Mixtures A, B, C, D, E, F, G
3.4 to 4.5 per cent	Moisture (Oven Test)	3.2 to 4.4 per cent
8.2 to 10.6 psi	Green Compression	8.3 to 11.7 psi
0.020 to 0.025 in.	Deformation	0.019 to .024 in.
88 to 134	Permeability	93 to 128
37 to 49	AFA Fineness	42 to 51
3.46 to 4.22 per cent	Total Carbon	2.96 to 3.40 per cent

NOTE: Our sand control procedure involves control testing in the foundry at the mullers and check tests in the main sand laboratory. In the laboratory at the mullers tests for moisture, green compression, deformation and permeability are made seven times per hour for the machine unit sand and five times per hour for the slinger unit sand. In addition to these tests, twice each day on each unit, a sample is taken for a complete test in the sand laboratory as shown in Table 6. The working properties shown are the lowest and highest result from the laboratory test obtained for each item, during the period June 15 to August 25, 1946.

Average properties for the machine unit would be as follows: green compression 8.7 to 9.9, permeability 88 to 115, total carbon 3.46 to 3.90 per cent, and the moisture, deformation and fineness as shown.

Average properties for the sand slinger unit would remain within the following limits: green compression 9.3 to 10.1, permeability 93 to 115, total carbon 3.06 to 3.40 percent, moisture 3.2 to 3.9 per cent and the deformation and fineness as shown.

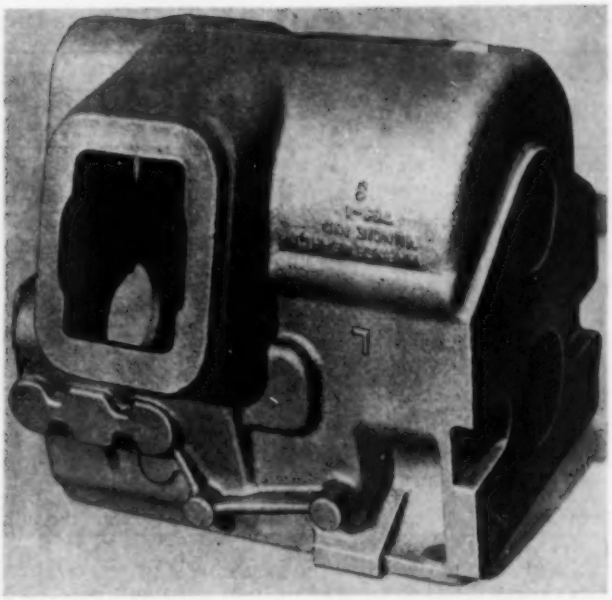


Fig. 21—Transmission case weighing 53 lb. A noticeable improvement in finish and loss by misrun and cracking resulted when the chemically treated sand was used in this application.

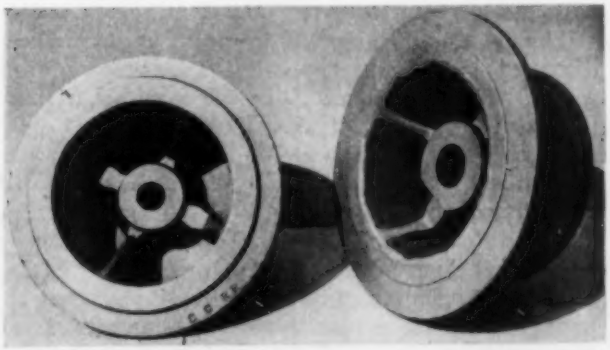


Fig. 22—Drag side of a tractor drum weighing 100 lb. The surface finish of the four deep pockets must be smooth, free from sand, and held to close dimensional tolerances because machining operations are jigged from these points. Toughness with good flowability was required in the molding sand and failure to have both exactly right either resulted in a lost mold or additional grinding operations in a very difficult area to clean properly.

the form of a bond addition at the mullers, has improved the working properties. There is not sufficient data available at this writing to be definite as to the results, but it appears that ample toughness and good thermal stability will be developed for all our castings with a reduction in the amount of cereal and bentonite required for maintenance of sand properties.

The question of the number of sand mixtures, or the number of materials used in one mixture, arises. Consideration must be given to the fact that the castings made must be treated as jobbing work on a high production basis and also that the chemistry of all the sand

TABLE 7—DATA ON COMPOUNDING OF CHEMICALLY TREATED SAND GRAINS

	Sand Slinger Unit								Molding Machine Unit							
Sieve	A		B		C		D		A		B		C		D	
Number	U	W	U	W	U	W	U	W	U	W	U	W	U	W	U	W
30	1.0	—	1.8	—	1.2	—	2.6	—	4.0	—	6.8	—	2.0	—	3.0	—
40	13.6	2.6	17.0	2.8	15.0	5.6	20.0	3.4	20.4	3.8	32.4	3.0	18.2	4.8	17.0	3.6
50	34.0	11.8	36.2	14.0	32.0	17.6	34.0	16.2	36.0	15.6	33.4	13.0	34.4	18.0	33.6	12.2
70	35.4	37.6	33.0	36.0	35.0	36.0	30.2	39.2	27.2	37.0	20.0	34.6	30.6	37.0	31.4	31.6
100	13.0	30.6	12.0	29.4	13.4	25.4	10.4	25.8	9.6	27.6	5.0	29.4	11.2	25.0	12.2	29.2
140	2.0	10.0	1.2	9.2	2.2	6.8	1.0	6.8	1.0	7.6	1.0	8.6	1.8	7.2	1.2	10.0
200	—	2.6	—	3.4	—	2.6	—	1.4	—	2.0	—	2.8	—	1.6	—	3.0
270	—	1.0	—	0.6	—	—	—	1.4	—	1.0	—	1.0	—	0.2	—	1.0
Pan	—	2.0	—	1.4	—	—	—	1.0	—	1.8	—	2.4	—	1.0	—	2.4
Fineness	46	67	46	64	46	54	43	59	42	63	38	65	44	59	41	65

LEGEND: A, B, C, and D represent samples of sand taken from the molding machine tanks and the sand slinger on different days.
U designates the sand as it comes from the storage tank and dried thoroughly to remove all moisture and solvent.
W designates the same sample as in U, washed by the A.F.A. method of determining clay content. Water was the only flotation medium used.
There is, of course, some compounding of the grains during the actual drying of the sample, but it is comparable to the conditions experienced in the mold at some integral distance from the surface of the hot metal.

mixtures is held within the same basic group of materials. Effort has been made to find a chemically treated sand mixture which would serve to make all our castings but the type of casting and the range of section sizes are too wide. The alternative is to hold a given set of working properties, vary the high temperature properties to suit the current production schedule, and take full advantage of the shakeout properties available with the chemically-treated sand.

The question of compounding of the grains has been discussed by foundrymen who have observed and worked with this process. The writer wishes to point out that the carbon resin, once set on the sand grain by evaporating the solvent and water, is insoluble in water. Mechanical force may separate some of the grains, but if the bond between the grains was the carbon resin alone, the breakdown shown in Table 7 would seem to be unusual since only a water wash was employed.

This same amount of compounding is present in the regular synthetic sands containing bentonite, cereal flour, and sea coal, and the breakdown of the compounded grain occurs with the same size of grains. The compounding illustrated is not advantageous, but it cannot be considered a disadvantage when the results of the same tests of synthetic sands are tabulated for comparison:

SYNTHETIC SANDS

Sand	Fineness Unwashed	Fineness Washed
A	45	62
B	49	69
C	52	63
D	35	67
E	39	51

Apparently the same amount of compounding of the sand grains is in evidence with both the synthetic and the chemically treated sands. However, there is no question that the finish of the casting made with the chemically treated sand is better than that with a synthetic sand of the same fineness number.

In a comparison of the sands in the molding system, the synthetic sands will lose plasticity and workability with a loss of moisture from the muller to the pattern plate, while the chemically treated sand will gain plasticity from the muller to the pattern plate with the same loss in moisture. The chemically treated sand will lose a higher percentage of moisture because of the lower moisture content of sand leaving mullers.

This gain of plasticity in the case of the chemically treated sand is due to the speed at which the resin coating can be made plastic, and the time interval between the discharge at the mullers and the molding operation (20 to 30 min.) is sufficient to bring the sand to the best temper. Additional time in the mullers does not change the sand materially because only a fraction of a minute of allowed time would be available if the production rate is to be maintained. As the resin coating nears complete plasticity, the compounding of the grains, whether the compounding is a result of the cereal-bentonite or the chemical, is reduced and an improvement in finish is obtained. Flowability also improves as the temper of the sand improves.

The synthetic sands would require several hours at a moisture content above that desired for molding and pouring to attain the best temper.

Recommendations have already been made to keep a larger supply of sand ready for the molding machines to take advantage of this improvement in the properties of the sand.

General Conclusions

The author's experience with the chemically bonded sand in his trial production run, to date, has indicated that further improvements can be made. Experimental work on each of these will unquestionably show trends toward refinements of a foundry molding sand which we believe has great possibilities now and even greater possibilities in the future. The author's organization has benefited in many ways from this work, and we hope that we have been able to contribute something of value to the foundry industry by using our facilities to give the process its first practical application in mass-production operations.

Note: All references to chemically-treated sands and the chemical treatment itself refer to a proprietary product or process known as Westonite, a development of B. M. Weston, Foundry Development Engineers, Inc., Detroit, Michigan.

HEAT TRANSFER

A.F.A. Committee Report

REPORT OF THE COMMITTEE CHAIRMAN

Dr. H. A. Schwartz *

THE FIRST PROBLEM taken up by the Heat Transfer Committee since issuing its last report concerned the question of how large an error would be introduced into the heat flow analyzer calculations by erroneous estimates as to the various thermal properties upon which the calculation rests. This project was completed at Columbia University and is covered in a paper by Dr. Victor Paschkis entitled "Studies on Solidification of Castings—Influence of Properties on Solidification of Metals." That report indicates that temperature is probably the most critical constant involved.

Since any calculations as to the heat transfer must involve assumptions as to the thermal conductivity of sand, it became obviously important to determine whether the conductivity of this material would change with temperature. There was reason to believe that the contribution made by radiation should increase the apparent conductivity with increasing temperature. It is impossible to examine this problem in complete detail, for, in a commercial sand there is heat transfer by conduction, by radiation and by convection (of steam and other gases from the heated sand to colder regions).

Grain Size Affects Conductivity

To determine the effect of grain size alone on the thermal conductivity of a granular material, experiments were conducted under the direction of Dr. H. W. Russell at Battelle Memorial Institute on the apparent thermal conductivity of silica sands of various grain sizes. This work is covered in a paper by F. C. Lucks, O. L. Linebrink and K. L. Johnson, entitled "The Thermal Conductivities of Three Sands." A study of the conductivity of a sand of mixed grain sizes will be reported in the future.

The work of comparing the results of bleeding tests with the results obtained by the heat flow analyzer has been continued into the field of cast iron and aluminum. In the interest of simplicity, white cast iron was chosen to escape the need of considering the heat change accompanying graphitization just below the solidus. The bleeding tests and cooling rates were obtained at the Cleveland Works of the National Malleable and Steel Castings Company and the results are reported under the title "Freezing Rate of White Cast Iron in Dry Sand Molds" by H. A. Schwartz.

The results on aluminum and some of its alloys were obtained through the kind cooperation of Louis Kempf and under the supervision of Dr. Kent Van Horn in the Cleveland Laboratory of the Aluminum Company of America. The results are reported in the paper, "Solidification Rates of Aluminum in Dry Sand Molds," by H. Y. Hunsicker.

Bleed Tests Practical

Considerable difficulty was experienced in the experimentation with white cast iron due to the growth of long and fragile dendrites in the liquid metal. Similar difficulties were encountered with aluminum alloys but were eliminated with pure aluminum. The work on cast iron and on aluminum has, of course, practical application. The work on aluminum is incidentally interesting as applying to a pure metal with a fixed freezing point and not a freezing range. This results in the need for special expedients in the investigation at Columbia University.

The comparison of the bleeding tests with the results predicted by the heat flow analyzer is in progress.

A brief survey has been made by Dr. Paschkis in the light of information so far gathered on steel, looking toward the problem of the relative rate of freezing of slabs of different thicknesses poured under the same conditions, irrespective of what these conditions may be. It is thought that an approach of this character may give light on the question of the freezing and feeding of castings, without the need of going through another experimental or mathematical investigation of the absolute time required for layers of various thicknesses to form. If this problem can be answered in some simplified way, most of the foundryman's needs for this particular purpose would be met.

Dr. Paschkis' paper, "Studies on Solidification of Castings—Approximate Equation of Steel Castings," may be considered an approach to the development of general rules for solidification.

* Manager of Research, National Malleable and Steel Castings Co., Cleveland, Ohio.

This concludes the work completed by the Committee during the current year. Work immediately ahead concerns a study of the heat conductivity of sands and mixed grain sizes, the completion of the comparisons between the heat flow analyzer work and the laboratory work on the freezing rate of cast iron and aluminum and an extension of the work begun in the last paper by Dr. Paschkis. The latter study is based on more extensive work on the heat flow analyzer, and includes a discussion of the slab, the cylinder and the sphere. A comparison of the predictions of Columbia with the work of Briggs and Gezelius on spheres' conductivity at the National Research Laboratory would also prove useful.

In broadening the experimental base of the Committee's work, it is hoped to extend the investigation into at least one of the copper-base alloys.

INFLUENCE OF PROPERTIES ON SOLIDIFICATION OF METALS

By
Dr. Victor Paschkis *
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New York City

I. Purpose and Scope of Experiments

The work covered by the present report is a continuation of the cooperative program carried out last year under the joint sponsorship of the AFA and Columbia University. Last year's work was reported in *Transactions*, American Foundrymen's Association, vol. 53, p. 87 (1945). By the reports, the validity of the electric analogy method has been tentatively established. The material on which last year's experiments have been carried out was selected because it was believed that the thermal properties for this material are known with reasonable accuracy.

The success of the electric analogy method depends largely on the correctness of the properties fed into the "Heat and Mass Flow Analyzer." In order to cover the different aspects of metal solidification, it will be necessary, obviously, to determine properties of a great variety of metals. In view of the size of the job involved, it appeared desirable to find out the relative importance of all thermal properties. The purpose of the experiments covered by this report is to find out whether all properties are of equal importance.

Study Pouring Temperature Influence

The ease with which experiments can be carried out by the electric analogy method made it possible to investigate to some extent the influence of the pouring temperature and to make a few tests with metals other than steel.

All experiments were carried out for a slab four inches thick, poured in sand of 10 in. thickness. In the experiments for nonferrous metals a sand thickness of only 8 in. was assumed. Because of the very short solidification times this difference in thickness is practically of no influence on the results.

Because the properties of sand are relatively easy to determine, the AFA Heat Transfer Committee decided to have all work carried out for the same sand, the properties being the same as those used last year. These properties are as follows:

Specific Heat	0.28	Btu/lb/°F
Density	93.6	lb/cu ft
Thermal conductivity	0.9	Btu/ft/hr/°F

The properties of the steel which were varied, were mainly thermal conductivity, volumetric specific heat (i.e., density x specific heat), and heat of fusion. From certain other experiments it appeared important to investigate the influence of a change of solidification range.

II. Preliminary Experiments

All "Preliminary Experiments" were carried out for the following "standard" steel properties and conditions, unless otherwise noted:

Thermal conductivity	—liquid,	9.2	Btu/ft/hr/°F
Thermal conductivity	—solid,	18.4	Btu/ft/hr/°F
Specific Heat	—liquid,	0.200	Btu/lb/°F
Specific Heat	—solid,	0.165	Btu/lb/°F
Density	—liquid,	450	lb/cu ft
Density	—solid,	468	lb/cu ft
Solidification range	—	2700–2600	F
Heat of Fusion	—	126.2	Btu/lb
Pouring temperature	—	2845	F

A—Lumping Procedure

As explained in last year's report, the electric analogy method is based on lumping. Casting and sand are thought of as being divided into sections or lumps. Last year it was found that the size of the lumps representing the steel and the sand have to be in a certain relationship. This relationship is based on the thermal properties of both materials and, inasmuch as the properties change between liquid and solid, the influence of the different methods of lumping was investigated. The results of lumping on the basis of "solid properties" were compared with those on the basis of "liquid properties." In a third combination the lumping was based on the conception of the heat of fusion being a thermal capacity.

The lumping based on liquid properties results in values which lie between those of the other two procedures. The differences between the three types of lumping are small and therefore all further experi-

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ments were carried out with the same kind of lumping as that used last year, namely "liquid properties."

B–Pouring Temperature

Almost all previous experiments were carried out for a pouring temperature of 2845 F. The only exceptions are the experiments holding for a pouring temperature of 2950 F which were included in Fig. 3 of last year's report. This year for one specific case the influence of the pouring temperature was investigated by systematically changing it from 2800 F to 3000 F. The results are shown in Figs. 1 to 4.

Figures 1 and 2 show the frozen thickness plotted against freezing time. Figure 1 shows the "start of freeze" and Fig. 2, the "end of freeze." The results are summarized in Figs. 3 and 4. In these figures the times for solidification of definite points are plotted against the pouring temperature. Figure 3 holds for "start of freeze" and Fig. 4 for "end of freeze." In both of these figures, two curves are shown, one for a point very close to the steel-sand interface, the other for the center of the casting.

Analogy and Bleed Tests Check

Last year's experiments proved that the curves for "start of freeze" check with those obtained by bleeding experiments. From Fig. 3 it may be seen that, measured in time, the influence of the pouring temperature is very small near the surface but very large at the center of the casting. The percentage increase, however, is much larger near the surface than in the center.

In Figs. 3 and 4 the curves are extended to "zero time." The "start of freeze" times, shown in Fig. 3 are those necessary to reach the temperature of 2700 F. If the steel were poured at 2700 (no superheat), the time to reach 2700 F would be zero. Of course this case is fictitious. Pouring at 2700 F would be impossible for this steel. Similarly the point "2600 F, zero seconds" in Fig. 4 is fictitious.

In Fig. 3 the curve for the center follows in its upper part approximately a square root law. Let F be the superheat (pouring temperature–2700) and the subscript denote the pouring temperature in hundred degrees Fahrenheit, and t be the time necessary to solidify the center, using the same subscript. F_{28} would then mean the superheat for a pouring temperature of 2800 F, or $2800 - 2700 = 100$; t_{28} is the corresponding solidification time. Then Equation 1 holds approximately for any temperature between 2800 and 3000 F

$$\sqrt{\frac{F}{F_{28}}} = \frac{t}{t_{28}} \qquad (1)$$

The results are important in connection with the selection of the proper superheat. Further information concerning the influence of the pouring temperature may be found in the section on Main Experiments. All experiments in that group were carried out for two temperatures, 2845 F and 3000 F.

C–Solidification Range

Some doubts have been expressed concerning the applicability of the solidification range, as shown in the constitution diagram, to the case of rather rapid

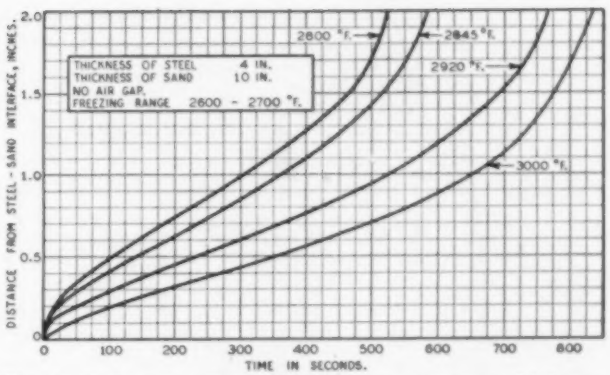


Fig. 1–Influence of pouring temperature on thickness frozen at start of solidification.

cooling occurring in foundry practice. (See page 95, *Transactions*, AFA, vol. 53, 1945.) The few experiments discussed in this section were made to show how the freezing times would change with the range of solidification. Three cases were compared, namely:

a. Solidification from 2700 F to 2600 F
b. Solidification from 2700 F to 2650 F
c. Solidification from 2650 F to 2600 F

In all instances the total heat of fusion was 126.2 Btu/lb.

The results of the comparison are contained in Table 1 and in Figs. 5 and 6. In the table, the first column shows the solidification range and the other columns the freezing times in seconds, for start and end of freeze. For the end of freeze two columns are used, one marked "surface," the other marked "center." The "center" refers to freezing time of center of the slab, whereas the point marked "surface" refers to the first lump, which is 0.07405 in. below the surface.

In the table there are four lines, the first three for the three different solidification ranges and the fourth entitled "2650 to 2600 corrected." This last line refers to the solidification range 2650 F to 2600 F. All experiments have been carried out for a pouring temperature of 2845 F. Thus the superheat for the experi-

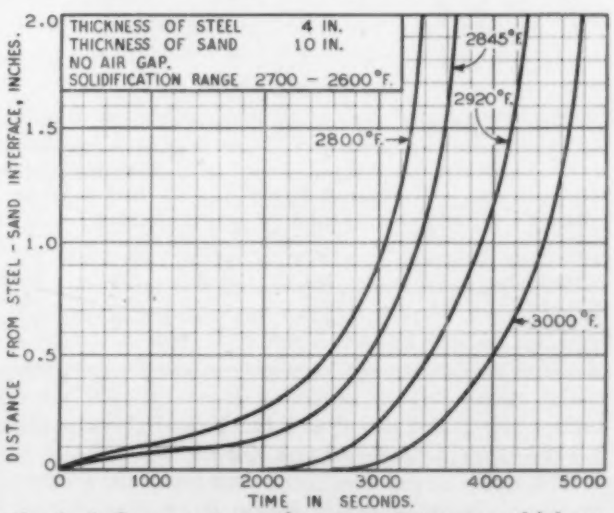


Fig. 2–Influence of pouring temperature on thickness frozen at end of solidification.

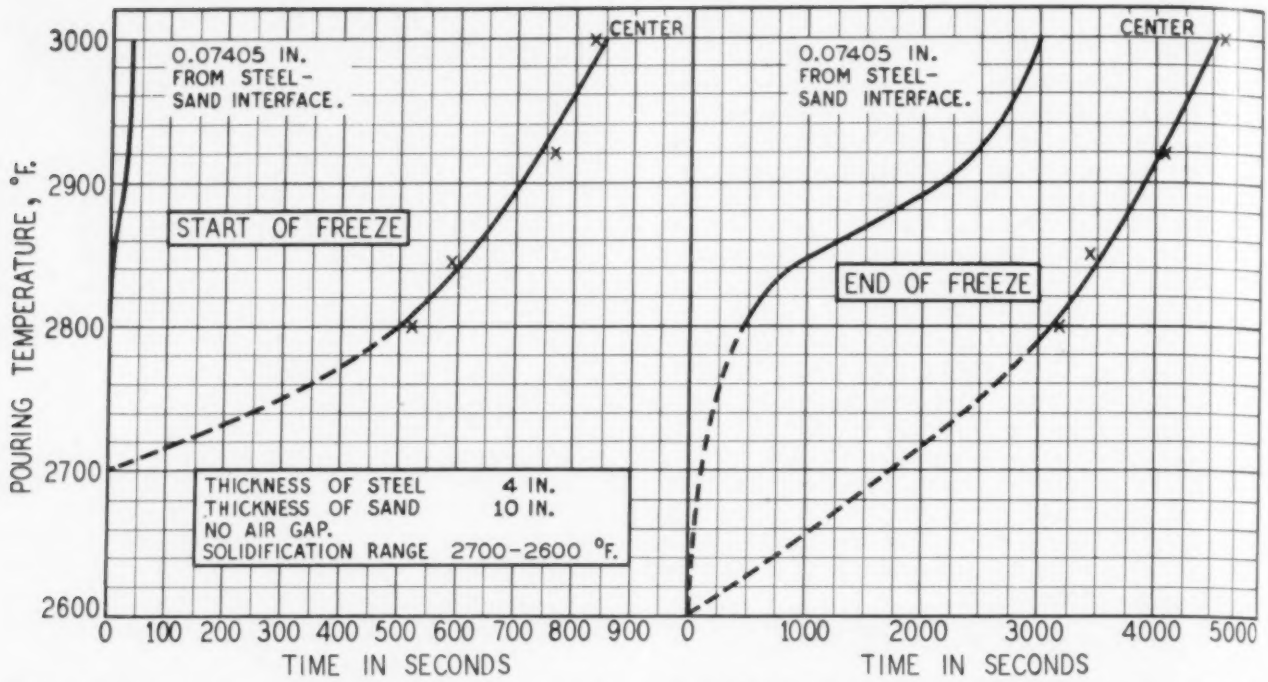


Fig. 3—Relation between pouring temperature and time for solidification of definite points at start of freeze for center and for layer at casting surface.

Fig. 4—Effect of pouring temperature on time for solidification at end of freeze for center and for layer 0.07405 in. from casting surface.

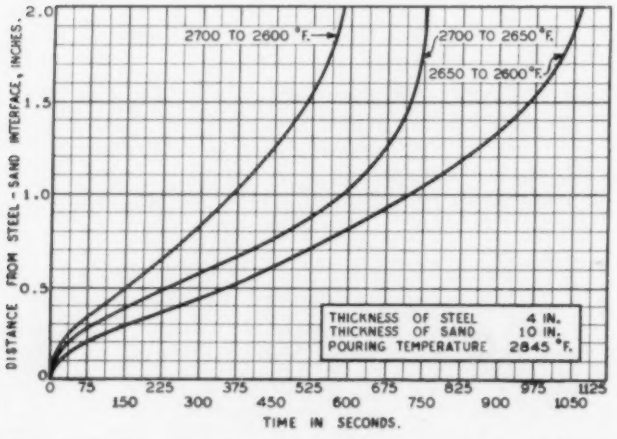


Fig. 5—Influence of solidification range of steel on thickness frozen at start of solidification.

TABLE 1.—HEAT OF FUSION RANGE

Range	Time in Seconds Start of Freeze Center	End of Freeze Surface	End of Freeze Center
2700–2600	585	901	3463
2700–2650	753	232	3140
2650–2600	1063	442	3755
2650–2600 corrected	927	212	3445

ments line 1 and line 2 was 145 F, and the superheat for the experiment line 3 was 195 F.

The times in the third line were corrected by multiplying them by a figure found from Figs. 3 and 4 in the following way: From Fig. 3 or 4 the time was read for a pouring temperature of 2895 F which represents a superheat of 195 F above the solidification temperature of 2700 F.

For example, for the "center," start of freeze, this time was 700 seconds. The solidification time was then read for a pouring temperature of 2845 F which represents a superheat of 145 F. For start of freeze at the "center" this time was 610 seconds. The value found for the solidification range 2650 F to 2600 F (1063 seconds) was reduced by ratio of 610/700 and thus the corrected "center" time of 927 seconds was found.

Obviously the corrections represent only approximations. It is noteworthy that the corrected times for the range 2650–2600 are close to the time for the range 2700–2650.

Figure 5 shows solidification curves for "start of freeze" and Fig. 6 for "end of freeze." The curves for the freezing ranges "2700 to 2600 F" and "2700 to 2650 F" can be compared directly. The comparison

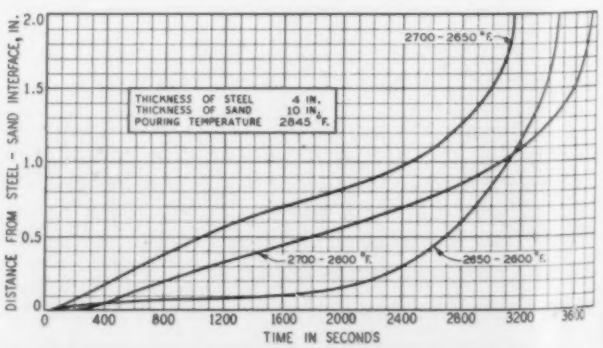


Fig. 6—Influence of solidification range of steel on thickness frozen at end of solidification.

Table 2.—Some Data on Freezing Time

Exp. No.	Conductivity Btu/ft, hr, F	Per Cent	Specific Heat Btu/lb, F	Per Cent	Heat of Fusion Btu/lb	Per Cent	See Fig. No.	Solidification Time at Center Pouring Temp. 2845 F Start	End
1	11.04	+20	0.180	−10	126.18	0	10, 14	436	3186
2	11.04	+20	0.200	0	126.18	0	7, 11	519	3460
3	9.2	0	0.200	0	113.56	−10	9, 13	586	2870
4	9.2	0	0.200	0	126.18	0	7-14	598	3463
5	7.36	−20	0.200	0	126.18	0	7, 11	690	3524
6	9.2	0	0.220	+10	126.18	0	8, 12	652	3630
7	9.2	0	0.240	+20	126.18	0	8, 12	695	3995
8	7.36	−20	0.240	+20	151.42	+20	10, 14	773	4750
9	11.04	+20	0.160	−20	100.94	−20	10, 14	350	2180
10	9.2	0	0.160	−20	126.18	0	8, 12	473	3116
11	9.2	0	0.200	0	100.94	−20	9, 13	566	2510
12	9.2	0	0.180	−10	126.18	0	8, 12	514	3340
13	9.2	0	0.200	0	138.80	+10	9, 13	621	4022
14	9.2	0	0.200	0	151.42	+20	9, 13	655	4755
15	9.2	0	0.280	+40	126.18	0	8	784	—
16	9.2	0	0.120	−40	126.18	0	8	375	—

with the curves for the ranges "2650 to 2600 F" is more difficult because the latter curves are taken for a larger degree of superheat, as explained in connection with Table 1.

Summarizing, it is apparent that the range of solidification has a big influence on the freezing times.

III. Main Experiments

The experiments reported in this section are summarized in Table 2 and results are shown in Figs. 7 to 14.

In Table 2, zero per cent indicates the "standard properties," ±10 or 20 per cent indicates deviation from "standard properties." Properties are for liquid steel. The solid properties were changed proportionally.

It is conceivable that only the solid or only the liquid properties might change as compared with the "standard properties." The solidification times in such cases, where only solid or liquid properties change, may be expected to lie between the times for

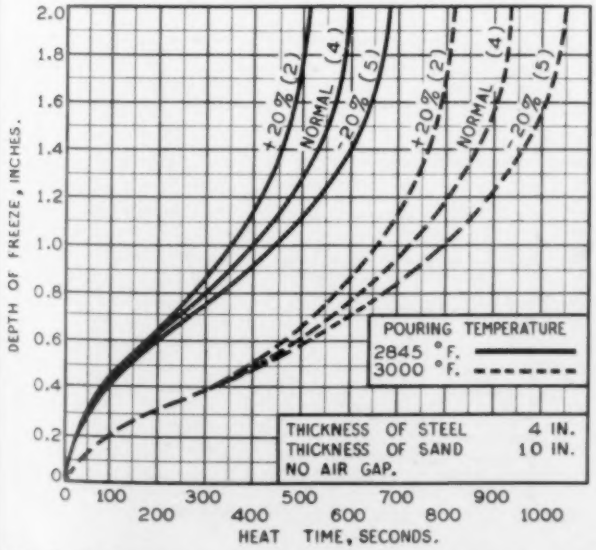


Fig. 7—Influence of thermal conductivity on start of solidification of steel for two pouring temperatures.

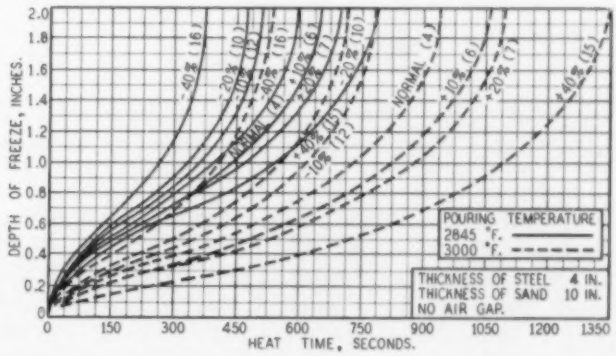


Fig. 8—Effect of specific heat on solidification at start of freeze shown for two pouring temperatures.

"standard values" and those times obtained with an equal change of both liquid and solid. Therefore, in order to reduce experimental time, changes on only solid or only liquid properties were not investigated.

Figures 7 to 10 show solidification times for "start of freeze," those in Figs. 11 to 14 for "end of freeze." In all charts solid lines refer to a pouring temperature of 2845 F, and broken lines to a pouring temperature of 3000 F. The influence of thermal conductivity can be studied from Figs. 7 and 11, the influence of specific heat from Figs. 8 and 12 influence of heat of fusion from Figs. 9 and 13, and the influence of combined changes from Figs. 10 and 14.

In Fig. 11 the area between two of the curves is shaded, indicating that the true values for all conditions lie between those lines, the accuracy of measurement not being safe to indicate the values separately.

The great number of curves make a comparison relatively difficult. The time of solidification for the center as characteristic point is summarized in Table 3 and Fig. 15. In the latter figure the solid lines show the times referring to a change of specific heat, the broken lines the times referring to a change of heat of fusion, and the dash-dot lines the times referring to a change of conductivity. Straight lines have been drawn. The measured points which do not fall on the curves are indicated also.

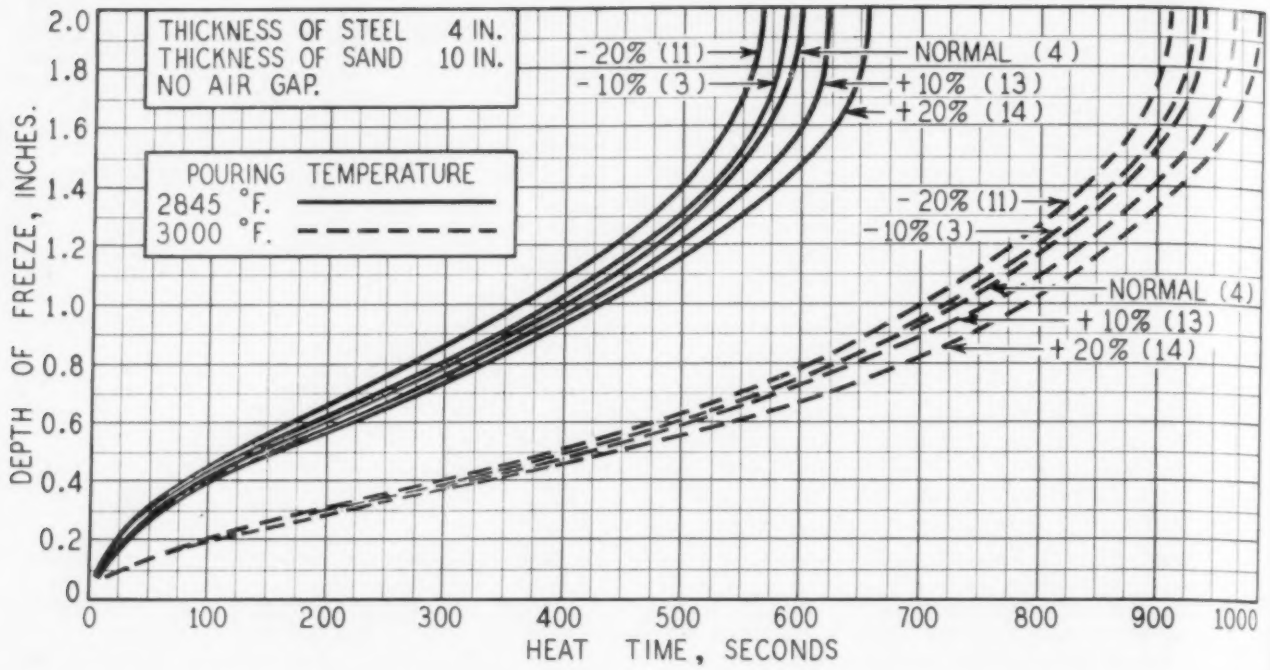


Fig. 9—Effect of heat of fusion on thickness of steel solidified at start of freeze, for two pouring temperatures. Compare with Fig. 12 for the same conditions at the end of solidification.

In Table 3, column 1 shows the deviation of the various properties from the standard value, line 3 ("0") holding for the standard value. Columns 2, 3, 4 show the times for change of specific heat, heat of fusion and thermal conductivity respectively. As expected, an increase of specific heat or heat of fusion (compare lines 1 and 3, columns 1 and 2) prolongs the solidification time, whereas an increase of thermal conductivity (lines 1 and 3, column 3) shortens the solidification time.

The figures in Table 3 show that the specific heat has a greater influence on the times for "start of freeze" than does the heat of fusion. This is surprising because the heat of fusion is much greater than the change in heat content (volumetric specific heat times temperature drop). The reason for this surprising behavior may be found in the fact that the heat of fusion for each section has no influence on the time necessary for the temperature drop of that section from 2845 to 2700 F. The heat of fusion influences time for "start of freeze" only for any part not yet solidified. For example, the heat of solidification for the first 0.1 in. under the casting-sand interface will influence the "start of freeze" time for the second increment of 0.1 in.

That this reasoning is correct becomes apparent from the fact that the "end of freeze" is influenced to a greater extent by the heat of fusion than by

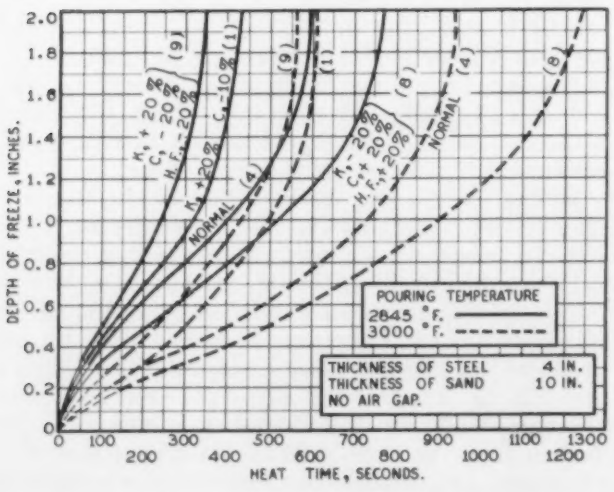


Fig. 10—Effect of combined changes on solidification at start of freeze. Curves for two pouring temperatures.

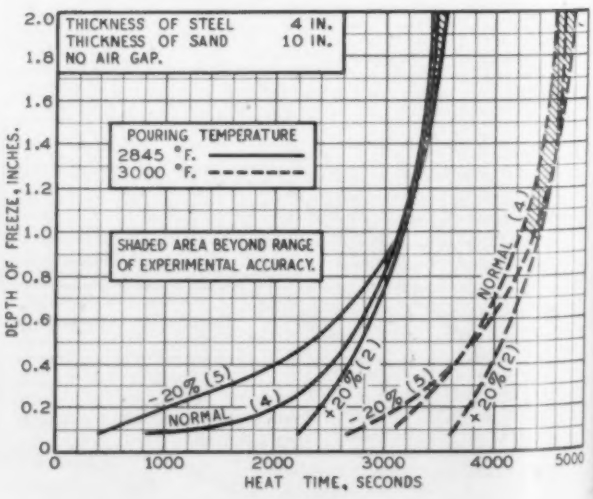


Fig. 11—Influence of conductivity on solidification at end of freeze, for two pouring temperatures.

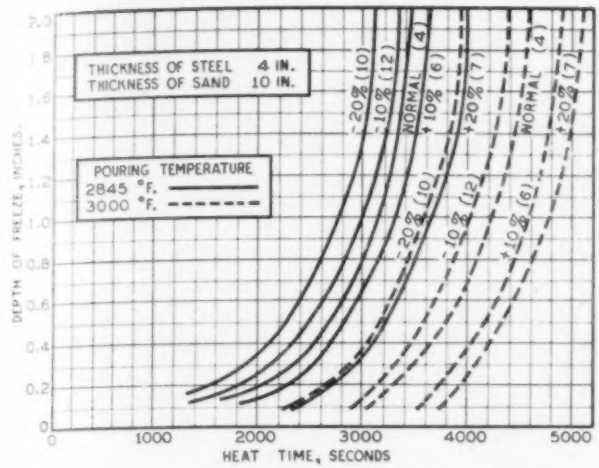


Fig. 12—Effect of specific heat on solidification at end of freeze. Compare with Fig. 9.

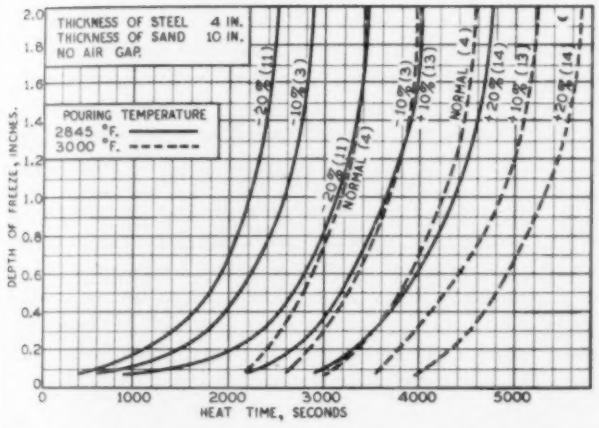


Fig. 13—Influence of heat of fusion on steel poured at different temperatures, end of freeze.

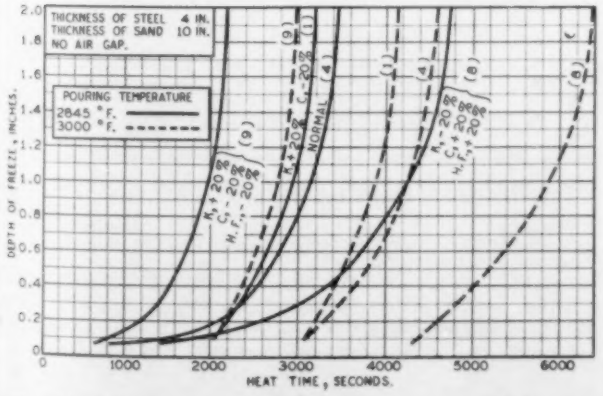


Fig. 14—Effect of combined changes on solidification at end of freeze, for two pouring temperatures.

specific heat. This will become obvious from Table 4. For "beginning of freeze" the time drops from 655 to 566 (1:1.16) for heat of fusion, and from 695 to 473 (1:1.47) for specific heat. For "end of freeze" the ratios are 1:1.90 for heat of fusion, and less, namely 1:1.28 for specific heat.

The solidification time for "no specific heat" is zero. Accordingly, the line of solidification times plotted against specific heat, if extended, would intersect the origin. Change of thermal conductivity was studied only in the more narrow range. It should be expected that with infinite conductivity (zero resistance) the solidification time would again be zero.

TABLE 3.—RELATIVE INFLUENCE OF CHANGES OF VARIOUS PROPERTIES ON "START OF FREEZE"

Deviation, Per Cent	Freezing Time (Seconds) Specific Heat	Heat of Fusion	Conductivity
+20	695	655	519
+10	652	621	
0	598	598	598
−10	514	586	
−20	473	566	690

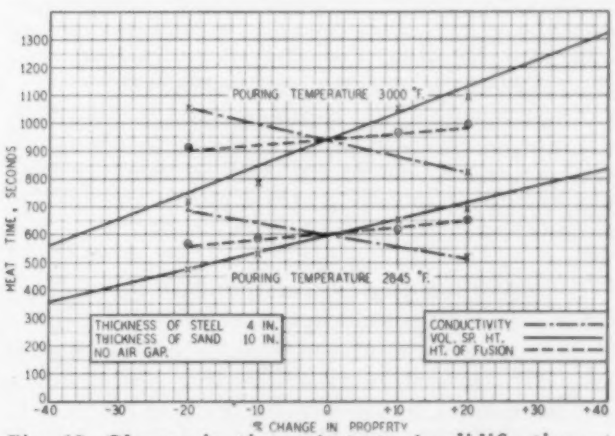


Fig. 15—Change in time of start of solidification at center as influenced by change in properties.

TABLE 4.—INFLUENCE OF SPECIFIC HEAT AND HEAT OF FUSION ON BEGINNING AND END OF FREEZE

Deviation Per Cent	Freezing Time (Sec.) Beginning of Freeze: Heat of Fusion	Beginning of Freeze: Specific Heat	End of Freeze: Heat of Fusion	End of Freeze: Specific Heat
+20	655	695	4755	3595
0	598	598	3463	3463
−20	566	473	2510	3116

In Fig. 16 are shown the results of the three experiments in which conductivities were changed; on the chart the curves have been extended to zero resistivity. It will be seen that the two lower resistivity values seem to lie nicely on the extended line going to the origin. At the highest resistivity it appears that the increase of freezing time slows down, the curve bending towards the abscissa.

The results of the conductivity tests for end of freezing are interesting, inasmuch as a lower conductivity results in shorter times for end of solidification. This

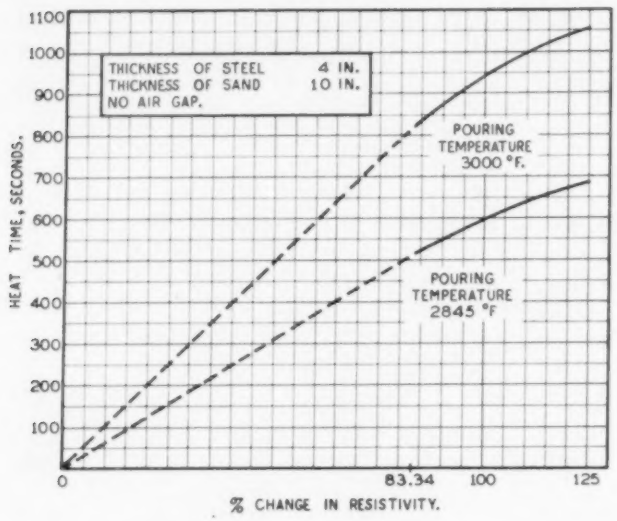


Fig. 16—Change in time of start of solidification at center change in thermal resistivity.

result seemed puzzling at first, therefore, the experiments were carried out several times. Between the experiments the circuit was entirely disassembled, and built up again, and all calculations were checked. The same results as previously were obtained.

The influence of the specific heat on the solidification time of the center can be expressed by the empirical equations, 2a and 2b, for 2845 and 3000 F pouring temperatures respectively. Let t denote the time in seconds and c the specific heat in Btu/lb/°F (liquid) then

$t = 2990\ c$ (pouring temperature 2845 F).....(2a)

$t = 4710\ c$ (pouring temperature 3000 F).....(2b)

The result of combined changes can be studied from Figs. 10 and 14, as well as from the figures in the last column of Table 2. A change of specific heat is more significant than one of heat of fusion, and the latter more than one of thermal conductivity, all influences referring to change of equal ratios (e.g., 10 per cent, 20 per cent, etc.).

Combined Property Changes

It was investigated to what extent the different changes of properties contribute to a combined change of property. For example, test No. 9 with 20 per cent increased conductivity and 20 per cent decreased specific heat and heat of fusion, showed a complete solidification time (start of freeze 2845 F) of 350 sec. The test for 20 per cent increased conductivity showed a freezing time of 519 sec., that of 20 per cent decreased specific heat 475 sec., and that of 20 per cent decreased heat of fusion 565 sec. The solidification time for standard properties was 600 sec.

The freezing time (start of freeze 2845 F) for experiment No. 9 may now be tentatively written:

$$\frac{519}{600} \times \frac{475}{600} \times \frac{565}{600} \times 600 = 380 \text{ sec.}$$

This expression is based on the assumption that the freezing time observed under "standard conditions" changes, as if all variations of properties would occur independently, or in other words, that the various changes may be superimposed. This calculated time compares with the observed time of 350 sec.

Table 5 compares the calculations of freezing time for combined changes of properties with the values found in the tests. Except for experiment No. 8, 2845 F, the experimental results check reasonably well with those from the approximate method of calculation, illustrated above.

IV. Other Materials

In order to get approximate information on the behavior of other materials, experiments Nos. 17 and 18 were carried out for brass and an aluminum alloy respectively. The properties and conditions used in the experiments are shown in Table 6.

The dimensions of the castings were the same as for steel, namely a 4 in. thickness (slab), backed by 8 in. of sand. Figure 17 shows solidification curves for brass, Fig. 18 those for the aluminum alloy. In order to compare the results with those of steel, Table 7 is offered.

A number of comments are in order:

a. Originally it was planned to study pure aluminum. However, aluminum, as does every pure metal, solidifies at one temperature, rather than solidifying over a finite range. Representation by electric analogy is more complicated for the case of one temperature than for a range of solidification. Therefore an aluminum-copper alloy was selected for the present tests.

Pure Metal Study Complex

b. Representation of a pure metal or a eutectic alloy with one specific solidification temperature would be done by feeding current to various points of the circuit. The rate would be such that the voltage (equivalent to the temperature) would be kept constant until a given amount of energy had been liberated (milliampere minutes equivalent to Btu, heat of

TABLE 5.—SOLIDIFICATION TIME (SECONDS) FOR CENTER

(Start of Freeze)

	2845 F		3000 F	
Exp.	Calculated	Test	Calculated	Test
9	380	350	606	565
1	446	435	640	610
8	866	773	1300	1242

SOLIDIFICATION TIME (SECONDS) FOR CENTER

(End of Freeze)

	2845 F		3000 F	
Exp.	Calculated	Test	Calculated	Test
9	2270	2180	2990	2970
1	3340	3180	4470	4140
8	5570	4760	6650	6380

TABLE 6.—SOME DATA ON BRASS AND ALUMINUM ALLOY

Property	Brass	Aluminum Alloy
Thermal Conductivity		
Liquid	67.2 Btu/ft/hr/°F	99.4 Btu/ft/hr/°F
Solid	67.2 Btu/ft/hr/°F	99.4 Btu/ft/hr/°F
Volumetric Specific Heat		
Liquid	57.8 Btu/cuft/°F	35.9 Btu/cuft/°F
Solid	57.8 Btu/cuft/°F	42.7 Btu/cuft/°F
Heat of Fusion	65 Btu/lb	158.3 Btu/lb
Solidification Range	1733 F to 1661 F	1166 F to 1040 F
Pouring Temperature	1922 F	1292 F

fusion). Several power supplies and ammeters would be required and the course of the experiments would be rather complex.

c. It is noteworthy that the "start of freeze" is different for the different metals, but that the differences in times for "end of freeze" are relatively small. An accurate comparison of the freezing times is impossible because the superheat for the three metals is somewhat different and hard to compare. But it is obvious that the freezing times increase with decreasing thermal diffusivity.

d. Experiments for highly conducting materials are substantially more difficult than those for steel. This holds true particularly for "end of freeze." The time till the surface reaches the "end of freeze" temperature is long, but once the surface reaches that temperature, it takes only a few seconds to reach the same temperature at the center also. If the electric experiments are short, the difference in time for solidification of various parts of the body are too small for accurate measurement. If the electric experiments are sufficiently long, leakage causes appreciable experimental difficulties. These difficulties are less marked for the "start of freeze" times.

V. Further Work

Recommendations for last year still hold. However, in view of this year's work and with particular reference to the selection of "important properties" (i.e., those which have great influence on solidification) the following comments are offered:

1. Experiments similar to those reported here should be carried out for different sand properties.

2. The experiments hold only for castings in sand, not for castings against chills. In studying the influence of properties on freezing against chills, the air gap time will be important. Therefore the study of air gap time is recommended before proceeding with such experiments. The technique is indicated in last year's report.

VI. Summary

The results of the tests can be summarized as follows:

a. The pouring temperature (superheat) has a marked influence on beginning and end of solidification.

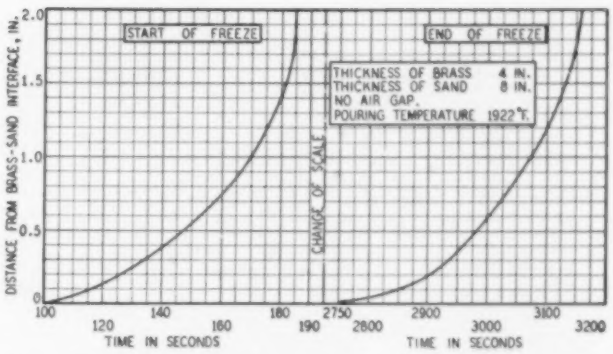
b. The solidification range influences beginning and end of solidification an appreciable amount.

c. The specific heat is the determining factor as far as times for beginning of freeze are concerned but has less influence on times for end of freeze.

d. The heat of fusion has a relatively small influence on start of freeze and a large influence on end of freeze.

e. Of the three thermal properties, the conductivity has the smallest influence on solidification times.

f. The pouring temperatures are, of course, a question of production technique. It is important to determine accurately the specific heat and with reasonable accuracy the solidification range and heat of fusion. Conductivity may perhaps be accepted at estimated values without making a great sacrifice in accuracy.



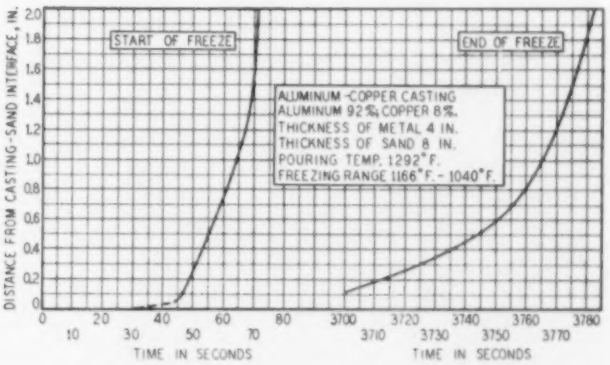


Fig. 17 and Fig. 18—Behavior of a brass (above) and an aluminum alloy (below) at start and end of freeze.

Table 7.—Solidification Time for Center of Casting

	Brass	Aluminum	Steel
Start of Freeze, sec.	185	71	598
End of Freeze, sec.	3160	3782	3463
Thermal Diffusivity Liquid, sq ft/hr	1.16	2.77	.1022
(Pouring Temp.—Upper Limit of Sol.)			
Upper Limit of Sol.	0.109	0.108	0.054

The A.F.A. Heat Transfer Committee

The Heat Transfer Committee is the outgrowth of a paper presented at the 1944 Annual Convention of the American Foundrymen's Association. Entitled "Heat Flow Problems in Foundry Work," this paper by Dr. Victor Paschkis, Columbia University, New York City, suggested that heat and mass flow analyzer techniques might well be used to solve complicated foundry heat flow problems. Using these techniques, Dr. Paschkis simulates heat flow under a given set of conditions by using electricity and appropriate electrical circuits. Under the sponsorship of the Committee, the electrical analogy method is being checked by means of tests carried out in foundry laboratories using molten metal. Present members of the Heat Transfer Committee are: Dr. H. A. Schwartz, manager of research, National Malleable and Steel Castings Co., Cleveland, chairman; J. B. Caine, metallurgist, Sawbrook Steel Castings Co., Cincinnati; Howard F. Taylor, associate professor, Massachusetts Institute of Technology, Cambridge, Mass.; E. C. Troy, vice president in charge of research and development, Dodge Steel Co., Philadelphia.

THERMAL CONDUCTIVITIES OF THREE SANDS

By

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Synopsis

The thermal conductivities of three sands were determined over the temperature range of 750 F to 2250 F. The sands tested were designated as 20–30, 50–70 (Ottawa Silica Company), and No. 7 (American Graded Sand Company). The sieve analyses for the sands are given in Table 8. The unit weights of the test specimens for the respective sands were 108, 102, and 91 lb/ cu ft.

The thermal conductivities of the three sands increased with temperature. The thermal conductivity of the 20–30 sand at 750 F is 3.0 Btu/hr/sq ft/in/°F. The value at 2250 F is 8.8 Btu/hr/sq ft/in/°F. The values for the 50–70 sand at 750 F and 2250 F are 2.6 and 5.2 Btu/hr/sq ft/in/°F, respectively. The values for the No. 7 sand at 750 F and 2250 F are 2.1 and 3.2 Btu/hr/sq ft/in/°F, respectively.

Specimens

Four bags of sand, designated as No. 7 sand, were received from the American Graded Sand Company. This material shall be referred to as No. 7 sand.

Four bags each of sands designated as 50–70 and 20–30 were received from the Ottawa Silica Company. These sands shall be referred to as 50–70 and 20–30 sands, respectively.

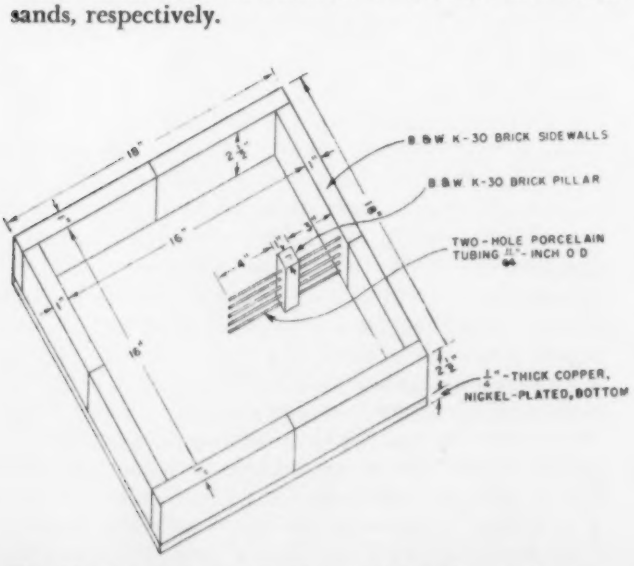


Fig. 19—General construction and location of thermocouples in container used for holding sand during thermal conductivity measurements.

Preliminary tests on each of the sands showed their "as received" moisture content to be at most only a few tenths of one per cent. The screen analysis of each of the sands, dried to constant weight, is given in Table 8.

The container shown in Fig. 19 was constructed to retain the sand for the thermal-conductivity tests. As indicated in the figure, the bottom of the container was an 18 in. square copper plate, ¼ in. thick. The copper was nickel plated to prevent oxidation. The 2½ in. high sidewalls were 1 in. thick pieces of Babcock and Wilcox K-30 insulating brick.

TABLE 8.—SIEVE ANALYSIS OF DRIED SANDS

Sand 20–30 U. S. Std. Sieve Nos.	Per Cent	Sand 50–70 U. S. Std. Sieve Nos.	Per Cent	Sand No. 7 U. S. Std. Sieve Nos.	Per Cent
−16 +20	None	−40 +50	None	−70 +80	5
−20 +25	78	−50 +60	72	−80 +100	76
−25 +30	22	−60 +70	27	−100 +120	17
−30	None	−70 +80	1	−120 +140	2
		−80	None	−140	None

Three inches in from one of the sidewalls, a 1 in. square pillar of the same insulating brick was used to stabilize the alignment of the 2-hole porcelain tubes containing the thermocouples. The 2-hole tubing extended through the one sidewall and the pillar to locate the thermocouples in a vertical plane and their hot junctions at the center of the specimen. The top and bottom thermocouples were approximately ¼ in. from the top and bottom sand surfaces, respectively. The three intermediate thermocouples were spaced at approximately ½ in. intervals.

The sidewalls and pillar were cemented to the copper plate. Two ¼ in. diameter porcelain tubes, approximately ¾ in. long, were used as dowel pins in each piece to assist in attaching it to the copper plate. The sidewall and pillar surfaces in contact with sand were coated with Sairset cement.

Jolt Test Specimens

In preparing each specimen, the sand was first dried at 400 F for at least 8 hr. The container of Fig. 19 was placed on a piece of ¼ in. plywood and the container filled with the dried sand. A ¾ in. thick piece of plywood was placed on top of the container. Clamps applied at the four corners of the unit held the filled

Fig. 20—Apparatus for measuring thermal conductivity of sand. In operation, the upper side of the specimen is heated by the electric furnace (shown suspended), the lower side is maintained at a constant temperature by a water-flow calorimeter.

container between the ¼ and ¾ in. thick pieces of plywood.

The assembly was placed on the jolting machine and given 10 jolts. In general, the thickness of sand decreased about ¼ in. as a result of this jolting. Sand was added to fill the container and, after covering and clamping as before, it was jolted 10 more times. In general, the decrease in thickness which resulted was about 1/16 in. Sand was again added to fill the container and the procedure repeated. In general, very little or no addition of sand was required after a total of 30 jolts had been given the specimen. The specimen was given about 10 additional jolts, making a total of approximately 40 jolts. The unit weights of the prepared specimens are given in Table 9.

Table 9.—Unit Weights of Thermal-Conductivity Specimens Prepared with Dry Sands

Sand	Pounds/Cubic Foot
20–30	108
50–70	102
No. 7	91

Method and Apparatus

The method, in brief, consisted of maintaining a constant elevated temperature on one side of the specimen, observing the temperature gradient through the specimen, and determining the rate of heat flow per unit area by means of a water calorimeter maintained at a constant temperature on the cold side of the specimen.

Figure 20 is a photograph showing a general view of the appartus used for measuring the thermal conductivity of sand. Suspended from the ceiling is a Globar furnace containing six heating elements. The furnace has an 18 by 18-in. heating area. The furnace is lowered onto the specimen which is in place for testing, and is used to maintain the top surface of the specimen at a constant elevated temperature.

Temperatures are measured by means of thermocouples. The temperature gradients are obtained at the desired temperatures as tangents to a curve, representing observed temperatures at five points in the specimen, and the distance between points.

The rate of heat flow is measured by means of a water-flow calorimeter. The measuring calorimeter is 6¼ in. in diameter and is surrounded by a 1⅜ in. wide annular guard calorimeter which, in turn, is surrounded by another cooling plate at the same temperature, so that an isothermal surface on the cold side of the specimen is provided over the entire area covered by the Globar furnace.

The specimen was elevated slightly from the calorimeter plates by the use of ½ in. wide by 0.020 in. thick strips of asbestos paper. These strips were placed to form a square at the outer edge of the guard calorimeter and at the outer edge of the specimen.

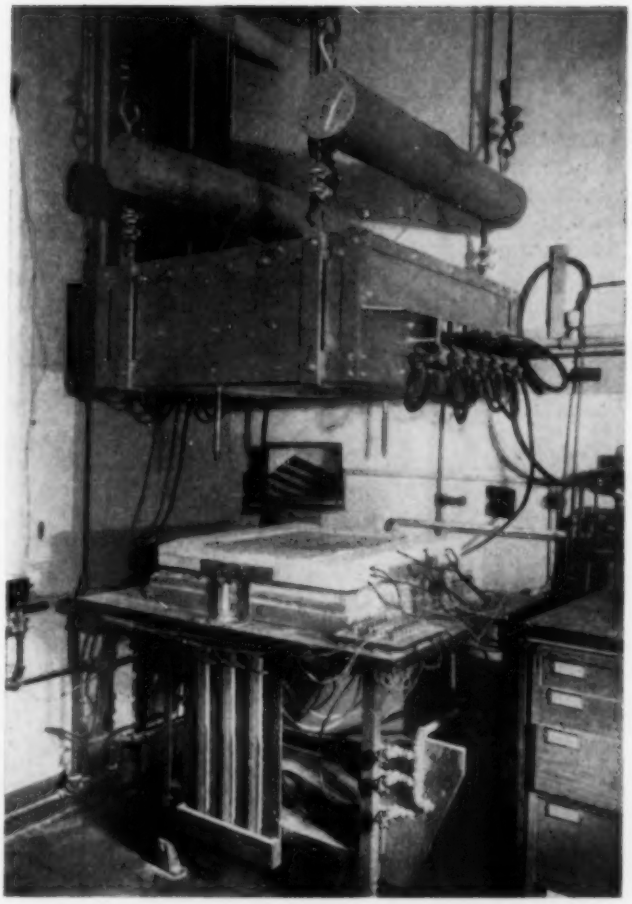


Table 10.—Observed Temperatures at Various Positions for 20–30 Sand

Position of Thermocouple From Bottom, In.	Temperature, °F., Equilibrium State 1	2	3	4
20/64	660	880	931	1107
51/64	971	1282	1368	1589
81/64	1226	1572	1675	1917
96/64	1361	1731	1843	2097
142/64	1617	2017	2147	2425

Table 11.—Observed Temperatures at Various Positions for 50–70 Sand

Position of Thermocouple From Bottom, In.	Temperature, °F., Equilibrium State 1	2
20/64	726	926
44/64	1026	1308
81/64	1451	1811
112/64	1741	2129
139/64	1981	2391

Table 12.—Observed Temperatures at Various Positions for No. 7 Sand

Position of Thermocouple From Bottom, In.	Temperature, °F., Equilibrium State 1	2	3
19.5/64	476	637	804
52.5/64	768	1060	1319
81.5/64	1015	1399	1705
115/64	1265	1711	2039
147/64	1519	2022	2415*

* This temperature at height of 150/64 in.

Results

Observed temperatures at each thermocouple position for each equilibrium condition are shown in Tables 10, 11 and 12.

The thermal-conductivity values calculated in the temperature range of 750 F to 2250 F at 250 F intervals, using the tangents to temperature-thermocouple position curves for temperature gradients at the selected temperatures, are given in Table 13 for 20–30 sand, Table 14 for 50–70 sand, and Table 15 for No. 7 sand. The data of these tables are shown plotted in Figs. 21, 22, 23 and 24.

TABLE 13.—THERMAL CONDUCTIVITIES OF 20–30 SAND AT VARIOUS TEMPERATURES

Temperature, °F	Thermal Conductivity* Equilibrium State 1	2	3	4	Average
750	3.00				3.0
1000	3.34	3.63	3.78		3.6
1250	3.97	4.09	4.14	4.48	4.2
1500	5.32	4.75	4.80	5.05	5.0
1750		5.98	5.51	5.84	5.8
2000			6.88	7.60	7.2
2250				8.81	8.8

* B.t.u. hr.$^{-1}$ft.$^{-2}$in.°F.$^{-1}$

TABLE 14.—THERMAL CONDUCTIVITIES OF 50–70 SAND AT VARIOUS TEMPERATURES

Temperature, °F.	Thermal Conductivity,* Equilibrium State 1	2	Average
750	2.55		2.6
1000	2.68	3.05	2.9
1250	2.79	3.21	3.0
1500	2.99	3.40	3.2
1750	3.46	3.64	3.6
2000	4.36	4.39	4.4
2250		5.20	5.2

* B.t.u. hr.$^{-1}$ft.$^{-2}$in.°F.$^{-1}$

TABLE 15.—THERMAL CONDUCTIVITIES OF NO. 7 SAND AT VARIOUS TEMPERATURES

Temperature, °F.	Thermal Conductivity,* Equilibrium State 1	2	3	Average
500	1.97			2.0
750	2.03	2.24	1.97	2.1
1000	2.14	2.30	2.06	2.2
1250	2.32	2.39	2.14	2.3
1500	2.43	2.54	2.32	2.4
1750		2.85	2.73	2.8
2000			3.07	3.1
2250			3.24	3.2

* B.t.u. hr.$^{-1}$ft.$^{-2}$in.°F.$^{-1}$

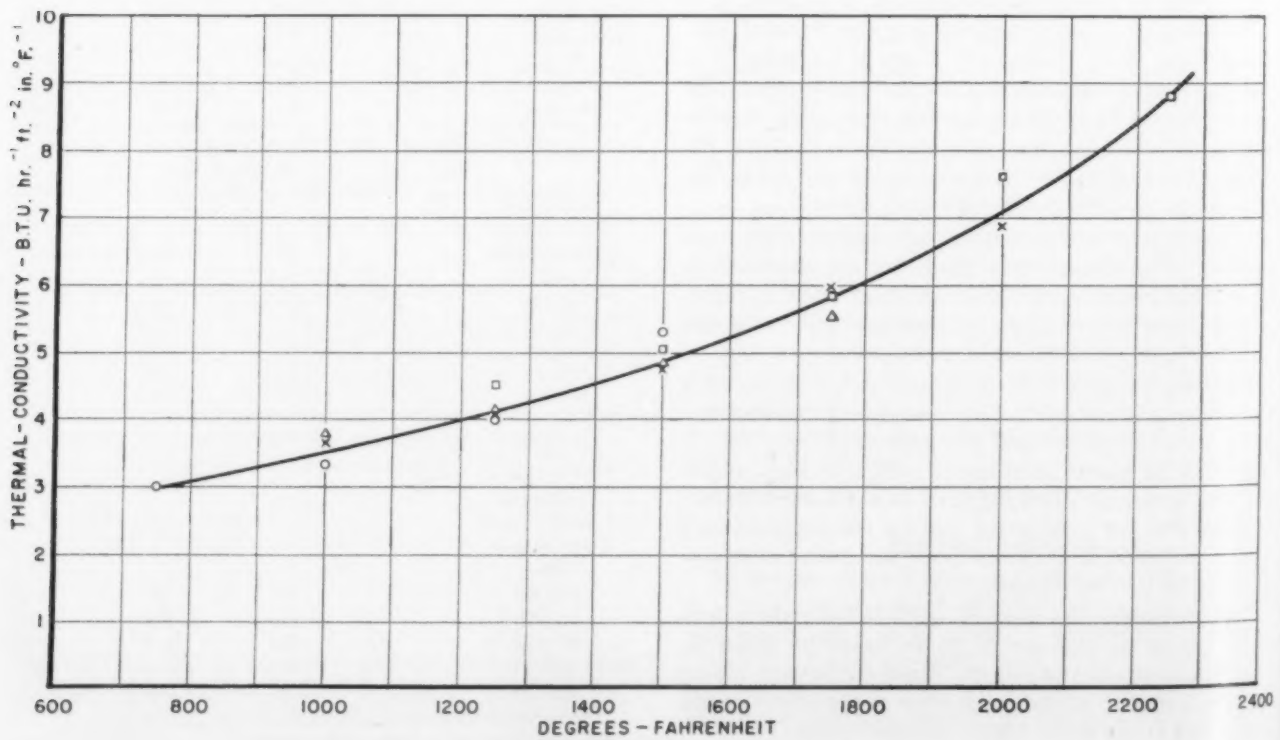
Graphical presentation of data from Tables 13, 14, 15. *on next page*

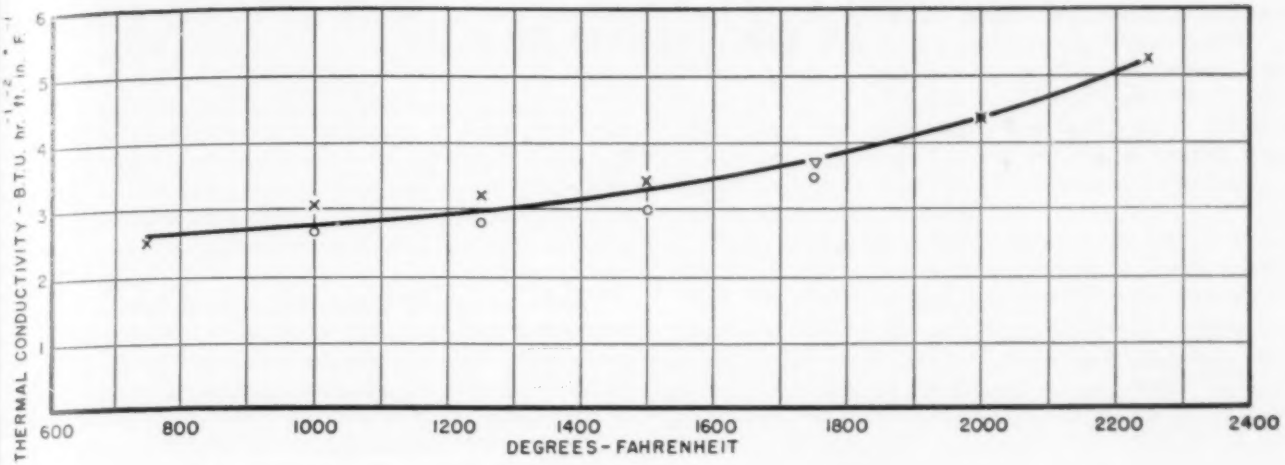
Fig. 21—Thermal conductivity of 20-30 sand.

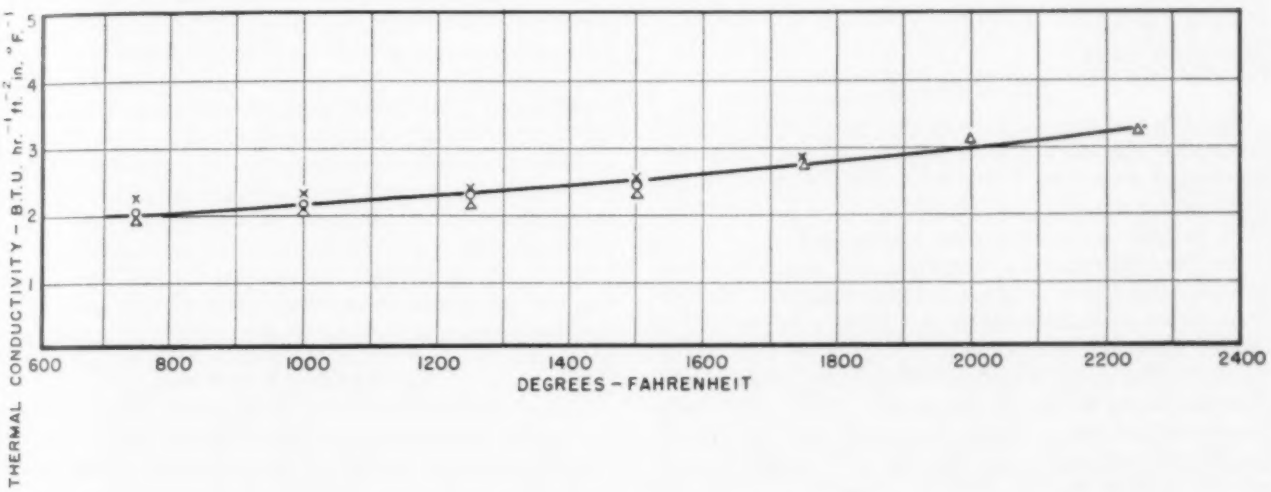
Fig. 22 (upper)—Thermal conductivity of 50-70 sand.

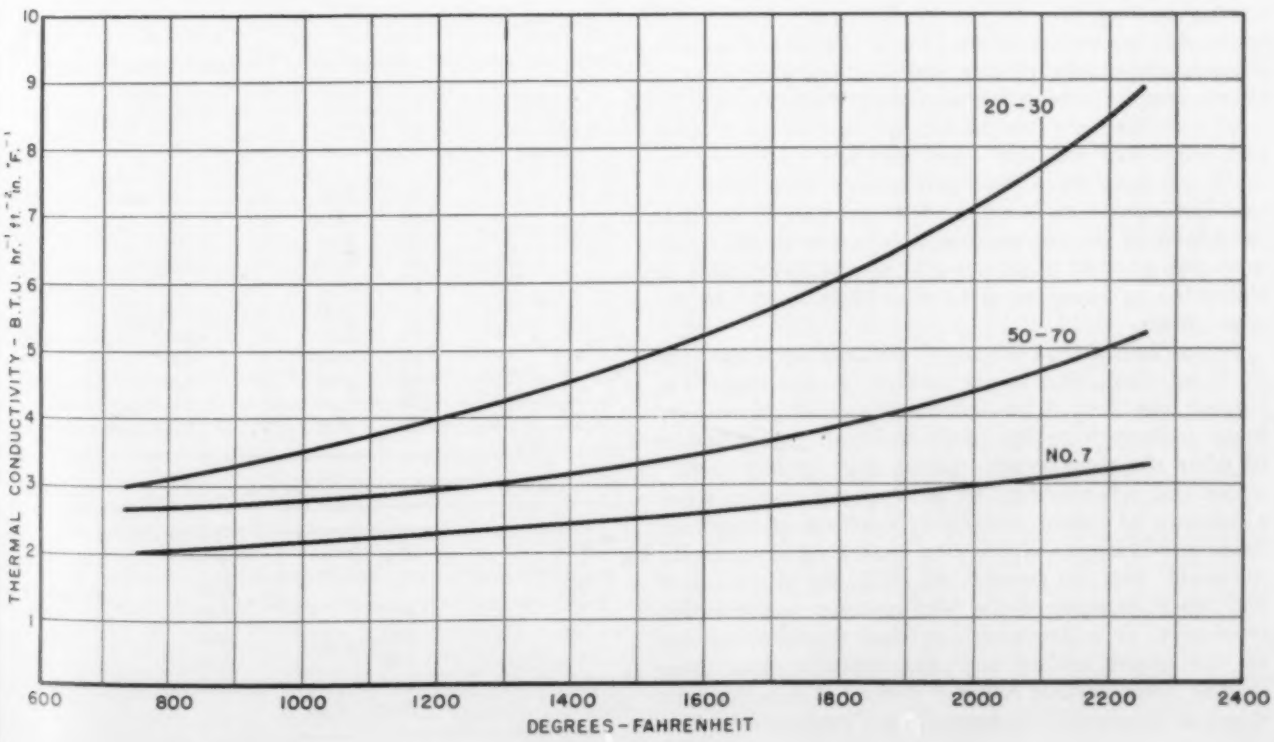
Fig. 23 (middle)—Thermal conductivity of No. 7 sand.

Fig. 24 (lower)—Average curves for comparison of thermal conductivity of the three sands tested. *below*




THERMAL CONDUCTIVITY - B.T.U. hr.$^{-1}$ ft.$^{-2}$ in. °F.$^{-1}$
1
2
3
4
5
6
600
800
1000
1200
1400
1600
1800
2000
2200
2400
DEGREES - FAHRENHEIT


THERMAL CONDUCTIVITY - B.T.U. hr.$^{-1}$ ft.$^{-2}$ in. °F.$^{-1}$
1
2
3
4
5
600
800
1000
1200
1400
1600
1800
2000
2200
2400
DEGREES - FAHRENHEIT


THERMAL CONDUCTIVITY - B.T.U. hr.$^{-1}$ ft.$^{-2}$ in. °F.$^{-1}$
1
2
3
4
5
6
7
8
9
10
20 - 30
50 - 70
NO. 7
600
800
1000
1200
1400
1600
1800
2000
2200
2400
DEGREES - FAHRENHEIT

FREEZING RATE OF WHITE CAST IRON IN DRY SAND MOLDS

By
Dr. H. A. Schwartz

Synopsis

White cast iron solidifying in a dry sand mold freezes somewhat rapidly for the first three minutes or so. The rate of deposition of solid metal then slows up until about five minutes after pouring and then suddenly accelerates, the dendrites meeting in a little over seven minutes. However, over 45 minutes are required for the interdendritic material at the center to freeze completely. A curve is developed giving the fraction of the median plane which has solidified in various times.

Uniform Procedure

For the purpose of comparing the freezing rate of white cast iron with that of steel, and for the ultimate analysis of these data in the heat analyzer at Columbia University, the freezing rate of a 4 in. x 8 in. x 8 in. slab, poured in dry sand, was investigated.

In the interest of uniformity the procedure was exactly that described in "Methods Employed to Obtain Rates of Solidification" by Ken L. Clark of the Naval Research Laboratories, as part of the report of the A.F.A. Heat Transfer Committee, published in *Transactions*, AFA, vol. 53, p. 88 (1945). The only exception was that a header 4 in. high and 4 in. x 8 in. in section was added to the top of the slab instead of the smaller header used at Anacostia. This was done to facilitate molding and pouring. Temperatures were measured by an optical pyrometer on the stream flowing into the mold. It would have been preferable to use a thermocouple immersed in the stream, but no means was available for protecting a thermocouple without introducing prohibitive lag.

The molds were poured and at various times after the metal had half-way filled the test specimen, the mold was inverted. The castings were then annealed and sectioned 4 in. from the bottom, that is, through the center of the test specimen. The size of test specimen was selected upon the advice of Dr. Paschkis of Columbia as being so great that there would be no edge effects.

It was found that when the castings were inverted to bleed them, the upper surface (as the mold was tipped) was likely to be moderately smooth, but on the lower surface there was likely to be an accumulation of what the foundryman would call "mushy metal" which did not run out. It is supposed that this was a mixture of liquid metal and portions of dendritic stems and branches which were broken off in inverting the mold. On this account the thickness of the upper and lower surfaces of the bled castings was recorded separately. It is the writer's opinion that the readings on the upper surface are more reliable than those on the lower surface but are somewhat less than the depth at which the first frozen metal was found.

Data as to the composition, temperature, time of freezing and resulting wall thickness will be found in Table 16. The results scatter quite badly.

In Table 17 the results for a given time of freezing have been averaged and the curve is, therefore, probably somewhat smoothed out.

One block, No. 13 in the series, having, according to the plant analysis, a carbon content of 2.55 per cent, manganese content of 0.48 per cent and silicon content of 0.87 per cent, was allowed to freeze solid around a platinum-rhodium thermocouple protected by a quartz tube. The couple and its indicator were calibrated just before use, by comparison with the Bureau of Standards calibrated couple.

The readings were made by taking the stopwatch times after the metal reached the level of the thermocouple for 10 F intervals on the temperature scale. The temperatures read were actually multiples of 10 F. The fact that the numbers all end in 7 is due to the correction of an error to match the calibration of the couple.

Cooling Curve Determined

The cooling curve at the geometric center of the 4 in. x 8 in. x 8 in. specimen is shown in Fig. 25. The solidus is well marked at about 2087 F. This corresponds to 1142 C, and by interpolation from the accepted equilibrium diagrams a temperature of 1135 C was expected. The liquidus cannot be detected with any certainty. The only discontinuity in temperature

TABLE 16.—COMPOSITION, TEMPERATURE, TIME OF FREEZING AND WALL THICKNESS

Serial No.	C, Per Cent	Si, Per Cent	Temp., °F	Time, Min.	Thickness Top	Thickness Bottom
3	2.49	1.01	2490	2½	Collapsed	0.40"
8	2.58	0.90	2540	3	0.43"	0.44"
12	2.40	0.93	2500	3	0.31"	0.38"
5	2.56	0.96	2560	3½	0.52"	0.65"
6	2.50	0.98	2560	3½	0.77"	0.91"
7	2.55	1.09	2520	3½	0.70"	0.81"
9	2.50	1.05	2530	4	0.66"	0.75"
1	2.52	1.00	2480	5	0.49"	0.88"
2	2.50	0.99	2520	5	0.81% did not drain.	
4	2.51	0.89	2530	7	Practically solid.	
10	2.46	0.91	2530	7	Solid (Sides collapsed)	

TABLE 17.—DATA ON RESULTING WALL THICKNESS

Time, Min.	Thickness, in. Top	Thickness, in. Bottom	Average
2½	?	0.40	0.40–
3	0.37	0.41	0.39
3½	0.66	0.79	0.73
4	0.66	0.75	0.70
5	0.65	0.88	0.77+
7	Nearly 2 in.		

readings to be found near the right temperature has been marked in Fig. 25 in the neighborhood of 2350 F.

Whether this is the liquidus or not, it is somewhere near the right temperature, and was reached at the center of the specimen in just over 10 min. The center remained partially frozen until the eutectoid began to freeze in about 35 min. and the freezing was not complete until after 42 min. During this time interval the center was partially solid and partially liquid.

Dendrite Formation

This would seem to account for the long branching growth of dendrites into the solution. Evidently these dendrites will be longer in a region where cooling is somewhat slower, and therefore, they become more and more pronounced as their outer ends penetrate more and more deeply into the castings. Thus the specimens bled after 2½ or 3 min. had fairly smooth surfaces, whereas at 7 min., while the metal obviously still contained the liquid phase, the dendrites had come so near interlacing that practically nothing would bleed out.

From the preceding it is possible to approximate the liquidus and solidus temperature of the alloy normally containing 2½ per cent C. From the data in "Alloys of Iron and Silicon" [1] the eutectoid composition and the solidus for non eutectiferous alloys can be approximated. These are plotted in Fig. 26, forming part of the iron-carbon diagram in the presence of 1 per cent Si. As in all ternary diagrams, the use of such a pseudo-binary diagram is open to the objection that it does not recognize the fact that the third element, here Si, is not present in equal concentration in the solid and liquid.

The literature does not contain data for determining the conjugate compositions of liquid and solid for an alloy of 2½ per cent C and 1 per cent Si for all temperatures.

[1] Earl S. Greiner, J. S. Marsh and Bradley Stoughton, "The Alloys of Iron and Silicon," (1933). Published for The Engineering Foundation by the Mc-Graw-Hill Book Co., Inc., New York City.

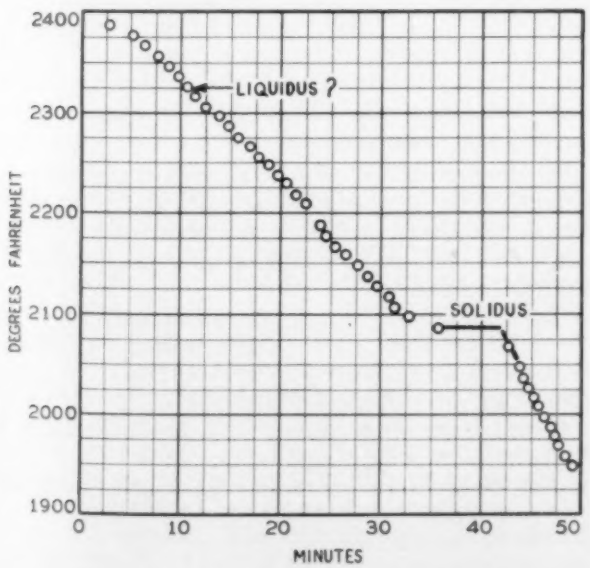


Fig. 25—Cooling curve taken at geometric center of 4 in. x 8 in. x 8 in. white cast iron specimen.

In Fig. 26, lines are drawn representing the temperatures reached at given times (by 5 min. intervals). These give, subject to the error just discussed, the composition of solid and liquid as a function of time. Knowing the two concentrations, their relative amounts, to average 2½ per cent C, is readily computed and plotted in Fig. 27. A constant freezing rate of the eutectoid is assumed.

The segregation of silicon toward the liquid would shift the carbon concentration toward lower values. The fact that diffusion may not be rapid enough to avoid coring of the solid dendritic branches, i.e., higher carbon at their surfaces than at their axes, makes the average carbon lower than expected from the diagram. The two errors somewhat compensate one another so that Fig. 27 is perhaps a closer approximation of the facts than would be expected at first glance.

The writer is indebted to Messrs. Zirzow and Oliphant of Cleveland Works for preparing and bleeding the molds, and to Messrs. Bock and Young of the Research Laboratory for the temperature readings during freezing.

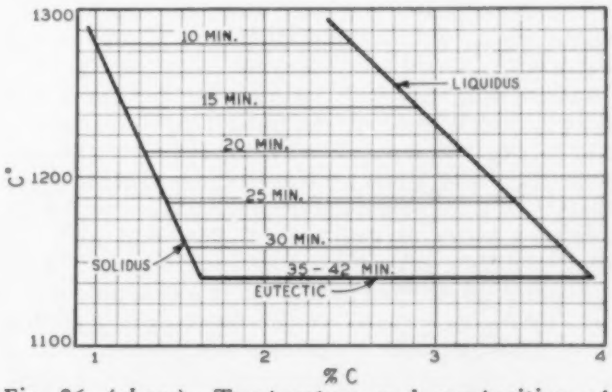
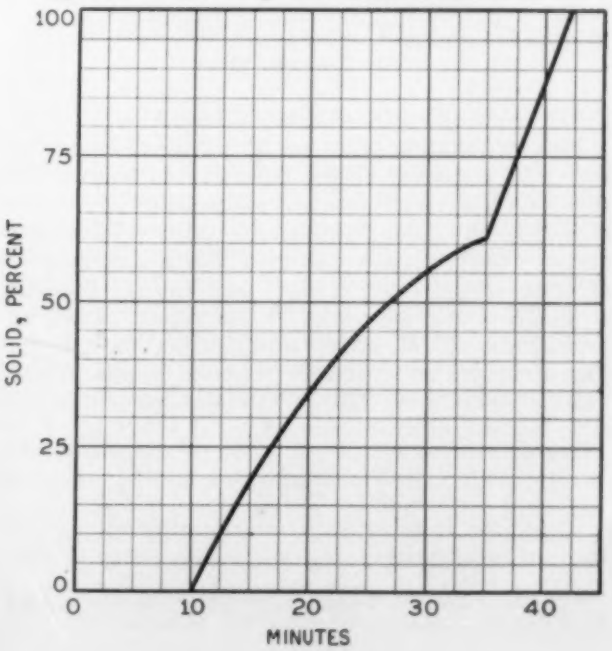


Fig. 26 (above)—Temperature and composition of liquid and solid phases at 5 min. intervals.

Fig. 27 (below)—Relation between proportion of solid phase and freezing time of white cast iron.



SOLIDIFICATION RATES OF ALUMINUM IN DRY SAND MOLDS

By
H. Y. Hunsicker *

Synopsis

The Heat Transfer Committee of the American Foundrymen's Association is attempting to correlate data obtained on metal solidification rates by experimental methods in the foundry with similar data which may be determined by a less expensive electrical method. The Cleveland Research Division was asked to obtain data on the solidification of aluminum by the direct experimental method.

Unsuccessful attempts were made to determine the solidification rate of dilute aluminum alloys containing copper, silicon or nickel by the "pour out" or "slush casting" method. Work with the alloys was unsuccessful because of the small temperature gradients in the sand castings and the very low mechanical strength of the alloys at temperatures slightly lower than the liquidus temperature which caused the partly solidified layers to pour out of the mold with the residual liquid metal.

Castings in which a solid shell of measurable thickness could be retained in the mold were produced using 99.8 per cent aluminum cast in the form of 2 in. x 12 in. x 12 in. plates in 10 in. x 15 in. x 16 in. baked sand molds. From several castings poured out at various time intervals from the instant of casting, the average thickness of the solidified layer was found to approximate:

$$D = 0.0367\ \sqrt{t} - 0.11$$

where D = thickness of solidified layer, in.
t = time interval, sec.

Theory and Practice Agree

This relation appears to be in good agreement with that predicted on the basis of heat transfer calculations assuming a constant temperature at the casting-mold interface throughout the solidification period. Extrapolation indicated that a time interval of approximately 9 sec after pouring was required to dissipate the superheat of the liquid metal, approximately 80 F, before solidification started, and that complete solidification required approximately 915 sec. The linear rate of solidification within this time interval may be expressed as:

$$\frac{dD}{dt} = 0.01835t^{-\frac{1}{2}}$$

Thus, the instantaneous solidification rate at the start of solidification is indicated to be 6.12×10^{-3} in./sec diminishing to about 6.07×10^{-4} in./sec near the end of the solidification period.

The grain size of the sand castings increased toward the center of the section, probably as a result of the retardation of solidification rate and preferential crystal growth, and the density decreased toward the center probably because of increasing obstruction to feeding.

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Procedure and Results

The question of the aluminum alloy composition to employ in the tests was given careful consideration. Several features were desired[1] which restricted to a certain extent the type of alloy which might be used. A solidification temperature range with a solid solution type time-temperature curve rather than a sharp freezing point was desired, and a range of approximately 100 F was preferred. In addition, it was desirable that the thermal properties, latent heat of fusion and thermal conductivity, be as accurately known as possible. For the latter requirement it was believed that as dilute an alloy as possible meeting the solidification temperature range would be most nearly satisfactory, the thermal properties approaching those of pure aluminum.

The plate casting size initially suggested was 4 in. x 16 in. x 16 in., and it was desired to provide some extension of the vertical dimension to allow feeding to compensate for solidification shrinkage. Since a casting of this size with the desired mold wall thickness of 8 in. required a large and heavy mold, it was desired to determine whether the casting and mold could be scaled down to about half the suggested size.

Preliminary Experiments

For the preliminary work to determine the approximate time for solidification of 4 in. and 2 in. thick plates, core boxes were built and fitted with patterns to produce cores and casting cavities of the dimensions outlined below:

Mold Cavity Dimensions	Core External Dimensions	Core Wall Thickness
4x16x22 in (6 in. feeder)	20x30x32 in.	8 in.
2x8x11 in. (3 in. feeder)	10x15x16 in.	4 in.

Cores of these dimensions were produced using a sand mix of the following composition:

1000 lb washed silica sand, A.F.A. No. 60 grain fineness
10 lb corn flour
2 qt. core oil
3 qt. kerosene
18 qt. water

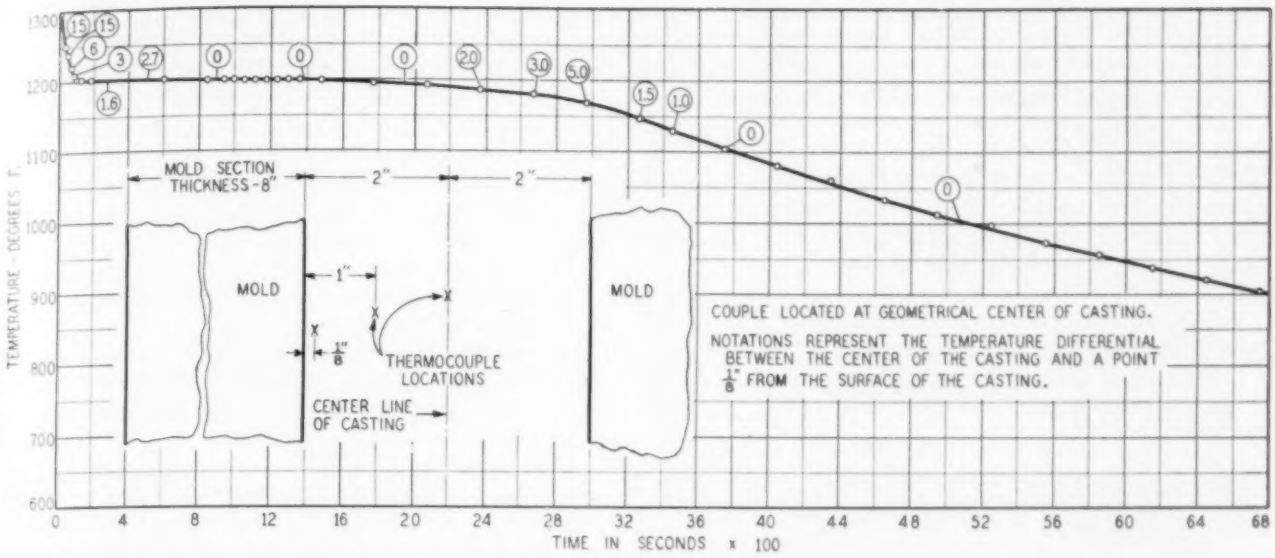
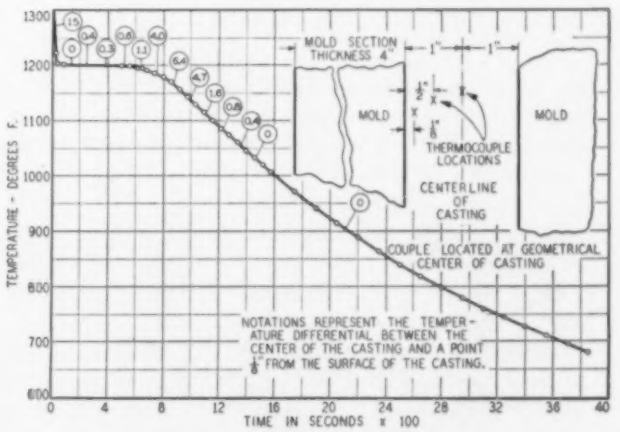


Fig. 28 (above)—Cooling curve of aluminum alloy showing temperature differentials between center and outer skin of 4 in. x 16 in. x 22 in. specimen.

Fig. 29 (below)—Cooling curve of small size aluminum alloy test specimen 2 in. x 8 in. x 11 in.



The 20 in. x 30 in. x 32 in. core was baked 8 hr. at 440 F and the smaller core 4 hr. at the same temperature. Thermocouples were located in the cavities of the two molds at the geometric centers of the lower square portion of the large faces and at 1/8 in. from the casting-mold interface, half the distance from the surface to the center of the casting cavity and at the center of the casting section.

The selection of an alloy for the initial experiments was based upon consideration of the desired characteristics outlined above. Since the freezing rate of a solid solution alloy with limited solid solubility may determine the amount of solute retained in solid solution, the choice of alloying element concentration had to be estimated from the anticipated solidification rate. It was estimated that about 2.5 per cent copper might be retained in solid solution at the anticipated rate of solidification of the 4 in. thick casting. The solidification range of a binary aluminum–2.5 per cent Cu alloy at the anticipated solidification rate should approximate 200 F, and half this amount of solute should produce a solidification range of about 100 F.

Alloy Selection Complicated

There were uncertainties in this calculation, however, and a binary aluminum (99.8 per cent)–1.75 per cent copper alloy was selected for the first castings. An equilibrium cooling curve of this alloy (C2794) indicated the liquidus temperature to be 1209 F and a slight jog in the curve of 1089 F might be interpreted as the conclusion of solidification or solidus temperature.

A 4 in. x 16 in. x 22 in. casting (weight 140 lb) was cast at 1300 F and allowed to solidify completely with 22 gage chromel-alumel couples located at 1/8 in., 1 in. and 2 in. from the casting mold interface with a 1 in. circle at the geometrical center of the lower 16 in. x 16 in. portion of the casting. The thermocouple potentials were measured by means of a Speedomax multiple point recorder. The cooling curve so obtained has been replotted in Fig. 28 with notations concerning the temperature differentials between the center and 1/8 in. locations.

The time required for completion of solidification is not clearly delineated by this curve, although it is estimated that at least 3000 sec (50 min) were required. The eutectic (some Al-$CuAl_2$ was apparent in the microstructure of this casting) probably remained liquid until a temperature of 1018 F was attained (4860 sec or 81 min). It is evident that a high percentage of the primary Al-Cu solid solution solidified at a relatively constant temperature close to 1200 F. Measurable thermal gradients (from surface to center) developed prior to the solidification arrest and near the final portion of the freezing period.

An analogous set of cooling curves was also obtained on a 2 in. x 8 in. x 11 in. plate with couples located in the casting at 1/8 in., 1/2 in. and 1 in. from the casting-mold interface. Thermocouples were also located in the baked sand mold at lateral distances of 1/4 in., 1/2 in. and 1 in. from the casting-mold interface. The cooling curve of the casting (Fig. 29) is

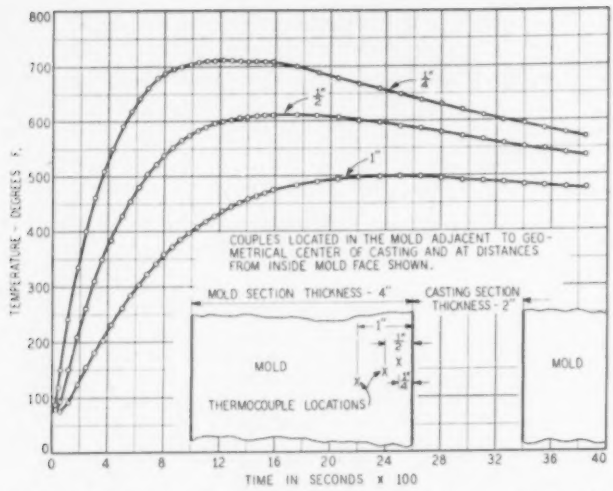


Fig. 30—Temperatures developed within sand mold in which 2 in. x 8 in. x 11 in. specimen was cast.

quite similar to that obtained with the larger casting and indicated that at least 600 sec were required for solidification. Some eutectic may have been present in the liquid state to a temperature of 1018 F which was attained after 1520 sec. The temperatures developed within the sand mold are plotted in Fig. 30.

Pour-Out Tests

On the basis of the results obtained in the preliminary experiments it was decided to employ a 2 in. thick casting for the "pour-out" tests. In order to minimize end effects, it was desired to increase the size of the casting to 2 in. x 12 in. x 12 in. The 10 in. x 15 in. x 16 in. core box was fitted with a 2 in. x 12 in. x 12 in. pattern, and attempts were made to obtain satisfactory molds using the sand mix and drying procedure employed in producing molds for the steel castings, i.e.:

A.F.A. No. 70 washed silica sand
3 per cent Western bentonite
0.5 per cent corn flour
1.5 per cent foundry dextrin

The sand was tempered with 4 per cent water. The molds were prepared by jolting and hand ramming followed by drying 20 hr. at 400 F. The molds prepared in this manner were quite weak and cracked badly after baking.

First Experiments Unsatisfactory

Two of these cracked molds were pieced together for pour-out tests. The first casting was poured at 1300 F, using the binary aluminum-1.75 per cent copper alloy and inverted 60 sec from the time that the pouring was half completed (mold half filled). All the metal with the exception of a thin skin at the bottom of the cavity ran out. If a thin shell had solidified at the other surfaces of the cavity, it was too plastic to remain in the mold. The second casting was inverted 300 sec after pouring and only a small amount of liquid metal drained from the cavity. A vertical section through the center of this casting is shown in the center of Fig. 31.

Since a great amount of difficulty was anticipated with cracking of molds prepared from the sand mix employed for the steel castings, it was decided to alter the binder and baking time to increase the strength of the mold. The following sand mix and baking time were employed for all subsequent molds, which were prepared by jolting and hand ramming:

1000 lb A.F.A. No. 70 washed silica sand
10 lb corn flour
3 qt core oil
18 qt water
Bake molds 4½ hr at 440 F

It is believed that the thermal properties of the molds prepared from this mix should not differ significantly from those of molds made from the sand used for the steel castings.

The next casting (Al-1.75 per cent Cu alloy) was made in a mold prepared from the modified sand mix and was inverted after 170 sec. The solid (or partially solidified) shell fell out of the mold with the remaining liquid.

Castings Not Inverted

The next casting was drained after 180 sec. This mold was tilted about 100° rather than inverting (180°) in an effort to prevent the solid shell from falling from the mold. In this and the previous castings the open top of the mold had been covered with an asbestos pad ¼ in. thick initially at room temperature. This cover was intended to prevent solidification at the opening of the mold cavity to allow free draining of the residual liquid. However, in this and previous molds some solid metal bridged over the top of the casting, and when the mold was tilted, the small amount of liquid which drained drew the side walls of the casting together because air could not get in to displace the liquid metal.

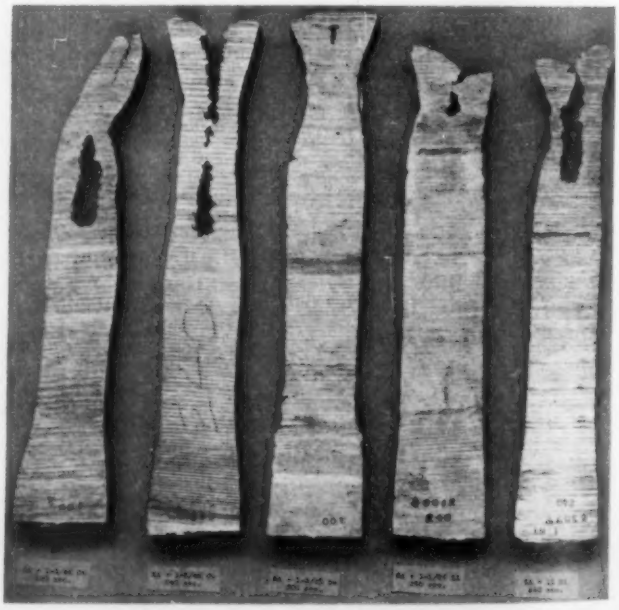


Fig. 31—Cross sections through the center of 2 in. x 12 in. x 12 in. plates cast in baked sand molds and poured out after times indicated. Aluminum-copper, aluminum-silicon and aluminum nickel alloys.

It was then decided to increase the casting section at the open end of the cavity by providing a flare 1½ in. deep by 1 in. wide around the entire opening and, in addition, to cover the open end of the mold with a ½ in. thick transite plate preheated to 1500 F.

Achieve Free Bleeding

Three additional castings were made using the binary aluminum-copper alloy and solidification intervals of 240, 180 and 120 sec. These three molds were tilted about 100°. The flared mold cavity and preheated transite cover successfully kept the open end of the mold from solidifying and allowed free drainage. The casting drained after 240 sec (section to left of center in Fig. 31) had apparently solidified completely through the 2 in. section at the center. This was also the case in the casting tilted after 180 sec (section to extreme left in Fig. 31). The open-end portion of the latter casting collapsed in the mold during drainage. The solidified portions of the casting tilted after 120 sec collapsed in the mold so that no section could be retained for measurement of the shell thickness.

It appeared from the results of the trials just described that in the case of the aluminum-copper alloy sufficient liquid was entrapped in the partially solidified shell at temperatures slightly below the liquidus that the shell was too weak to remain intact. It was considered at this time that a binary aluminum-1.5 per cent silicon alloy with an equilibrium solidification range of about 135 F (1210–1075 F) might have sufficiently greater strength at temperatures slightly below the liquidus that the shell would remain in the mold.

Binary Alloys Unsuccessful

Two plates were cast using a 99.8 per cent Al + 1.5 per cent Si alloy and pour-out times of 120 and 240 sec. The casting tilted after 120 sec collapsed, while the one tilted after 240 sec appeared to have almost completely solidified (section to right of center in Fig. 31). The prospects of attaining the desired information using the binary Al-Si alloy did not appear hopeful, so the next trials were made using a high purity Al-1 per cent Ni alloy (approximate equilibrium solidification range 1210–1184 F). Again castings were tilted after 120 and 240 sec, and the behavior closely paralleled that observed with the silicon-containing alloy. A section of the 240-sec casting is shown to the extreme right in Fig. 31.

Pure Aluminum Castings

The results of the trials using dilute alloys containing Cu, Si or Ni indicated that these materials, in which the partially solidified shell may contain small amounts of liquid eutectic at temperatures within the solidification temperature range, were too hot-short to provide data from which to plot the progress of solidification by the slush casting method. It had been anticipated that this difficulty could be overcome by using relatively high purity aluminum, and two preliminary castings made using solidification intervals of 120 and 240 sec indicated that a solid shell of pure aluminum could be retained in the mold. It was then agreed that the work be continued using pure aluminum.

Castings of 99.8 per cent aluminum poured at 1300 F were drained after the following solidification intervals: 60, 90, 120, 180, 240, 300, 450, 600 and 720 sec. Representative cross sections of these castings with the exception of the 600 sec casting, which was made after the photograph was taken, are shown in Fig. 32. It is evident in this photograph that even the pure aluminum castings did not always retain an external shape exactly reproducing that of the mold cavity. The solid shell formed in relatively short solidification intervals did not readily remain in the mold, and at longer periods a tendency for collapsing and hot tearing were evident.

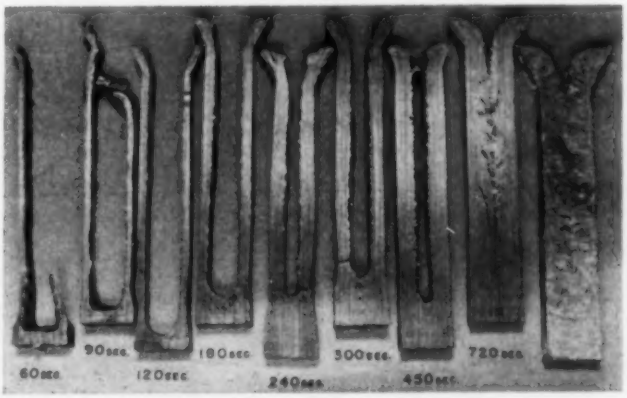


Fig. 32—Cross sections through the center of 2 in. x 12 in. x 12 in. plates of 99.8 per cent aluminum cast in baked sand molds and poured out after time interval indicated. Section at right macroetched.

The casting which was drained after 720 sec had progressed sufficiently near complete solidification that the shells from the opposite surfaces were merged to a considerable extent. Evidence that a fairly continuous channel existed at the center was offered by the fact that 0.76 lb of metal drained from the casting. The effect of solidification from the bottom end of the mold is evident in Fig. 32. The selection of a length of 12 in., however, appears to have eliminated any error from end effect at the casting center.

The internal surfaces of the castings were fairly rough, the roughness increasing with the solidification time, and it was believed that the most accurate measurement of average thickness of the solidified shell could be obtained by cutting sections at the center of the surface of the castings, determining the volume and the area in the plane perpendicular to the thickness and from this data calculating the average thickness. Consequently, sections approximately 2 in. square were cut from the center of the two casting surfaces, measured in the two directions parallel to the casting surface, weighed in water and in air to determine the volume, and the average thickness was calculated from these measurements. The data from such measurements are summarized in Table 18.

These data have been plotted in the form of a time-average thickness relation in Fig. 33. The various experimentally determined points form a continuous

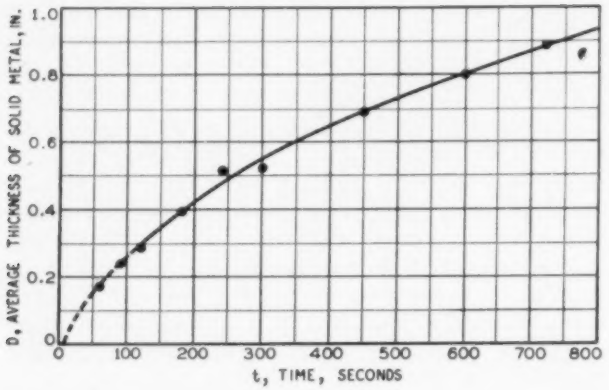


Fig. 33—Course of solidification of pure aluminum test plate determined by pour-out method.

From this equation it would appear that substantial completion of the solidification process in the 2 in. thick plates should require approximately 915 sec. The instantaneous rate of solidification can be derived from the above equation and is found to be:

$$\frac{dD}{dt} = 0.01835\ t^{-\frac{1}{2}}$$

This function has been plotted in Fig. 35. The maximum rate of solidification occurs as soon as the superheat has been dissipated (after approximately 9 sec) and amounts to 6.12×10^{-3} in./sec. The solidification rate diminishes rapidly at first then increasingly slower until at the instant before completion ($t = 915$ sec) it is approximately 6.07×10^{-4} in./sec.

An attempt was made to check experimentally the time required for complete solidification by temperature measurements. One 99.8 per cent aluminum casting was allowed to solidify completely with thermocouples located in the plate at distances of 1/8 in. and 1 in. from the casting-mold interface. The time-temperature curves so obtained are reproduced in Fig. 36. A measurable temperature gradient between

TABLE 18.—VOLUME AND LINEAR MEASUREMENTS, CALCULATED AVERAGE THICKNESS AND DENSITY OF SAMPLES CUT FROM CENTER OF PLATE CASTINGS DRAINED AFTER VARIOUS SOLIDIFICATION INTERVALS
(99.8 Per Cent Aluminum)

Sample No.	Time, sec.	Length, in.	Width, in.	Area, in.	Volume, cu. in.	Average Thickness, in.	t	Density Gm./cc. (25° C)
60	60	2.308	2.014	4.6483	0.8076	0.0174	7.75	2.6976
90-1	90	2.045	1.995	4.0798	0.9665	0.237	9.49	2.6977
90-2	90	2.043	2.030	4.1473	1.0025	0.242	9.49	2.6969
120-1	120	2.022	1.898	3.8377	1.0988	0.286	10.95	2.6904
120-2	120	2.020	1.884	3.8057	1.0625	0.279	10.95	2.6936
180-1	180	2.083	2.022	4.2118	1.6707	0.397	13.42	2.6916
180-2	180	2.081	2.008	4.1786	1.6580	0.397	13.42	2.6881
240-1	240	2.140	1.977	4.2308	2.1239	0.502	15.49	2.6934
240-2	240	2.117	1.973	4.1768	2.2271	0.533	15.49	2.6895
300-1	300	2.022	1.998	4.0400	2.1337	0.528	17.32	2.6785
300-2	300	2.028	2.019	4.0945	2.1034	0.514	17.32	2.6827
450-1	450	2.045	2.058	4.2086	2.8783	0.684	21.21	2.6468
450-2	450	2.109	2.043	4.3086	2.9693	0.689	21.21	2.6225
600-1	600	2.025	1.987	4.0237	3.2560	0.809	24.50	2.6502
600-2	600	2.028	1.977	4.0093	3.1439	0.784	24.50	2.6168
720	720	2.123	2.018	4.2842	3.7796	0.882	26.83	2.6527

curve which on examination appeared to be a parabola with its vertex at the origin. Consequently, the data were replotted, the average thickness of the solid shell as a function of the square root of time, in Fig. 34. The thickness-$\sqrt{t}$ relation is approximately linear, and indicates that the intercept on the zero time axis is less than zero thickness.

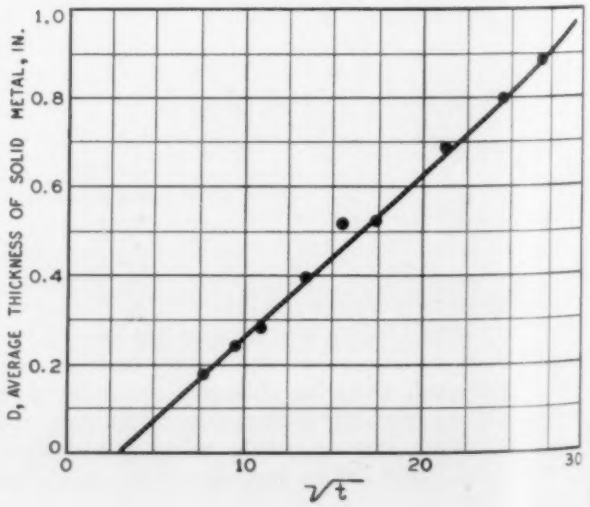
This is indicative of a slight time lag (approximately 9 sec) in the beginning of solidification which might be accounted for by the superheat of the liquid metal (approximately 80 F). The parabolic relation appears to be in agreement with that predicted on the basis of the theoretical consideration of heat transfer assuming a constant temperature at the casting-mold interface throughout the solidification period.[2]

Using Fig. 34 the approximate linear relation was fitted to the points by inspection, and the constants in the equation of the line were determined graphically. This relation may be expressed approximately as:

$$D = 0.0367\ \sqrt{t} - 0.11$$

where D = thickness of solid layer, in.
t = elapsed time from moment mold is half filled, sec.

Fig. 34—Relation between average thickness of shell and time after pouring pure aluminum plate.



the ⅛ in. and 1 in. locations developed only after about 1000 sec, and solidification did not appear to have been complete until 1125 sec had elapsed. This point does not fall on the curve formed by the pour-out data, and may be in error due to uncertainties in the interpretation of the cooling curve.

Correlation of Solidification Rate and Metallurgical Characteristics of the Castings

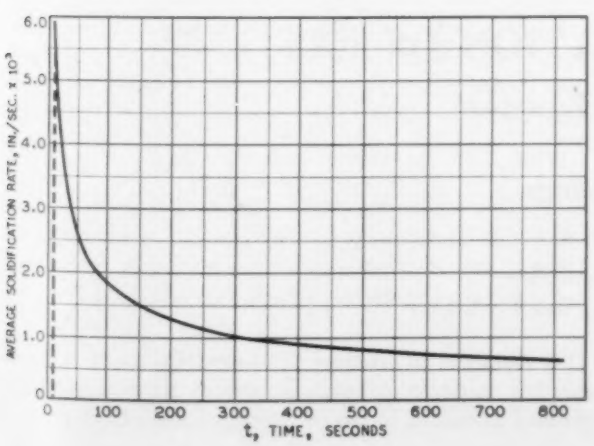
It was quite obvious on examination of the inner surfaces of the pour-out castings (liquid-solid interface) that a progressive change in surface pattern and roughness occurred throughout the course of solidification of the plates. The increase in dendrite size and irregularity of the liquid-solid interface is apparent in the photograph of the various sections, Fig. 32. This change is further illustrated in Fig. 37, a photograph approximately full size of the inner surface of castings poured out at various periods ranging from 90 to 450 sec.

A progressive change in color from the thin castings which were quite bright to the heavy castings which were considerably darker was noted, and it was believed that this might be attributable to normal segregation (migration of impurities toward the last portion to solidify). The variation in grain size throughout a completely solidified casting is shown in the section to the extreme right in Fig. 32. A shell of fine grains forms during the early stages of solidification, and as the rate of heat extraction is reduced, interference to growth between the various crystals apparently blocks off those which are not aligned in preferential directions resulting in large columnar grains.

Average Density Computed

Since the average thickness calculations involved a measurement of volume, data were available from which the average densities of the several casting sections could be computed. The average density data is tabulated in Table 18 and has been plotted in Fig. 38. It is evident that the average density of the solidified sections decreased markedly with increasing section thickness. It was considered desirable to derive from this data the densities at various points throughout the entire casting section. This derivation follows:

Fig. 35—Solidification rate of pure aluminum test specimen determined by the pour-out method.



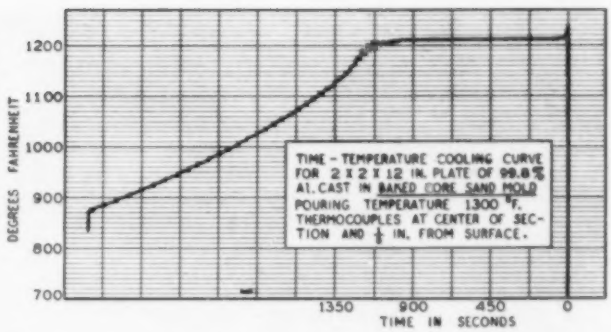


Fig. 36—Cooling curve for pure aluminum test specimen. Thermocouples at center and ⅛ in. from surface.

$$\overline{\rho_t} = \frac{\int_o^t \rho_t dt}{\int_o^t dt}$$

where $\overline{\rho_t}$ = average density of section of thickness t
ρ_t = density of thin layer at average distance t from casting surface

$$\int_o^t \rho_t \, dt = \overline{\rho_t} \int_o^t dt = \overline{\rho_t}\, t$$

From the experimental data the average density, $\overline{\rho_t}$, was found to be related to thickness, t, approximately as follows:

$$\overline{\rho_t} = 2.7028 - 0.0708t^2$$

substituting this value,

$$\int_o^t \rho_t dt = (2.7028 - 0.0708t^2)\, t = 2.7028t - 0.0708t^3$$

differentiating,

$$\rho_t = 2.7028 - 0.0708(3)t^2$$
$$= 2.7028 - 0.2124t^2$$

ρ_t is the density of a layer of infinitesimal thickness at any distance, t, from the external surface of the casting, and the plot of this value, the dashed curve of Fig. 38, shows a very marked decrease in density from the external surface to the center of the casting section. This increase in unsoundness toward the center of the section is probably attributable to unfed shrinkage.

Conclusions

1. Satisfactory slush castings could not be obtained in baked sand molds using dilute aluminum-copper, aluminum-silicon or aluminum-nickel alloys because of the low temperature gradients and the extreme hot shortness of the alloys at temperatures slightly below the liquidus temperature.

2. Castings in which a solid shell of measurable thickness could be retained in the mold after pouring out the residual liquid metal were produced using 99.8 per cent aluminum.

3. The relation between the thickness of the solidified layer and the time interval between casting and draining appeared to be parabolic, and the following approximate relation was established:

$$D = 0.0367\sqrt{t} - 0.11$$

where D = thickness of solidified layer, in.
t = time interval, sec.

4. These data are indicative of a short time lag from the instant of pouring to the beginning of solidi-

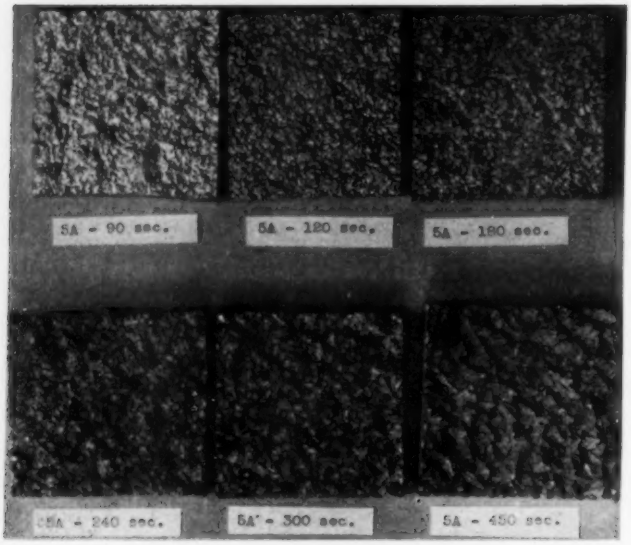
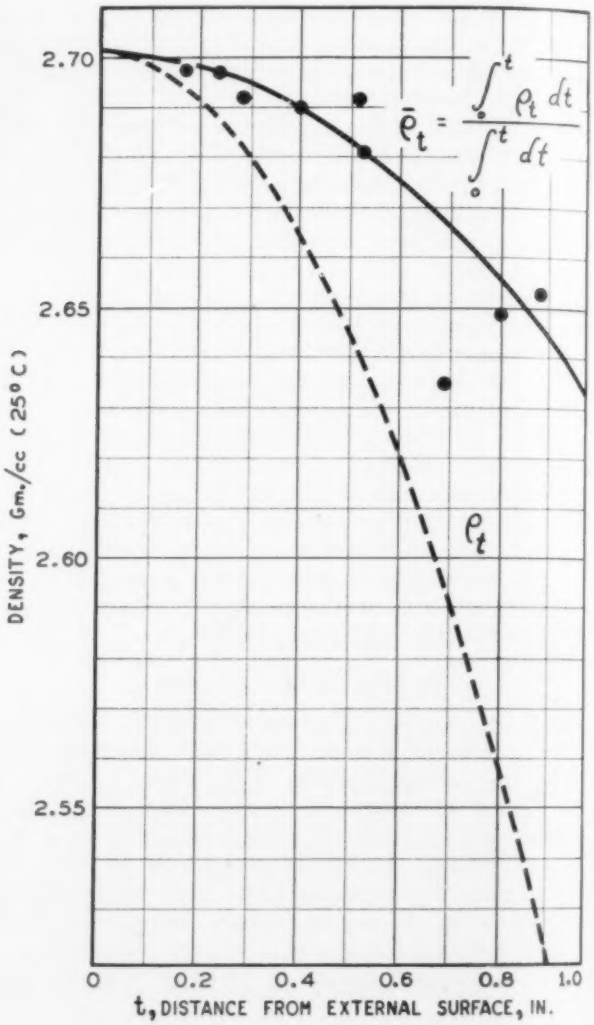


Fig. 37—Inner surface of pour-out castings at various time intervals illustrating the progressive change in dendrite size. Reduced one-half.

Fig. 38—Average density, ρ_t, and layer density, ρ_t, of sections from pour-out castings (99.8% Al).



fication amounting to approximately 9 sec and attributed to the superheat of the liquid metal.

5. The time required for complete solidification was computed from the pour out data to be approximately 915 sec. A cooling curve of a casting which was allowed to solidify completely indicated that about 1125 sec were required. This discrepancy may be attributable to uncertainty in the interpretation of the cooling curve.

6. Solidification rates ranging from 6.12×10^{-3} in./sec at the start of solidification to 6.07×10^{-4} in./sec just prior to completion were derived from the data.

7. An increase in grain size from the surface to the center of the castings was evident and is attributed to the retardation of solidification and probably preferential growth.

8. The marked reduction in density from the surface to the center of the castings is probably the result of increasing obstruction to feeding.

References

1. Private Communication. Letter from Dr. H. A. Schwartz to L. W. Kempf, Jan. 29, 1946.

2. H. A Schwartz, "Solidification of Metals." *Transactions*, AMERICAN FOUNDRYMEN'S ASSOCIATION, vol. 53, p. 1 (1945).

STUDIES ON SOLIDIFICATION OF CASTINGS

Approximate Equation for Steel Castings

By

Dr. Victor Paschkis

Columbia University, New York City

In the first report of this laboratory to the Heat Transfer Committee, a number of solidification curves were shown for steel cast in sand and against a chill. This report is to attempt to summarize the findings of the first report* in such a way as to give tentative general solutions to the problems of solidification.

Before the solutions are presented, it is well to remember that the experiments were relatively limited in number. The difficulty of the experiments necessitated averaging for each experiment the values of several runs. Whereas for the sake of comparison with bleeding tests, this averaging is sufficient, it will lead to erroneous results if it is attempted to obtain accurate formulas from these values. Moreover, the number of changes made was quite limited and therefore again the value of such generalizations is limited.

The simplified equations presented below should be considered as a first approach to the setting up of general rules for solidification. In order to improve the validity it would be desirable to run more tests on the analyzer and see how additional values check as against the equations.

More Experiments Needed

In way of example, it should be said here that only two castings in sand had been tested, one casting being 2 in. and the other 4 in. thick. For these two castings a reasonably simple formula can apparently be set up, but to be sure that this formula holds, it would be necessary to compare experiments for values between those two limits of 2 in. and 4 in. respectively, and in addition, to find the limits up to which the generalized formula holds.

With these limitations in mind, the following equations are offered:

Casting of steel against sand, no chill

The equation holds under the assumption of sand at least five times as thick as the half thickness of steel. For example, for a slab 3 in. thick the sand would have to be 7½ in. or more thick.

The rate of solidification can be expressed by an equation relating the solidified thickness, L, to the time, t, of solidification. It is necessary to write two expressions, one for the earlier parts of solidification and the other for later parts.

For the early parts of solidification, namely up to the time when 0.4 of the thickness is solidified, the following equation holds:

$$L = 0.465 \sqrt{t} \quad \text{.....(1)}$$

In this equation t stands for the time of solidification in seconds, and L for the solidified thickness in inches.

This equation if plotted in a graph, L vs $\sqrt{t}$ gives a straight line. For later parts in the solidification beyond the range of the previous equation, the solidification can be approximately expressed by the following equation:

$$L = 1.88 \pm 0.1 \sqrt{267 - 18.8 \sqrt{t} + t} \quad \text{.....(2)}$$

This equation plotted in the same kind of graph as described above, yields a circle with the center characterized by the following coordinates:

Abscissa 9.4 $\sqrt{\text{sec}}$
Ordinate 1.88 in.

Casting of Sand against a Chill

The equation holds under the assumption that the chills are 0.75 times as thick as the total thickness of the steel casting. For a steel slab of 3-in. thickness, the chill would therefore be 2.25 in. thick. A further assumption is, that the chill is backed up by sand 1.25 times as thick as the steel casting. For a steel casting of 3-in. thickness, the sand would be 3.75 in. thick.

A further assumption is that the air gap time may be expressed by the following equation:

$$t_A = 225\ L^2 \quad \text{.....(3)}$$

Here t_A is the time of formation of air gap, and L the total thickness of the casting. This equation covers the best checking chill tests of the report referred to above.

Under these assumptions the solidification process can be expressed again by two equations:

$$L = 0.116 \sqrt{t} \quad \text{.....(4)}$$

Equation 4 is valid up to a time when two-thirds of the total thickness is solidified.

Equation 5 relates the time of total solidification with the total thickness of the casting. Equation 5 reads:

$$t = 2.09 + 5.025\ L + 0.365\ L^2 \quad \text{.....(5)}$$

The use of equations 4 and 5 is as follows: First plot the early part of the solidification curve from equation 4. Then plot the end times for complete solidification from equation 5. Then estimate the missing connecting part for the solidification line from the end point to the last point found from equation 4.

DISCUSSION

Chairman: H. A. Schwartz, National Malleable & Steel Castings Co., Cleveland

Co-Chairman: E. C. Troy, Dodge Steel Co., Philadelphia

Dr. V. Paschkis: In Mr. Hunsicker's report, temperature curves for the sand (Fig. 30) and for the aluminum alloy (Fig. 29) are shown. Based on these curves one can calculate the heat balance for the end of freezing, i.e., for 600 seconds after pouring. The heat extracted from the aluminum alloy is the amount of superheat plus the heat of fusion. The heat absorption of the sand can be calculated, if we assume, that the sand temperatures are the same on all four sides of the casting. This assumption is valid, if it is permissible to neglect end effects.

Actually the corner effects may not be neglected, as may be seen from the following figures. The heat extraction from the alloy is 273 Btu per inch of height of the casting. But the heat absorption of the sand is 712 Btu per inch of height of the casting, provided the isotherms are parallel to the faces of the casting.

Inasmuch as the two figures do not agree, it may be concluded, that the casting was not large enough to warrant assumption of heat flow only perpendicular to the sand-casting interface. A detailed proof will appear in next years report of the heat transfer committee.

We also duplicated the aluminum casting on the Heat and Mass Flow Analyzer. In our work we originally took the thermal properties of the sand to be the same as those used in last years work on steel castings, and in this years work on white cast iron (Dr. Schwartz paper). We did not find good agreement between Mr. Hunsicker's values of solidified thickness and ours. Then we looked over the conductivity values for sand, and compared them with the findings of Battelle Institute. We noticed, that the conductivity at the pouring temperature of aluminum is approximately half as large as the sand conductivity at the high temperature used in steel and iron casting. We repeated our analysis with the lower conductivity values and found very gratifying correlation between the results of Mr. Hunsicker and ours.

* V. Paschkis, "Studies in Solidification of Castings," *Transactions*, A.F.A., vol. 53, p. 90 (1945).

C. W. BRIGGS[1]: Why did the Heat Transfer Committee take this type of unusual size specimen, when they must have known from the beginning it would not represent an infinite type of casting; and if the corner effect was so pronounced why did they not use the sphere type of casting to start with?

DR. SCHWARTZ: The Heat Transfer Committee did not know until it got to working with close approaches to complete solidification that slab was not sufficiently near an infinite slab, so that the cooling would be right in the middle. We did not know that, even though the original dimensions of the slab were based on heat analyzers to begin with.

Perhaps, I should say, that at the start there was not too much known about how to approach these problems, and that the committee is trying to build itself a structure as it goes along, and it is not going to be extremely embarrassed if it finds it makes a mistake on the road. It hopes to rectify those mistakes as it goes along.

W. J. KLAYER[2]: From Fig. 24 we apparently should presume that the coarser sand has the higher conductivity, but do you think that would work also with the lower melting point materials? The curves only go down to 800° F. and the aluminum alloy castings in some places where we use Albany sand, the temperature of the sand never reaches that high temperature. Do you think those three lines would begin to come together?

MR. LUCKS: They are closer together at the lower temperatures but I would not say they would come together and have the same values.

DR. SCHWARTZ: In regard to these conductivities, a year ago I tried to separate the conductivity as such from the heat transfer by radiation from grain to grain. If we put in a lot of layers of fine sand, each step in the radiation transfer goes over a shorter range of temperature. Consequently the heat transfer from one point to another by radiation is in many steps each proportional to the difference of point and the fourth power of two temperatures. This rate is much less than if you went right in one step between the two extreme temperatures existing between two layers at given distances apart. It is the same thing as putting in aluminum foil insulation. The shape of the curve is roughly what you might think would happen with the summation of the heat transfer by radiation and the heat transfer by merely conductivity of solid material. I am speaking now of the three sands which are a portion of this paper as related to the little note the year before in which these calculations were made on just the basis of assumptions.

H. E. FLANDERS[3]: Dr. Schwartz mentioned the effect of radiation on the conduction of heat in sand molds. It is very likely that radiation at the higher temperature is the chief means by which heat transfer from one part to the other, from the part to the casting and from the casting to the sand. What will this do to the mathematical analysis and electrical analysis on the conduction of heat through the sand mold from the casting?

DR. PASCHKIS: It is possible to represent on the Analyzer an apparent conductivity curve such as described by Dr. Schwartz: the heat transfer through the sand being composed of radiation between the grains of sand and of conduction through the sand proper. However, so far we have worked with constant properties of sand, because all together there is not yet enough known regarding the conductivity of sand. With the constant values assumed by us, we obtained close correlation of results for steel castings as well as for aluminum castings.

[1] Steel Founders' Society, Cleveland
[2] Aluminum Industries Inc., Cincinnati
[3] University of Utah, Salt Lake City, Utah

DR. SCHWARTZ: The most important variable in affecting freezing time here is that variable about which the physicist can do nothing and about which the foundryman does everything.

You change your solidification time by the pouring temperature that you select more than by any other item that enters into the picture. That is if you are interested in the absolute time in which a given object is going to freeze to its center. The Committee is cognizant of the fact that one may be more interested in the relative times of the different types of sections to freeze to given thicknesses than the absolute times it takes to freeze. But doing the simplest things first, the Committee is not doing that yet. But I do feel that with the type of investigation in which we are now engaged the foundryman can introduce a lot more variation by peculiarities of his operation than the physicist can keep up in the terms of determining physical contents.

MEMBER: What are the properties of the sand? Did you vary the heat diffusion?

DR. PASCHKIS: As I mentioned before, we do not vary the properties of the sand.

MR. FLANDERS: In Dr. Paschkis' report he said that he extrapolated his data and came down to zero freezing time. We know that is impossible so I was wondering where else we might extrapolate that data. Then the other question deals primarily with the practical application of this method to include the time of gray iron solidification in particular. I once worked on the solidification of gray iron with respect to the development and use of machine tool castings and it is extremely important, in getting good structure, that there is sufficient temperature so that the metal goes through the solidification range at least at a reasonable rate and does not start the chilling action immediately upon pouring. What happens is that the mold surrounding the casting is heated due to the heat transfer. The idea of superheat that has been brought out in this paper, I think is definitely important in pointing out that the time of solidification through the solidification range is prolonged and the dendritic pattern is decidedly changed.

DR. PASCHKIS: I cannot entirely agree that extrapolation here is improper. If a steel with a liquidus temperature of 2700° F. is poured at 2700° F., then, obviously the time to reach 2700° F. is zero.

MR. FLANDERS: I misunderstood your remark. That is perfectly all right. I understood you to say that the solidification time was zero.

DR. PASCHKIS: The solidification time is the time to reach 2700°.

MR. FLANDERS: Not the way I understand it. The solidification time is the time during which solidification takes place. We may pour steel that reaches 2700° in zero time but it can stay at 2700° indefinitely if we extract heat slowly and the solidification time can become infinite if our rate of heat extraction is zero.

DR. PASCHKIS: We have to agree of course as to what we mean by "solidification time." We found, that the time required to reach the liquidus line in our electric experiments checked the time obtained from bleeding tests. The statement that the solidification time is the time required to reach the liquidus is thus not a premeditated conception, but rather a fact, found empirically. I personally know as yet no explanation for this behavior. I can only repeat, that in a number of experiments we found, that the thickness obtained in a given time in bleeding experiments is the same as the thickness shown by our electric experiments, if we assume the liquidus line to be the criterion of solidification.

AN INTERPRETATION OF THE CONSTITUTION OF IRON-CARBON-S LICON ALLOYS

By

J. E. Rehder *

Toronto, Ontario

ABSTRACT

Review and correlation of published experimental data on the ternary system iron-carbon-silicon is shown to lead to the conclusion that iron-carbon-silicon alloys, at least in the range of zero to four per cent carbon and zero to four per cent silicon, act as though the silicon is all present as dissolved Fe_3Si, with the iron and carbon acting as a pure iron-carbon alloy. It is suggested that similar reasoning can be applied to the low phosphorus range of iron-carbon-phosphorus alloys.

THE TERNARY SYSTEM IRON-CARBON-SILICON is the basis of all commercial malleable cast irons and gray cast irons, and is of importance in many steels, and so has been the subject of intensive study for many years. The system is complex, and only the high iron corner has been delineated. In view of the large bibliography on the subject, reference will here be limited to the monograph *The Alloys of Iron and Silicon* [1] which contains an excellent summary of technical literature up to the year 1933, and to experimental work pertinent to the development of the present argument published since.

The ternary diagram of the complex iron-rich corner presented in reference [1] is the most recent published. Search for a simplification, and review of recent published work, led to a view of the constitution of iron-carbon-silicon alloys in the range of widest commercial use (zero to four per cent carbon and zero to four per cent silicon), which fits many of the known experimental data into a simple pattern. The present paper will describe the steps leading to a conception of iron-carbon-silicon alloys, in this range of compositions at least, as iron-carbon alloys containing dissolved Fe_3Si.

Reference (1) reviewed the experimental evidence available up to 1933 concerning the existence of the compound Fe_3Si in the iron-rich iron-silicon alloys, and concluded that its existence was indicated but not proven. Since then the work of Korber and Oelsen,[2] who gave evidence of the existence of Fe_3Si from heat content curves, Vaughan and Chipman,[3] Darken [4] and Chipman [5] leads to the conclusion that silicon acts in liquid low-silicon iron-silicon alloys as though it were combined with iron as Fe_3Si.

The work of Ihrig [6] on silicon impregnation indicates that silicon also persists in solid iron-silicon alloys as Fe_3Si, as analysis of silicon cases shows the silicon to diffuse inward from a surface layer of relatively constant composition close to the stoichiometric proportions of Fe_3Si. It is therefore logical to investigate iron-carbon-silicon alloys, at least in the lower ranges which are of commercial interest, from the viewpoint that they are iron-carbon alloys with the silicon combined with a portion of the iron as Fe_3Si.

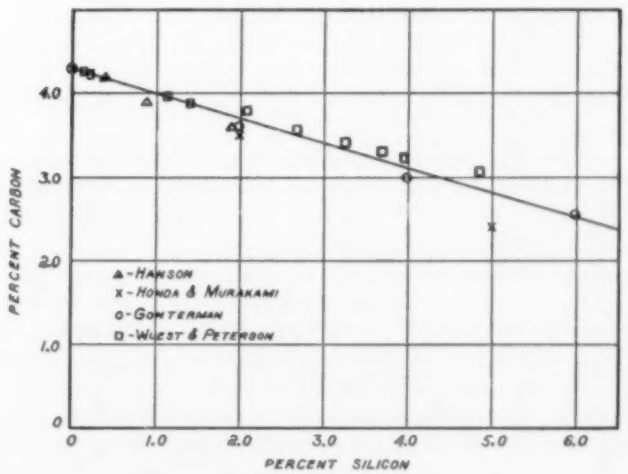


Fig. 1—Fe-C-Si eutectic compositions.

The lowering of the carbon content, with increasing silicon, necessary to form a ternary eutectic in these alloys has been determined experimentally several times, and Fig. 1 shows a graph containing experimental points from *The Alloys of Iron and Silicon* [7] and from Wuest and Peterson reported by Bolton.[8] The data can be represented by a straight line whose equation is

$$\text{Eutectic Carbon} = 4.30 - 0.30\,\text{Si} \quad \ldots\ (1)$$

This relationship is in common use as a means of determining the closeness to eutectic composition of gray cast irons.

If the silicon in each composition on this line is as-

* Formerly, Metallurgist, Grinell Co. of Canada, Ltd. Now, Metallurgical Engineer, Physical Metallurgy Research Laboratory, Bureau of Mines, Ottawa, Canada.

sumed to exist in the alloy as dissolved Fe_3Si, then each per cent of silicon will form 6.97 per cent of Fe_3Si and "tie up" 5.97 per cent of iron. The pure iron-carbon alloy remaining is thus enriched in carbon as a result of the iron held by the silicon. It should be noted that it is not postulated that the Fe_3Si crystallizes out as such, or is present as a discrete phase visible metallographically. It remains in solution.

Calculation of the composition of this iron-carbon alloy in a ternary eutectic shows that it contains very close to 4.30 per cent carbon, or eutectic composition for pure iron-carbon alloys. Table 1 gives the results of this calculation applied to a series of iron-carbon-silicon eutectic alloys. It can be seen that the iron-carbon remainder is of constant eutectic composition for pure iron-carbon alloys, over the range of silicon contents shown in Table 1.

Another way of expressing the results shown in Table 1 is that the iron-carbon eutectic composition is constant at 4.30 per cent carbon, but the addition of dissolved Fe_3Si lowers the percentage of carbon in the alloy as a whole by dilution. Iron-carbon-silicon alloys may then be conceived as alloys of iron and carbon with dissolved Fe_3Si.

It can also be shown mathematically that the assumption of Fe_3Si in the ternary eutectic leads to equation (1) aforementioned. This, of course, is a mathematical re-statement of Table 1.

Consistency requires that the above reasoning should apply to other points on the iron-carbon diagram besides the eutectic. Figure 2, with experimental points [9] from *The Alloys of Iron and Silicon*, shows the lowering of the maximum solubility of cementite (point E on the iron-carbon diagram) as silicon content increases. A line with a slope representing the presence of silicon as Fe_3Si traverses the experimental points satisfactorily.

The percentage of carbon necessary for pure eutectoid structure, as affected by silicon, is also shown in Fig. 2 with experimental points from reference (9). Again a line representing the assumption of silicon present as Fe_3Si traverses the experimental points.

The effect on these three important points of the iron-carbon diagram as silicon is increased is shown in Fig. 3, in which the carbon axis may be considered as a vertical view of the iron-carbon diagram. For example, at two per cent silicon the eutectoid carbon will be 0.60 per cent, the maximum solubility of cementite will be at 1.65 per cent carbon, and the ternary eutectic point will be at 3.70 per cent carbon.

The assumption of the existence of all silicon in these alloys as Fe_3Si in the range of compositions under discussion should show effects on the temperatures of transformations. Figure 4 shows experimental data [10] on the change in eutectoid and eutectic temperatures as silicon content increases. As the composition Fe_3Si is approached it would be expected that eutectoid and eutectic temperatures would approach one another and become the same as the melting point of Fe_3Si. The lines drawn on Fig. 4 show that this trend is followed for the lower silicon concentrations.

Experimental work on the alloys of iron and phosphorus as reported by Bolton [11] shows the compound

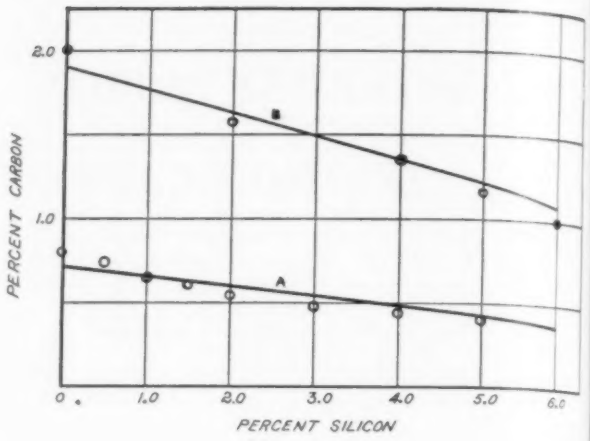


Fig. 2 (above)—Effect of silicon on eutectoid carbon and solubility of cementite.

Fig. 3—Effect of silicon on eutectic and eutectoid carbon and solubility of cementite.

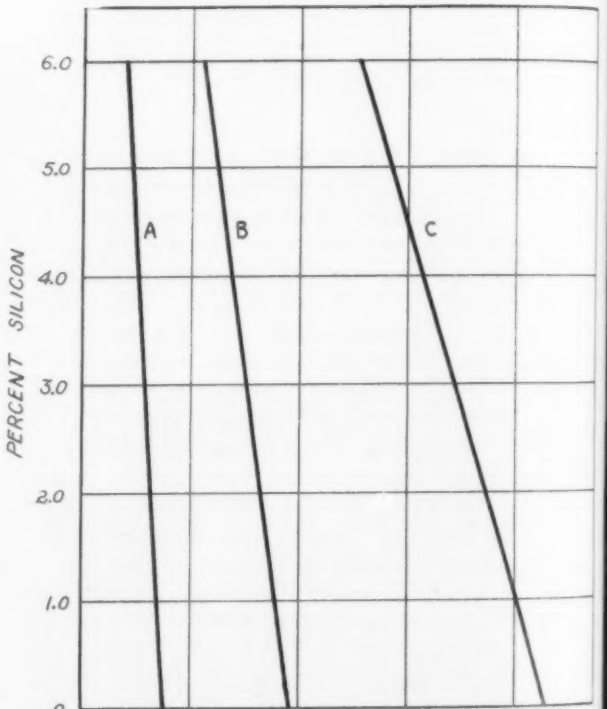


TABLE 1.—COMPOSITION OF Fe-C LIQUID IN Fe-C-Si EUTECTIC ALLOYS

Si, per cent	C, per cent	Fe_3Si, per cent	(Fe+C), per cent	$\frac{100C}{(Fe+C)}$ per cent
0.00	4.30	0.00	100.00	4.30
0.50	4.15	3.48	96.52	4.30
1.00	4.00	6.97	93.03	4.30
1.50	3.85	10.45	89.54	4.30
2.00	3.70	13.94	86.05	4.30
2.50	3.55	17.42	82.58	4.30
3.00	3.40	20.91	79.09	4.30
3.50	3.25	24.39	75.61	4.30
4.00	3.10	27.88	72.12	4.30

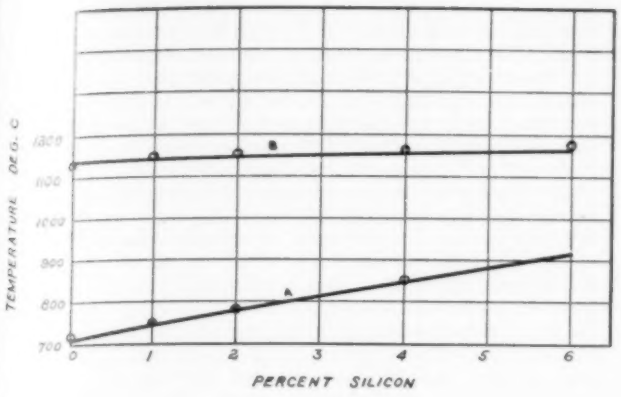


Fig. 4—Effect of silicon on eutectic and eutectoid temperatures.

Fe_3P as a constituent and that phosphorus lowers the eutectic carbon of iron-carbon-phosphorus alloys in roughly the same proportion as does silicon. Since the atomic weight of phosphorus is close to that of silicon, and similar iron compounds are concerned, it is logical to propose a similar mechanism for iron-phosphorus alloys as for iron-silicon alloys. This fits well with the common practice of adding the percentage of phosphorus to the percentage of silicon when calculating eutectic carbon by equation (1), the phosphorus and silicon acting separately but in the same manner and direction.

Conclusion

Experimental data on iron-carbon-silicon alloys in the range of zero to four per cent carbon and zero to four per cent silicon is consistent with the assumption that all of the silicon is present as dissolved Fe_3Si and the remaining iron and carbon act as a pure iron-carbon alloy. The constitution in this range of compositions at least is therefore one of iron-carbon alloys with dissolved Fe_3Si, or Fe_3Si-Fe_3C-Fe.

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DISCUSSION

Chairman: V. A. CROSBY, Climax Molybdenum Co., Detroit.
Co-Chairman: MILTON TILLEY, National Malleable & Steel Castings Co., Cleveland.

CO-CHAIRMAN TILLEY: From the practical man's view, when you look at these theoretical figures you wonder why they do not correlate with practice. For instance, at 10 per cent silicon silvery pig iron, we have 2¼ per cent carbon, where this theoretical figure would be zero. Why do the figures not follow the theory?

DR. R. SCHNEIDEWIND:[1] Silvery pig iron naturally is poured at a temperature above the eutectic temperature, and if the metal comes out of the furnace at 3100 or 3200° F. it will have considerably more than the eutectic amount of carbon. Most of the excess carbon will tend to kish out. Since the temperature is above the melting point, the actual carbon content will be above the eutectic. With 10 per cent silicon, you will have around two per cent carbon. All silvery pigs will contain carbon in excess—0.25 per cent at the low silicons and about 1.0 per cent in excess at 10 per cent silicon.

W. D. MCMILLAN:[2] Does that apply to blast furnace silvery iron?

DR. SCHNEIDEWIND: Yes.

MR. MCMILLAN: Electric furnace silvery iron will run only about 0.5 per cent carbon. Does anybody else have that same experience—0.5 per cent carbon with 15 per cent silicon?

CO-CHAIRMAN TILLEY: I did not know that.

MR. REHDER: Everyone is aware of the fact that pig irons and silvery pig irons run quite high in carbon—higher than you can account for on the basis of Fig. 1, but I do not think you can consider pig irons as equilibrium phenomena. They are usually poured in chilled molds under conditions that promote maximum retention of carbon, although that should not affect the true solubility. In this paper I am talking only about four per cent silicon and under. Above that percentage those curves do not tie in, and furthermore they do not even seem to add up between various investigators. If that curve, when you get above, say, 10 per cent silicon, described a loop that would be fine as long as everybody agreed that that was what happened. Then you would have to find out why. But there does not seem to be any agreement above that figure, so I am restricting myself to below six per cent silicon as a maximum.

Then again you can get hyper-eutectic cast iron. It is not difficult. Take a hyper-eutectic cast iron, and it is not too difficult to find some primary dendrites, or what look like primary anstenite dendrites in that sample, if properly etched. I have always wondered where dendrites came from in a hyper-eutectic cast iron. That is one of the things that may point toward an explanation.

W. H. WHITE:[3] We developed a carbon vs. silicon diagram for blast furnace and silvery pig irons (Fig. 5). In getting down

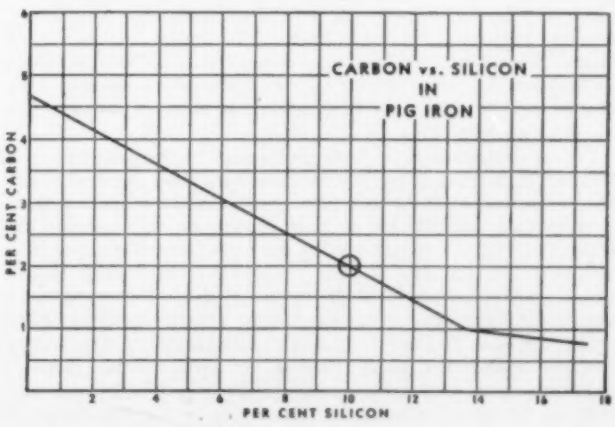


Fig 5—Carbon vs. silicon in pig iron

to about one per cent carbon, at a silicon content of 13.6 per cent, we found a definite break in the diagram. There was a levelling of that line, showing that the carbon was reduced very much less for an increase of one per cent silicon after the

[1] University of Michigan, Ann Arbor, Mich.
[2] International Harvester Co., Chicago.
[3] Jackson Iron & Steel Co., Jackson, Ohio.

point of 13.6. The entire line was distinctly above the eutectic line but roughly parallel to it. It occurs to me, that point at 14.3 is analagous to the point at which we found a break in the curve on the pig iron diagram. Is that possible?

MR. REHDER: The only reason that particular composition of silicon (14.34 per cent) is there is because that happens to be the composition of pure Fe_3Si. I am not saying that is the way the line goes above six per cent silicon. It certainly goes that way below that per cent of silicon.

Dr. Hurst in England has done a lot of work on silicon-bearing corrosion-resistant iron. As a matter of fact, they run about 14 per cent silicon. They show that in cupola melted irons you can get appreciable amounts of carbon and you can get more than one per cent if you quench the iron in water, but even in the normally cast alloys you get an appreciable content of carbon, 50 to 60 points, which of course, according to the extrapolated line in the paper, you are not supposed to have, but I was not talking about silicon contents above about six per cent silicon.

The fact that the pig iron curve for carbon vs. silicon lies above that, I think again is simply a question of some supersaturation.

MEMBER: I have noticed that 10 per cent silicon iron rusts considerably. When the silicon content is increased to 15 per cent, the iron is stainless. Is that the line of demarcation where it changes from a corrosive iron to one that is stainless?

MR. REHDER: I do not know whether there is an exact point or not. I do know that you apparently have to get above 10 per cent before it starts to become stainless. Of course, that lies directly in the field of people who are making the high silicon alloys for corrosion resistant purposes, and one of the explanations that has been advanced in the literature is that you are getting close to that compound, Fe_3Si, and you are getting some stainlessness.

There are a lot of ramifications to this thing. If you take X-Ray data you get quite a sharp break in the lattice constant. I think it is at about nine atomic per cent silicon. That is about six weight per cent. Apparently something different happens in there, but there is no agreement on the mechanism.

There is some analogy between the iron-silicon and iron-aluminum systems. On the iron-aluminum diagram, there is a point, Fe_3Al, and aluminum has an atomic weight close to that of silicon. No one claims that Fe_3Al is a separate compound, but it certainly looks like it on the basis of electrical resistivity. It acts that way.

In iron-aluminum alloys you get what is known as a superlattice formation, due to the presence of Fe_3Al. It starts at a fairly low percentage of aluminum. In a work published in 1945 in the *Journal* of the Iron and Steel Institute (Great Britain) on Iron-Aluminum Alloys it is shown that a superlattice formation is taking place there. That is the theoretical background for some of the matters discussed. I do not think it necessary to consider it in the range of silicon content below six per cent by weight.

DR. SCHNEIDEWIND: I think we ought to congratulate Mr. Rehder in making sense out of something that is very highly theoretical and about which there is not too much data. I think it is quite a contribution to have shown the relationship that if we had Fe_3Si, it would predict the experimental results. The equation of that line is that carbon plus something silicon equals four point three. If this intercept is 14.34 and the other one is 4.3, does that come out 0.3 or does it come out 0.33?

MR. REHDER: It comes out 0.301. Incidentally, I was a little hesitant about this paper because to some of the practical foundrymen it may seem remote from iron-foundry practice. But we are only going to learn more about the metallurgy of malleable and gray iron in the proportion that we understand a little better the reasons behind some of the things that go on.

To take an example from everyday malleable iron foundry practice illustrating this latter point, a foundry was using a 1½-in. sprue test at the furnace for mottle control. The iron was duplexed in a cupola-air furnace combination. The sprue was poured in an open top core mold and one of the fractured ends of the sprue was drilled for chemical analysis. Occasionally carbon contents would be much lower than indicated by other results and by the fluidity of the iron, and analyses of production castings showed the carbon content to be normal. These false low carbon results upset our control and the matter was gone into thoroughly. The cause, when found, proved to be something that I think may be overlooked sometimes. In a normal bar poured in an open top mold, you usually get a shrink in the center. If the drillings are taken from outside of the shrinkage area, the analysis will be the same as though you had poured a shot sample or a thin bar or something that had no shrink in it. That is the analysis of the iron as a whole. But what had happened occasionally was that the drillings were taken by drilling axially down the sprue almost entirely in the shrinkage area, and these drillings were low in carbon. We then poured a whole series of bars, on different days and of different compositions, and in every one of them we drilled nothing but the shrink with a small drill. The shrinkage areas were not discolored or oxidized as they were in the center of the bar. Then we took drillings from the solid outer portion of each bar. There was about 50 points difference between the carbon in the shrinkage area and the carbon in the solid portion. The shrinkage area is always lower in carbon. The silicon is higher by five to eight points in the shrink than it is in the iron as a whole. The manganese is about the same. The sulphur is lower, and the phosphorus is lower in the shrink. In the shrink, there are mostly primary dendrites. As shown by the iron-carbon diagram there is a considerable amount of primary austenite dendrites in malleable iron when the solidus is reached, and the amount increases for lower carbon malleable. What is being drilled in the shrinkage area is mostly primary dendrites from which a good deal of the eutectic liquid has been drawn by liquid shrinkage at the solidus. The primary austenite dendrites are supposed to be lower in carbon than the body of the alloy, and that is what you find. The phosphorus and sulphur are lower in the shrinkage area because it is not supposed that they do separate out with the primary austenite, but the interesting thing is the difference in silicon contents. You would expect that they would be the same. Apparently, the silicon is not freezing uniformly through the whole mass. If you get a shrink you can get a segregation. That was one of the things I had in mind in writing the paper, trying to explain why there is a difference in silicon. Any of you who have broken annealed castings may have run into the fact that close to your shrink you probably have a harder iron. You look at it under the microscope and there is pearlite or cementite close to the shrink. That again is tied in with the fact that the silicon content is different there. I think this paper may help explain some of those things.

MR. MCMILLAN: Do I understand correctly that you figure that the melting point or decomposition point of Fe_3Si is around 1200° F.?

MR. REHDER: No. The iron-silicon diagram at present is not exactly determined, and many temperature points are in doubt. What that exact temperature is, I do not think anyone can say at present. It apparently is a eutectic point. It is not a melting point as such. It is similar to the eutectic point of carbon, which is 4.3 per cent.

CO-CHAIRMAN TILLEY: The handbooks give the melting point of ferro-silicon as 1200° F., too.

MR. REHDER: At the moment, I do not remember the higher silicon parts of the diagram. Fifty per cent silicon, of course, is a long ways from 14 per cent. I cannot answer just off hand. As a matter of fact with 50 per cent ferro-silicon you are into another compound, FeSi, equal molecular proportions of iron and silicon. It is well recognized as a distinct compound.

CO-CHAIRMAN TILLEY: The analysis of the shrink area being lower in silicon is new to me. I have always attributed the cementite which you often find in shrinks to the fact that the carbon had no path to migrate, and therefore it did not break up. The silicon may have an effect there.

MR. REHDER: I was surprised at finding that effect in the shrink, but I did it on different days, on different bars, running the silicon analyses side by side, the shrink and the solid part, and so far as I can see, it does check.

On the question of not having a migrating path, I usually find the cementite in the solid material adjacent to the dendrites and my interpretation is simply that your silicon is a little low there and for a given cooling rate it will not decompose.

H. L. DAY:[4] We should bear one thing in mind about these theoretical curves. They are distinct equilibrium curves. There

[4] Auto Specialties Mfg. Co., St. Joseph, Mich.

has been a lot of comment about comparing curves with actual practice. Our material is seldom in equilibrium. It should be remembered these are distinct equilibrium curves.

MR. REHDER: I would make one point on this question of the curves being theoretical. They are based on experimental data of course, but if the iron-carbon and iron-silicon diagrams are combined to give an iron-carbon-silicon diagram, using the relationships given in the paper, it will be seen that the effect on the liquidus and solidus curves of introducing silicon into an iron-carbon alloy is to make the alloy act as though it had a higher carbon content.

The interesting thing is, the cooling curves published of ordinary cast iron fall right on the point obtained by moving the iron-carbon diagram to the left in accordance with this formula, $C + 0.3\ Si = 4.3$. The curves do check the diagram. They do represent what actually happens in iron that is cooling in the mold provided, of course, it is not small sections that are cooling very rapidly.

Another interesting thing is this, and this has practical significance: If you plot fluidity of iron vs. carbon content, the fluidity increases. As you increase in carbon, the melting point of your iron comes down and your fluidity goes up in direct proportion until you arrive at the eutectic. Then the fluidity drops. If you put one per cent silicon in, you move the eutectic carbon to the left. If you plot your measured fluidities again, it again is inversely proportional to that line, and there again the fluidity checks very well with the movement of the diagram to the left by silicon.

That is something of practical interest to the foundryman. I can put it this way: It looks like silicon is about one-third as effective as carbon in lowering the melting point and increasing the fluidity thereby. That is a practical result of that diagram.

DR. LORIG:[5] The paper does a remarkably good job of correlating some of the things that we notice, not only in the laboratory, but also in the foundry. It ties in very closely with foundry operations.

I think there is this point that might be mentioned. In the corrosion-resistant alloys of this type, unless there is more than 14.3 per cent silicon the full benefit from stainlessness is not achieved. In the manufacture of high silicon irons an attempt is made to reach a silicon content which is just above 14.3 per cent, another indication that perhaps it is the compound Fe_3Si in the alloy that makes for stainlessness although one is not sure the compound exists in these irons.

CO-CHAIRMAN TILLEY: In ihrigizing, when the outside of the metal is impregnated with silicon, what percentage is involved?

DR. LORIG: It approaches 15 per cent.

MR. REHDER: In the paper I mention ihrigizing. You impregnate iron with silicon and you get a case on it that contains between 14 and 15 per cent silicon. It depends on various factors. It runs betwen 14 and 15 and even if you make the case very deep, you do not build up silicon on the outside. It builds up to between 14 and 15 per cent and then the whole thing seems to go in en masse, which is another evidence that the Fe_3Si proportion is acting as though it were a compound. I still do not say it is a compound, but it acts that way.

Incidentally, the diffusion constant for silicon, which is of importance in this thing, is about one hundredth of that of carbon, as measured from those siliconized cases.

MEMBER: An interesting corollary to this paper is the support of the use of the figure 0.3 for the slope of that line. It may go a long way in settling this controversy over whether to use one-third silicon or three-tenths silicon plus phosphorus. Personally, I have always favored the three-tenths and I think when phosphorus is included with the silicon it will come close to what you get when you take one-third.

In establishing an empirical curve for pig iron, which comes down on the hyper-eutectic side, I found 0.28 as the slope of the line in simply mechanically arranging some 500 points. The slope of the line was 0.28 which checks closely with that 0.3.

[5] Battelle Memorial Institute, Columbus, Ohio.

MICRO-RADIOGRAPHY OF GRAY CAST IRON*

By
E. I. Salkovitz, J. H. Schaum, and
F. W. Von Batchelder

ABSTRACT

This report is a description of the micro-radiographic technique by which graphite in gray cast iron may be studied with conventional X-ray equipment. Good results are obtained by using an industrial type, tungsten target X-ray tube. The effects of different exposure times, photographic plates, and radiation are shown. Photomicrographs and micro-radiographs of the same specimens are included in the report to permit comparison of the graphite as observed by the two different methods. By means of micro-radiographic techniques, graphite in gray iron is found to be wider, longer, and more uniform in shape than by metallographic examination.

WITH PHOTOMICROGAPHIC TECHNIQUES for studying metals, a metal specimen is carefully polished, etched, and viewed through a microscope at magnifications normally as high as 1,000 diameters. By this type of examination it is possible to observe inclusions, cracks, holes or other defects, as well as such metal structures as pearlite, cementite, ferrite, martensite, graphite, etc.

Metallurgists have long taken advantage of the ability of X-rays to penetrate metals and reveal the degree of density of the subject material on film placed behind the specimen and sensitized by the impinging X-rays. Naturally, cavities or low density areas in a specimen offer less resistance to the passage of X-rays and appear on the film as areas more heavily exposed than the surrounding metal.

Micro-radiographic Technique

These two methods of observing metals have been combined to form a third known as micro-radiography. In micro-radiography, X-rays are directed through a thin specimen on to a fine grained sensitive film which is then developed and examined under the high magnification of a microscope. If the various micro-constituents have sufficient difference in their X-ray coefficients of absorption they can be distinguished from each other by their variations in density on the exposed film.

Metallography is only partially adequate for the study of metals, since it is limited to the two dimensions in the plane of observation. Micro-radiography has the advantage of revealing the depth of micro-constituents through variations in density.

The successful application of micro-radiography to the study of micro-segregation and micro-porosity in nonferrous alloys led to developing the most practical operating techniques for studying graphite structures in gray iron.

The size, shape, and distribution of graphite particles in gray cast iron have an important influence on such engineering properties as tensile strength, plastic flow, modulus of elasticity, damping capacity, wear characteristics, machineability, etc. Many hypotheses have been presented concerning the three-dimensional shape of various micro-constituents. The shapes of graphite particles in gray iron, for example, have been described as flakes, plates, whorls, nodules, rosettes, etc., and the distribution as random, interdendritic, eutectiform.

Space Models of Graphite Flakes

Roll's method [2] can be used to construct a space model of the true shape of graphite, but the method is tedious and requires photographing and repolishing the same area many times. Between each polishing operation the thickness of the specimen must be measured accurately. When sufficient photographic plates are obtained they are stacked with the proper spacing between them. From this three-dimensional picture, a model can then be constructed.

Because of the difficulty of such a technique only a limited amount of this work has been done and the cast iron metallurgist continues with a two-dimensional concept of graphite. The relatively new tool, micro-radiography, gives promise of being well adapted to a three-dimensional study of graphite in cast iron. In this report micro-radiographs are compared with photomicrographs of the same gray iron specimens.

The underlying physical principles of micro-radiography and various methods of sample preparation were described in an article entitled "Micro-radiography with Routine X-ray Equipment" by E. I. Salkovitz.[1]

Because most foundries do not have X-ray diffraction tubes, this study was made with an ordinary industrial tungsten target X-ray tube, the type used for routine radiography.

Specimens of cast iron were obtained and samples

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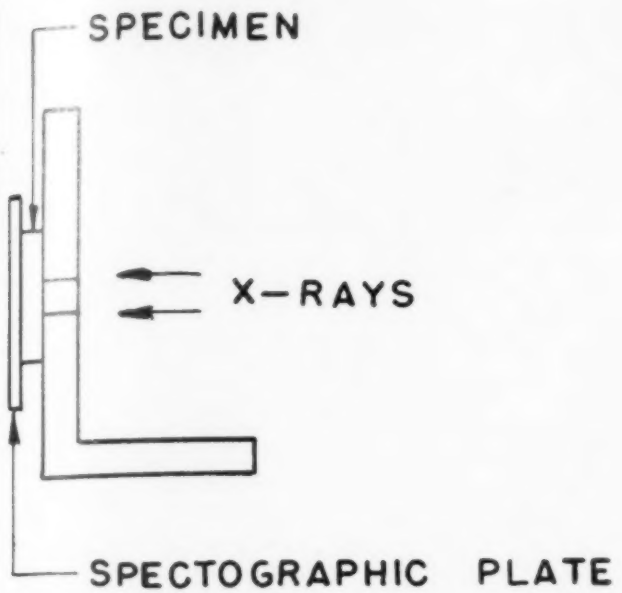


Fig. 1—Fixture for mounting specimen.

0.005 in. thick prepared in accordance with one of the several methods described in reference (1). In addition to these, another method was tried and found to save considerable time. With a thin cut-off wheel a section of cast iron $\frac{1}{32}$ in. thick was prepared. One side was polished on Aloxite paper and on 3/0 French paper.

A fixture, illustrated in Fig. 1, was made by drilling a $\frac{1}{8}$-in. hole in one of the legs of an L-shaped piece of brass. The specimen was fastened to the fixture with medium or hard deKhotinsky cement, so that the unpolished surface was available for further preparation. This was done by heating the brass plate over a gentle flame, coating it with cement, and pressing the specimen to the plate, so that it was centered over the hole. By use of a grinding wheel and then Aloxite paper, the sample was reduced to a thickness of approximately 0.005 in.

Finally the sample was polished on 3/0 paper. The entire polishing operation required approximately one half hour. Instead of removing the sample, the assembly was taken into the dark room, and a small spectrographic plate placed over the sample as indicated in the sketch. The back of the spectrographic plate was covered with black paper to prevent fogging. The unit was then clamped in front of the X-ray tube and the radiograph taken.

From an examination of the absorption coefficients of carbon and iron it is indicated that any wavelength longer than 0.5 Angstroms can be used successfully to detect graphite in cast iron. Although the highest degree of contrast is obtained when copper or nickel radiation is used, the white radiation of a tungsten target tube is adequate for this purpose. Regardless of the type of radiation, graphite appears black in the micro-radiograph because it is less absorbent than iron. The micro-radiographs in this report were made with a 150 Kilovolt medical X-ray unit. The tube was operated at 60 kilovolts because, at this relatively high value, the

TABLE 1.—SUMMARY OF MICRO-RADIOGRAPHIC DATA

Fig. No.	Sample No.	Spectrographic Plate	Exposure Technique	Magnification	Remarks
2A	A	Photomicrograph		100X	Unetched
2B	A	Photomicrograph		500X	Etched
2C	A	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	30X	Good definition and contrast. Shows distribution of graphite.
2D	A	Type 548	W rad. 60KV 10 ma. 5 min. distance 8 in.	100X	A bit grainy—Good contrast and definition. Shows graphite distribution.
3A	A	Type 548	W rad. 60KV 10 ma. 5 min. distance 8 in.	100X	Same as Fig. 2D—but different area
3B	A	Type 548	Fe rad 35KV 10 ma. 2 hrs. distance 6 in.	100X	Higher contrast and better definition
3C	A	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	100X	Underexposed. Poorer definition than Fig. 3B due to grain.
4A	B	Photomicrograph		50X	Unetched
4B	B	Photomicrograph		500X	Unetched
4C	B	Photomicrograph		500X	Etched.
4D	B	Type 548	W rad. 60KV 10 ma. 5 min. distance 8 in.	30X	Less contrast than Fig. 5A. Shows graphite distribution better.
5A	B	Type 548	Fe rad. 35KV 10 ma. 2 hrs. distance 6 in.	30X	High contrast, sharp definition. Dendritic structure clear.
5B	B	Type 548	W rad. 60KV 10 ma. 5 min. distance 8 in.	100X	Old developer used, plate dried in air blast which was too hot—result—very mottled micro-radiograph
5C	B	Type 548	Fe rad. 35 KV10 ma. 2 hrs. distance 6 in.	100X	Different area from Fig. 5B. Same as Fig. 5A at higher mag. Still very high contrast and sharp definition.
5D	B	Type 548	W rad. 60KV 10 ma. 5 min. distance 8 in.	100X	Same as Fig. 5B. Very grainy—can still detect graphite.
6A	B	Same as Fig. 5A	Same as Fig. 5A.	100X	Same as Fig. 5A. Print was overexposed.—Seems to improve contrast.
6B	B	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	100X	Too grainy—slight indication of dendrites. Contrast better in Fig. 6A.
7A	C	Photomicrograph		50X	Unetched.
7B	C	Photomicrograph		500X	Unetched.
7C	C	Photomicrograph		500X	Etched.
7D	C	Type V	W rad. 60KV 5 ma. 30 sec distance 8 in	20X	Grainy Clearly shows dendrites. Contrast poor. Definition poor. Graphite is very fuzzy.
8A	C	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	30X	Same micro-radiograph as above. Very grainy. Have reached the ultimate in magnification for this micro-radiograph.
8B	C	Type 548	Fe rad. 35KV 10 ma. 2 hrs. distance 6 in.	30X	Improved contrast and definition.
8C	C	Type 548	W rad. 60KV 10 ma. 30 sec. distance 8 in.	100X	Grainy but shows dendritic structure very well.
9A	D	Photomicrograph		50X	Unetched.
9 B	D	Photomicrograph		500X	Unetched.
9C	D	Photomicrograph		500X	Etched.
9 D	D	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	30X	Good contrast and definition. Dendrites very apparent. Distribution of graphite clearly shown.
10A	D	Type V	Fe rad. 35KV 5 ma. 5 min. distance 6 in.	30X	Slightly grainy—dendritic graphite distribution apparent. Better contrast than Fig. 9C
10B	D	Type 548	W rad. 60KV 5 ma. 5 min. distance 8 in.	30X	Considerable artifacts in film probably due to careless processing. Underexposed. Approximately same area as in Fig. 9C. Less grain. Dendrites very apparent. Good contrast.
10C	D	Type 649	Fe raddis 35. 10 ma. 5 hrs. tanceKV6 in.	30X	Extremely high degree of contrast. Dendritic structure apparent.
10D	D	Type V	W rad. 60KV 5 ma. 30 sec. distance 8 in.	100X	Same radiograph as Fig. 9C, but higher mag. Grainy. Shows graphite very well, but cannot see dendrites due to enlargement.
11A	D	Type 649	Fe rad. 35KV 10 ma. 5 hrs. distance 6 in.	100X	Same micro-radiograph as Fig. 10B. Different area enlarged—Portion of sections underexposed. Definition and contrast still very good.
11B	D	Type 649	Fe rad. 35KV 10 ma. 5 hrs. distance 6 in.	300X	Same micro-radiograph as Fig. 10D around 8 o'clock. Still high contrast and very good definition. Grain beginning to show.
11C	D	Type V	W rad. 60KV 10 ma. 30 sec. distance 8 in.	30X	Grainy. Clearly shows dendritic structure. Definition poor. Graphite is very fuzzy. Note how propagation of crack is influenced by dendrites.
11D	D	Type 548	W rad. 60KV 5 ma. 5 min. distance 8 in.	100X	Same micro-radiograph as Fig. 10A, enlarged. Mottled due to poor processing technique and to a drying air blast which was too hot. Contrast is still good.

intensity of the X-ray beam is much greater and exposure time considerably shorter than required at some lower and perhaps more desirable potential. For comparative purposes, micro-radiographs were also made with iron radiation at 35 kilovolts. Three different types of spectrographic plates were employed, Eastman Type V, 548-O, and 649-GH, having resolving powers of 170, 500, and 1,000 respectively. All plates were processed in a D-19 developer and fixed in X-ray fixing solution. Developing time was one minute for Type V and five minutes for Types 548 and 649, at 68 F. The longer time for the latter two resulted in improved contrast.

It was found advisable to filter the developer and fixing bath daily to remove suspended matter which might damage the emulsion. Some of the micro-radiographs were dried in a warm air blast, carefully avoiding overheating the emulsion. The rest of the micro-radiographs were dried in a conventional X-ray film dryer. When dry, the micro-radiographs were examined under the microscope and suitable areas photographed at various magnifications on ortho-commercial film. The best observations of the micro-radiograph are made directly with the microscope itself, rather than from a photographic print.

For simplicity, the experimental data and observations have been divided into two parts; the development of a micro-radiographic technique for gray iron and a metallurgical analysis of the micro-radiographs.

The pertinent data for the first phase of this discussion is contained in Table 1. The individual micro-radiographs appearing in Figs. 2 through 11 should be observed in conjunction with the data in Table 1.

For magnifications of 30 diameters, sufficient definition is obtained with spectrographic plate Type V, and relatively short exposure times are possible when this plate is used. As the examples show, it is possible to detect graphite, its distribution and dendritic structure. Definition suffers severely at magnifications of 100 diameters because of the grain of the emulsion. Plate Type 548 can be used for magnifications of 100 diameters without special care and plate Type 649 for magnifications as high as 300 diameters under the same conditions.

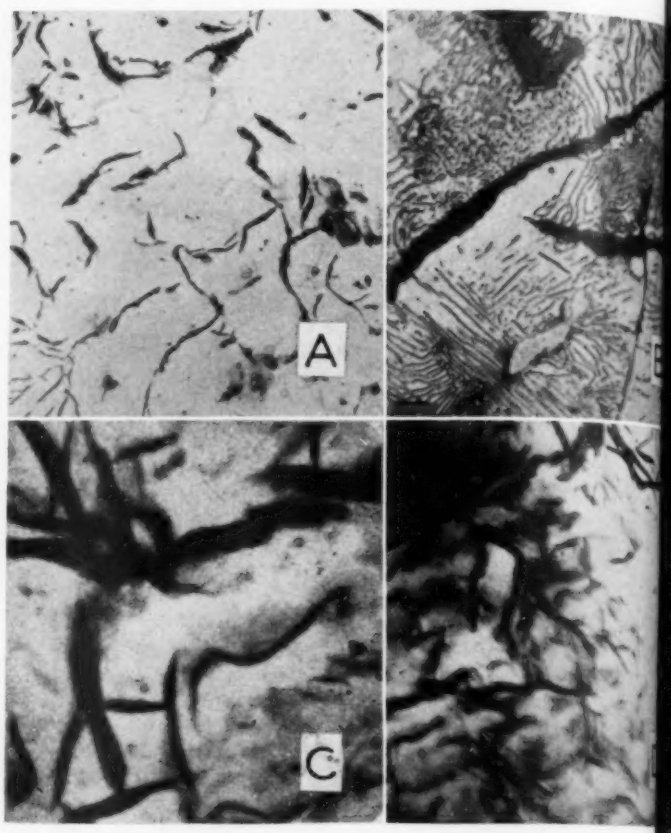


Fig. 2—A, photomicrograph unetched at 100×; B, etched, 500×; C, micro-radiograph at 30×; and D, at 100× of Specimen A, with 3.32% C, 1.70% Si, 0.51% Mn, 0.093% S, 0.20% P, balance, Fe.

Figure 11D is included to show the results of carelessness. The drying air blast may have been too hot, causing the mottled appearance. From the various micro-radiographs it is quite apparent that iron radiation provides higher contrast than tungsten radiation, at the expense of increased exposure time. Sufficient detail is obtained, nevertheless, from plate Type V and tungsten radiation with an exposure time of only 30 seconds.

Photomicrographs and micro-radiographs of the same specimens were made to permit comparisons of the two- and three-dimensional appearance of graphite.

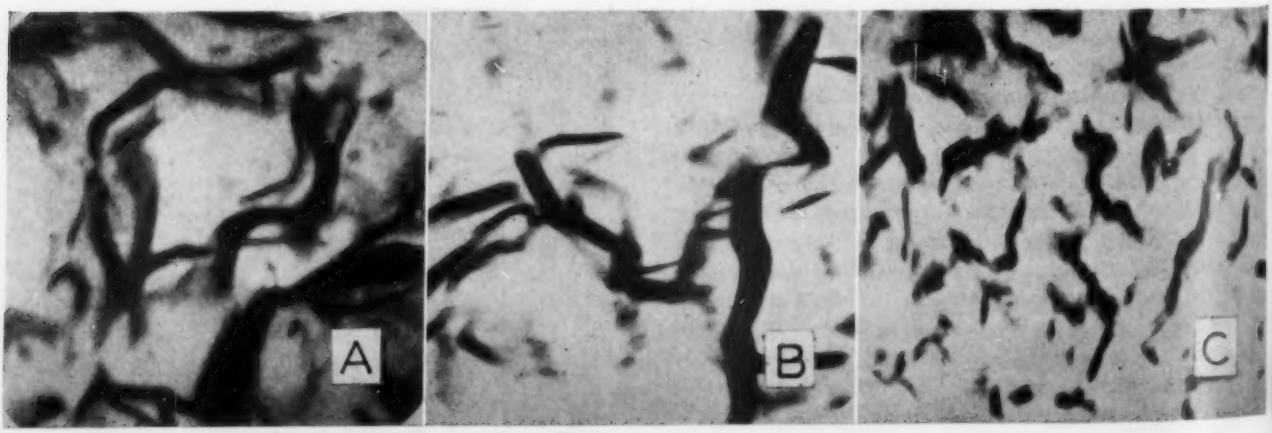


Fig. 3—Additional micro-radiographs of Sample A at 100×.

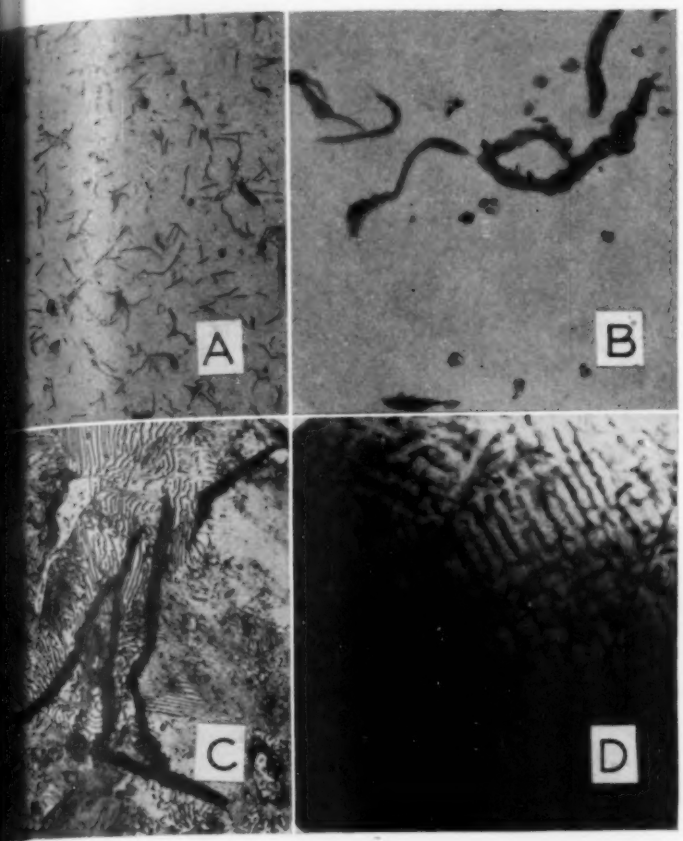
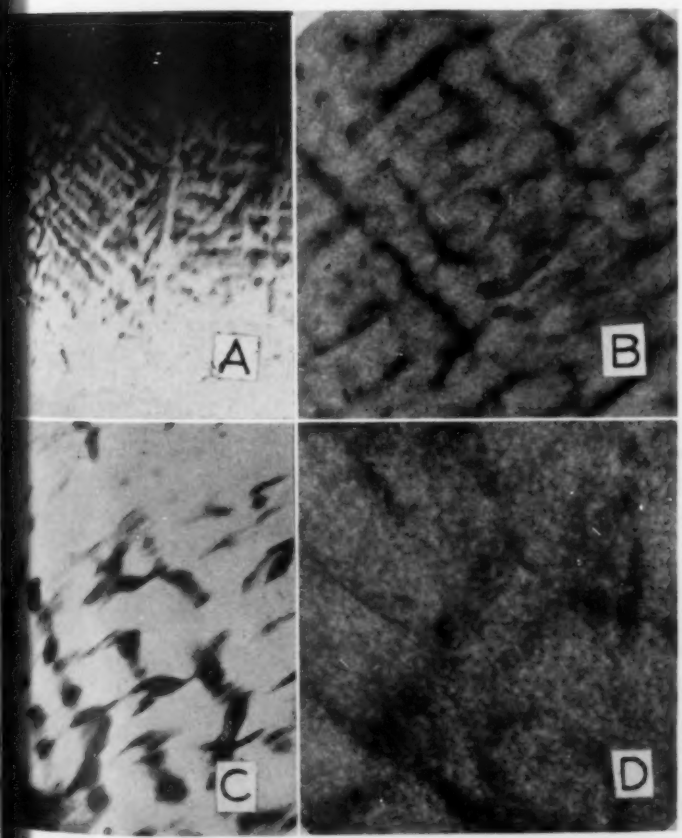


Fig. 4—A, photomicrograph, unetched at 50×; B, unetched at 500×; C, etched at 500×; and D, micro-radiograph at 30× of Specimen B. Fig. 5 (below)—Additional micro-radiographs at 100×.



Figures 2A and 2B are conventional photomicrographs of Specimen A, at 100 diameters unetched and 500 diameters etched, respectively. Specimen A is a pearlitic gray cast iron with a small amount of free ferrite, containing 3.32 per cent carbon, 1.70 per cent silicon, 0.51 per cent manganese, 0.093 per cent sulphur, 0.20 per cent phosphorus and Brinell Hardness Number of 153. The metal was poured at 2680 F into a green sand mold. The casting from which specimen A was obtained measured approximately 2 x 5 x 4 in.

Figure 2C is a micro-radiograph of the same specimen at 30 diameters. It is immediately apparent that the quantity of graphite visible in a specimen approximately 0.005 in. thick is greater than the amount observed in the single plane of a photomicrograph. The flakes in Fig. 2C are broader at 30 diameters than those in Fig. 2A at 100 diameters because the micro-radiograph reveals the true maximum width rather than the width exposed at some random plane which is prepared for a metallographic examination. In Fig. 2A the graphite varies considerably in width and presents a broken line on the surface whereas in Fig. 2C the graphite flake actually has a uniform width and is longer than indicated in a photomicrograph. These size, shape, and quantity characteristics of graphite are apparent in all the comparisons which follow. The crossing of graphite flakes in different planes is clearly evident in Fig. 2C. In Figs. 2D, 3A and 3B inclusive, the same area is shown at 100 diameters. (Fig. 2D is an enlargement of the area in the 5 o'clock position in Fig. 2C). It will be observed that the higher magnification permits more detailed study of the individual flakes. Figure 3C is another photograph at 100 diameters of the same plate shown in Fig. 2C. The photograph is underexposed and seems to be more sensitive to differences in thickness of the graphite flakes. The images of most of the flakes have a uniform density indicating a fairly consistent thickness throughout their length.

Figures 4A, 4B and 4C are photomicrographs of specimen B at 50 diameters unetched, 500 diameters unetched, and 500 diameters etched, respectively. Specimen B is a completely pearlitic gray iron containing 2.43 per cent carbon, 2.10 per cent silicon, 0.53 per cent manganese, 0.043 per cent sulphur, and 0.24 per cent phosphorus. The specimen was obtained from a standard arbitration bar, 1.2 in. in diameter, 18 in. long, poured at 2700 F into a vertical mold made of a synthetic sand mixture.

Close examination of Fig. 4A reveals a tendency of the graphite to follow a unique pattern. This condition is identified as Type E in the A.F.A. graphite standards. Previous investigators have theorized that this type of graphite outlines the original austenite grain boundaries. Figures 4D and 5A give a marked verification of this distribution by micro-radiography. The dendritic solidification of the metal and the interdendritic pattern of the graphite is clearly evident. The undesirability of this distribution from the standpoint of the many continuous planes of weakness is evident. Figure 5B shows this graphite enlarged 100 times. The variations in photographic density along the length of the graphite flakes may indicate changes in thickness; in some cases a portion of a flake may have

been removed in sample preparation.

Figures 5C, 5D, 6A and 6B are described in Table 1 and have been discussed in previous paragraphs. They are included to show the effects of changing the type of radiation, the voltage, and the type of plate. Figures 7A, 7B and 7C are photomicrographs of specimen C at 50 diameters unetched, 500 diameters unetched, and 500 diameters etched. This specimen was cut 1¼ in. from the tip of a wedge shaped casting in the area often identified as the "sooty" or "smoky" zone. The metal contained 2.91 per cent carbon, 1.99 per cent silicon, 0.50 per cent manganese, 0.044 per cent sulphur, and 0.26 per cent phosphorus.

The graphite in this iron may be identified as A.F.A. Type D—interdendritic, random orientation. A eutectic-appearing precipitation of fine flakes instead of individual flakes (as in Type E) seems to outline the original austenite grains. The micro-radiographs, Figs. 7D, 8A and 8B, however, show the dendritic development of the metal and the grains outlined with graphite. Figure 8C has been included to show the tree-like dendrites with graphite between the branches. From a comparison of Figs. 7D, 8A and 8B with Figs. 4D and 5A, the differences between type D and E graphite can be observed. The individual flakes of Type E are rather sharply defined whereas the finely divided type D graphite appears somewhat diffused.

Figures 9A, 9B and 9C are photomicrographs of specimen D at 50 diameters unetched, 500 diameters unetched, and 500 diameters etched. The sample was cut from a 1.2-in. diameter arbitration bar containing 3.07 per cent carbon, 2.20 per cent silicon, 0.84 per cent manganese, 0.111 per cent sulphur, 0.088 per cent phosphorus, 1.38 per cent nickel, 0.50 per cent molybdenum, and 0.65 per cent chromium. The graphite in Fig. 9A is a mixture of the grain boundary and random distribution types. Similar to specimen B, the dendritic development of the matrix is evident in the micro-radiographs (Figs. 9D to 10C). In these same figures, one can observe both random graphite and inter-

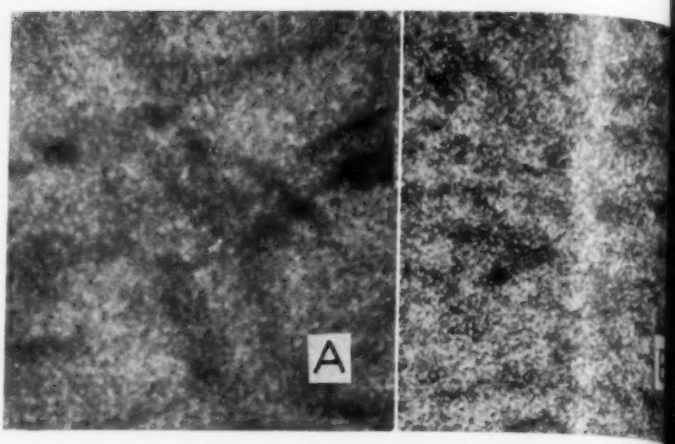
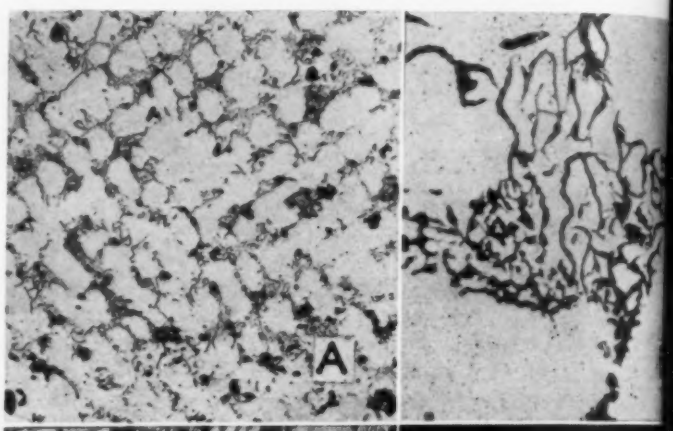


Fig. 6—Additional micro-radiographs of Sample B at 100×.



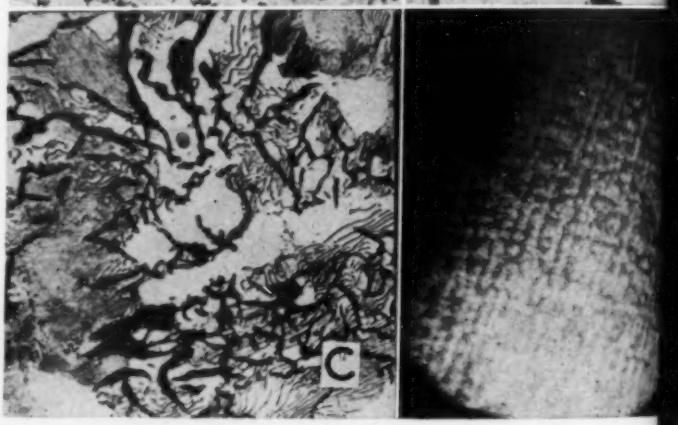
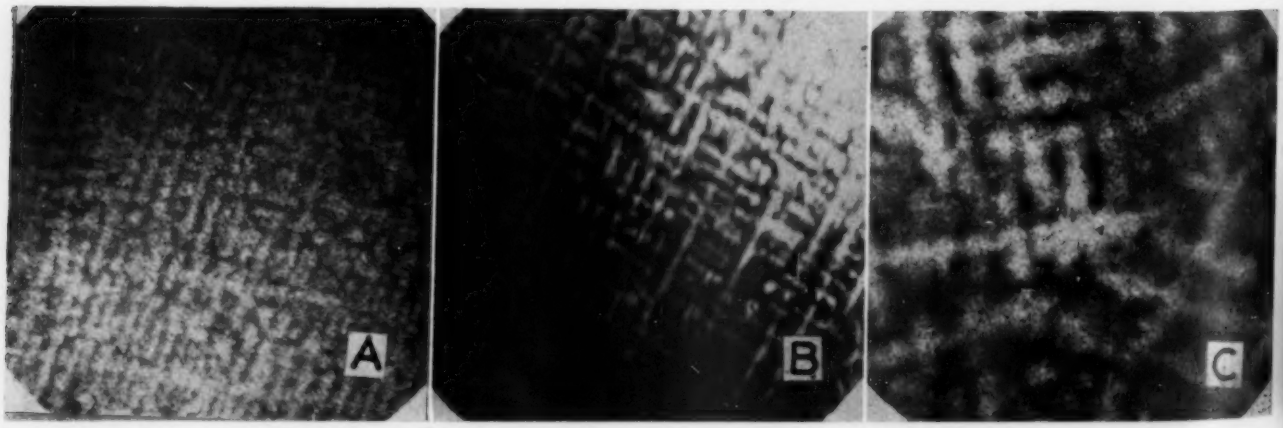


Fig. 7—A, photomicrograph unetched at 50×; B, unetched at 500×; C, etched at 500×; and D, micro-radiograph at 20× of Sample C with 2.91% C, 1.99% Si, 0.50% Mn, 0.044% S, 0.26% P, balance, Fe.

Additional micro-radiographs of Sample C; A and B at 30× and C at 100×.



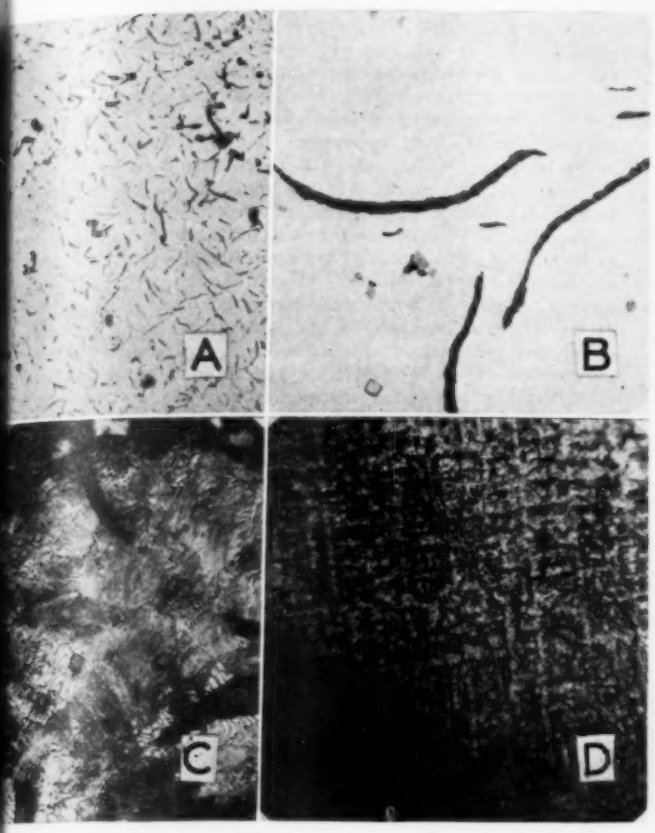


Fig. 9—A, photomicrograph, unetched at 50×; B, unetched at 500×; C, etched at 500×; and D, micro-radiograph at 30× of Sample D with 3.07% C, 2.20% Si, 0.84% Mn, 0.111% S, 0.088% P, 1.38% Ni, 0.50% Mo, 0.65% Cr, balance, Fe.

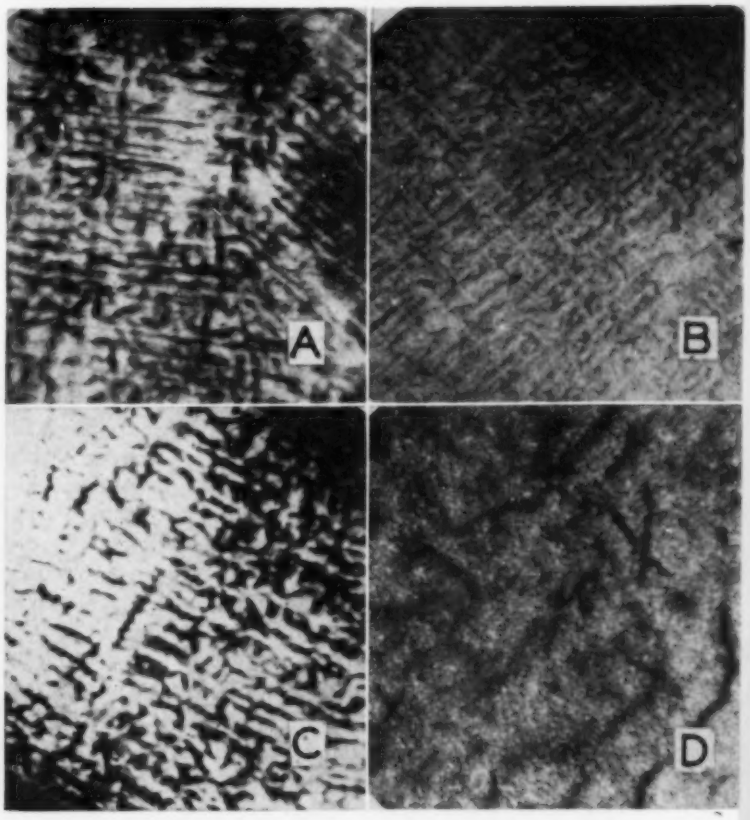
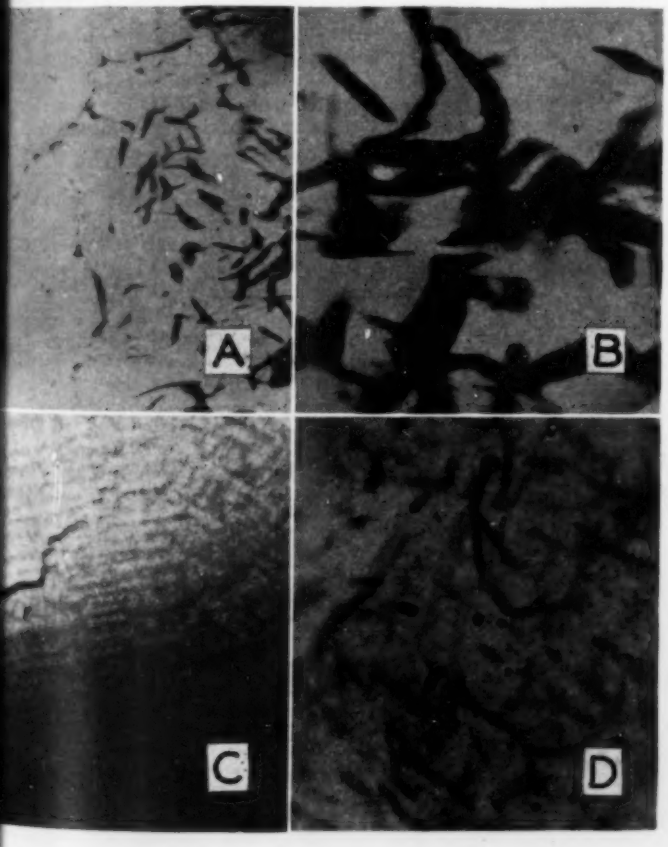


Fig. 10—Additional micro-radiographs of Sample D; A, B and C at 30×, D, at 100×.



dendritic graphite. The higher magnification and sharper contrast of Fig. 11A permits better observation of graphite. Figure 11B is the area located in the 5 o'clock position in Fig. 11A. The magnification of this micro-radiograph, Fig. 11B, is 300 diameters, the highest reproduced in this report. The changes in density can be noted along the length of some of the flakes. In Fig. 10D, which is at the same magnification as Fig. 11A, the matrix is obscured by the grain of the film but the random flakes are visible.

Figure 11C has been included to show a small crack detected by micro-radiography. The crack is definitely interdendritic since it travels along the arms of adjacent dendrites.

Although it is not within the scope of this report to present a detailed study of graphite, nevertheless some observations can be made. The dimensions of the flakes as indicated by photomicrographs are misleading. It has been clearly demonstrated that micro-radiographs are a means of showing the true size, shape, and orientation of graphite. In all instances the graphite is wider, longer, and more abundant than was previously assumed from photomicrographs. The width of flakes as measured on the micro-radiographs vary from 0.0001 to 0.002 in., while the photomicrographs show a maxi-

Fig. 11—Additional micro-radiographs of Sample D; A, at 100×, B, at 300×, C, at 30×, and D, at 100×.

mum width of only 0.0005 in. In Fig. 4D, the interdendritic graphite flakes appear to be half as wide as the dendrite arms. This is not disclosed by photomicrographs. Indeed, in the photomicrograph the interdendritic nature of these flakes is not even shown.

The dendritic development of the matrix during solidification is shown by several of the micro-radiographs. Since these dendrites lie at all angles in space, they are not as apparent in photomicrographs. Under the microscope, type D and E graphite appear to outline the original austenite grain but X-rays clearly reveal their consistent distribution between the arms of the dendrites. Evidently the low melting eutectic mixture is trapped in the dendritic interstices where it solidifies and rejects graphite simultaneously. The expansion of the graphite probably fills any interdendritic voids created by the shrinkage of the solidifying metal. Although all metals solidify in a dendritic system, it is not usually possible to observe the dendrites. Fortunately in gray iron, Type D and E graphite are precipitated between the arms of the dendrites and when revealed by micro-radiography, serve to outline the individual dendrites. When the graphite has a random distribution, it is impossible to observe this dendritic structure.

Summary and Conclusions

The graphitic structure in gray iron can be studied advantageously by the micro-radiographic techniques described in this report.

An industrial type tungsten-target X-ray tube is suitable for the study of graphite in gray iron, although better results may be obtained with an iron target tube.

With plate Type V, graphitic and dendritic structure may be detected and magnified at least 30 diameters in cast iron specimens measuring 0.005 in. thick by using tungsten radiation for 30 seconds at 60 kilovolts and 10 milliamperes at a distance of eight inches.

For higher magnification with good definition and better contrast the very slow, finer grain plates, Types 548 and 649 are recommended.

The micro-radiographs presented herewith show that graphite is actually wider, longer, more uniform in shape, and more abundant than shown in photomicrographs.

A.F.A. type D and E graphite lie between the arms of the dendrites and serve to outline the individual dendrites when observed by micro-radiography.

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2. J. T. MacKenzie, "Gray Iron-Steel Plus Graphite," AIME, Technical Publication No. 1741.

DISCUSSION

Chairman: V. A. CROSBY, Climax Molybdenum Corp., Detroit.
Co-Chairman: H. N. MYERS, Perfect Circle Corp., Hagerstown, Md.

MEMBER: Can you tell me the procedure used in making these microradiographs?

MR. VON BATCHELDER: The microradiographs were made on Eastman Spectrographic Plate 548, 649 and Type 5, depending upon the amount of magnification desired. We have gone up as high as 300 diameters. We are trying to go to higher magnification but are limited to the grain of the film.

J. T. MACKENZIE:[1] It seems to me, looking through the thick section, that you could not get as short a section as you get in the conventional photomicrograph. Take any flake—you have to cut it every time you take the conventional photomicrograph but you see the whole of it in the x-ray unless it lay outside the 0.005-in. thickness specimen. And that 0.005 in., if you could improve that to, say, 0.05 in., it would be a big help because the flakes of graphite, say anything like No. 2 3 or 4, would not be wholly in your section.

MR. SCHAUM: We realize that the technique can stand a lot of improving. The status of microradiography today is similar to that of x-ray examination of castings ten or twenty years ago. We have high hopes that three-dimensional x-ray pictures of graphite can be made by the stereoscopic system in which either the specimen or the tube is moved a short distance between exposures. The advantages of using a thicker specimen might be offset by the longer exposures required.

MR. VON BATCHELDER: Not only would you go to longer exposures but it would probably amount to exposure time in hours instead of minutes. Also, your structure may not be perfectly in alignment; it is randomly distributed throughout. With a 5/8-in. specimen you would not get a dendritic structure. You would get a total blackening effect similar to that of an industrial radiograph. There is a maximum thickness you can work with. It is our opinion that a specimen 0.005 in. thick is best from the standpoint of economy and pick up of graphite.

DR. MACKENZIE: You would have to consider the amount of graphite in the specimen. Of course, if you took a fine graphite specimen and made it 0.05 in. thick, you would have so many graphite flakes in that volume that you could not tell them apart. I was thinking of the slightly larger flakes which under 100 magnifications show about an inch, which is quite a commonly used grade of iron. That flake is at least 0.01 in. long as shown by the conventional photomicrograph, so you could not get more than half of the 0.005-in. thick specimen.

R. G. TOBEY:[2] Do the results at 300 diameters enlargement, secured with the Eastman Spectroscopic Plate 649, justify the longer exposure time as compared to 200 diameters enlargement available by use of the faster 548 emulsion?

MR. VON BATCHELDER: If you are really interested in higher magnification it does justify the excess exposure time required with the 649 plate. But in most cases, in this paper, on practically all the work we have done you can use the 548 plate, especially in a study of graphite, and get the results needed because you do not have to go to high magnifications. When you go to 200 or 300 diameters you cut down the field of view so much that all you see is a few graphite flakes presented there. You do not see the dendritic structure. But if you are really interested in high magnification, it would pay to go to the finest grain film obtainable.

CO-CHAIRMAN MYERS: I was wondering about the statement made that perhaps the flakes are somewhat thicker than we believe, thinking about a flake that might be inclined along the long axis, perhaps 2° or 5° from the normal beam of the x-ray. You have a tendency to pick up a considerably wider indication of the flake than the flake actually would be. If the flake would turn, say, 2° from the normal axis of the beam would it not show thicker than the actual graphite flake?

MR. SCHAUM: You are assuming that this flake is the shape of your hand. In some of these pictures the flakes seem to resemble the shape of an undulating worm. For that reason you often see on a photomicrograph a series of small flakes which originally were one long flake dipping above and below the plane of observation. If you have a flat flake, which dimension are you going to call the width? Since we cannot differentiate between the thickness and width, we can only say that we have revealed the maximum dimension of the flake in the particular orientation in which it is lying.

DR. MACKENZIE: You could probably develop a technique for that. An ordinary flake is shaped like a corn flake or curved blade. But it should show if you had everything under control. The flake you are looking at one way then would be denser than if you were looking at it in another way. The authors' technique is extremely new and novel and I certainly wish them well in it.

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[1] American Cast Iron Pipe Co., Birmingham, Ala.
[2] Eastman Kodak Co., Rochester, N. Y.

EFFECT OF COPPER ADDITION CONTAMINANTS ON MECHANICAL PROPERTIES OF GRAY CAST IRON

By
K. E. Rose * and C. H. Lorig **

THE USE OF COPPER AS AN ALLOYING ADDITION to cast iron needs no explanation. Because of its availability in grades of high purity it is customary to use pure copper shot or sheared wire for ladle additions, although any other convenient form may be equally suitable. Pound-for-pound credit may be allowed for the copper addition because no copper is lost and substantially no other elements are present.

This same complete recovery of copper would be expected from the addition of copper-base alloys. In many instances, copper-alloy scrap, purchased for its copper content alone, might compete economically with high-purity copper shot. Accordingly, a series of experiments was undertaken to determine what harmful effects might result from use of copper-base alloys and the extent to which they might be safely employed as a source of copper for gray cast iron.

Elements in Copper-Base Alloys

A survey of the field of alloys indicated that only nine elements, other than copper, are found in the bulk of copper-base alloys. A tenth, bismuth, might be introduced in bearing metals, and is sometimes intentionally added to cast iron, so it was included with the group shown in Table 1 used for this investigation.

The effect of most of these elements, when added to cast iron, has been studied by other investigators, and is reported in various technical papers. It was expected that the individual effects with or without copper would be similar. However, the effect of one element in the presence of others (in addition to copper) or the interaction of two or more elements was not known. These interaction effects are sometimes pronounced as in the hardenability of steel where small amounts of several elements approximate the effect of larger amounts of fewer elements. Since scrap alloy copper is likely to contain more than two elements, attention was directed to the interactions as well as to the main effects of single elements.

Two factorially designed [1, 2] experiments were drawn up to explore the main effects and interactions as the first step in the program. The factorially designed experiment departs from the conventional practice of considering only one variable at a time and provides data which can be easily treated by statistical analysis to determine the probable reliability of the results indicated. Furthermore, the interactions and the main effects may be determined with equal precision because the same number of separate determinations are involved in each.

In this application, the ten elements were divided into two groups of five each. Two percentage levels were then chosen for each element. The higher level was set at some point where the effect would be noticeable, yet not so high as to preclude the presence of other elements at a proportionately high level. The lower percentage level was taken as one-tenth of the higher level; the purpose being to have a measurable, yet presumably ineffective amount of the element in question.

Factorial Test Percentage Levels

Table 2 shows the distribution and percentage levels for the two factorial tests. Thirty-two combinations or "compositions" are possible from each set of five elements at two percentage levels, sixteen with each element at its higher and sixteen with each element at its lower level.

Iron for the tests was prepared in a 27-in. cupola equipped with an air-volume control, a pressure gauge, and a blast-moisture control. The melting practice was kept as consistent as possible during all of the test runs. An effort was made to keep the analysis of the iron at 3.30 per cent carbon, 1.90 per cent silicon, and

TABLE 1.—TEN ELEMENTS REPRESENTATIVE OF THOSE USED IN MOST COPPER-BASE ALLOYS

Element	Normal Maximum Percentage
Aluminum	10.0
Arsenic	0.5
Antimony	0.5
Beryllium	4.0
Bismuth	2.0 (assumed)
Cadmium	1.0
Lead	30.0
Tellurium	0.5
Tin	10.0
Zinc	40.0

* Formerly Research Engineer, Battelle Memorial Institute. Now Chairman, Department of Mining and Metallurgical Engineering, University of Kansas, Lawrence, Kansas.
** Supervisor, Process Metallurgy Division, Battelle Memorial Institute, Columbus, Ohio.

0.75 per cent manganese, although there was some variation in the actual analysis.

Twelve-hundred pounds of iron were tapped into an insulated holding ladle to provide the base iron for the tests. This was moved to the molding floor where 40-lb. lots, each treated separately, were taken in hand ladles and used to pour a two-bar 1.2 x 21-in. transverse-bar mold and a chill test. The alloy additions were made to the hand ladle as they were filled from the holding ladle.

Since a single holding ladle would provide only enough iron to pour 20 molds, it was necessary to use two ladles for the complete set of 32 tests. The pouring order was arranged so that only the fourth-order interaction was affected, but it was rendered useless.

The tests were made in duplicate, providing four transverse bars and two chill tests for each composition. In determining the main effects and interactions, the results of 64 determinations are involved in the transverse strength, resilience, impact strength, and deflection tests, and 32 determinations in the chill test.

A measure of the reliability of the tests was made by an analysis of variance, using the differences between molds of the same composition as a basis for determining the inherent experimental error. If the effect attributable to composition is greater than the factor representing experimental error, the reason may be either:

a. The apparent effect was achieved through sheer accident.

b. The effect is actually the result of the change in composition.

The analysis of variance provides a measure of the significance of the data in terms of the probability that the results are accidental. Thus, a significance value of 0.1 means that the chances are 1 in 10 that the indicated effect was arrived at by accident; a value of 0.001 means significance of the data in terms of the probability that the chances are only 1 in 1000.

Tables 3 and 4 show the results of the analysis of variance carried out on the data from the two factorial experiments. Tabulation of the data for the analyses summarized in these tables was accomplished by subtracting the property values of the compositions with

TABLE 2.—PERCENTAGE LEVELS OF TEN ELEMENTS ADDED WITH COPPER * IN FACTORIALLY DESIGNED EXPERIMENTS

Element	Higher Level	Lower Level
Set I. Elements More Likely to be Found		
Antimony	0.002	0.0002
Arsenic	0.002	0.0002
Lead	0.033	0.0033
Tin	0.03	0.003
Zinc	0.033	0.0033
Set II. Elements Less Likely to be Found		
Aluminum	0.03	0.003
Beryllium	0.008	0.0008
Bismuth	0.002	0.0002
Cadmium	0.002	0.0002
Tellurium	0.001	0.0001

* Regardless of the percentage of other elements added, the alloy addition was balanced to provide an addition of one per cent of copper.

TABLE 3.—SIGNIFICANCE OF MAIN EFFECTS AND INTERACTIONS FROM THE ADDITION OF ARSENIC, ANTIMONY, LEAD, TIN, AND ZINC WITH COPPER TO GRAY IRON

	Transverse Strength		Deflection		Resilience		Impact Strength		Chill Depth	
Elements	Effect Total (x 0.1)	Significance	Effect Total (x 1000)	Significance	Effect Total (x 10)	Significance	Effect Total (x 10)	Significance	Effect Total (x 100)	Significance
As	140		−97		107		145		−127	0.2
Pb	−2386	0.01	−2539	0.01	−5433	0.01	−2335	0.01	287	
Sb	−252		−119		−423		−765	0.1	39	
Sn	−140		9		25		−555		1	
Zn	−324		−173		−421		155		135	
As-Pb	84		95		195		−265		19	
As-Sb	50		131		285		185		−69	
As-Sn	−86		131		−103		115		−83	
As-Zn	258		393		859	0.2	−555		−53	
Pb-Sb	−140		−195		−295		−335		−31	
Pb-Sn	−196		−203		−291		95		47	
Pb-Zn	120		−73		−45		305		69	
Sb-Sn	58		81		115		245		111	
Sb-Zn	26		15		61		−5		−95	
Sn-Zn	10		23		137		−615		3	
As-Pb-Sb	110		19		73		−145		−135	
As-Pb-Sn	270		259		569		245		−89	
As-Pb-Zn	−246		−431		−825	0.2	−245		−51	
As-Sb-Sn	−188		−57		−289		−325		31	
As-Sb-Zn	−40		−7		−39		5		−95	
As-Sn-Zn	−148		−23		−231		−165		−21	
Pb-Sb-Sn	22		421		579		375		113	
Pb-Sb-Zn	26		27		−95		−135		−181	
Pb-Sn-Zn	−38		11		9		−285		49	
Sb-Sn-Zn	172		107		343		385		−123	
As-Pb-Sb-Sn	44		111		163		−35		−11	
As-Pb-Sb-Zn	152		377		697		−5		−73	
As-Sb-Sn-Zn	122		1		35		635		113	
As-Pb-Sn-Zn	264		345		501		125		−147	
Pb-Sb-Sn-Zn	−96		47		−13		−285		143	
Experimental error — based on standard deviation between duplicate molds (corrected for ladle differences)	±170 lb./bar		±0.04 in.		±5 ft.lb./bar		±3+ ft.lb./bar		±0.2 in.	

the element or elements at the lower percentage level from those of the corresponding compositions in which the same element or elements are at the higher percentage level. In the factorial design employed in these analyses, there were sixteen differences so obtained for each main effect and each interaction, when the elements were run in duplicate, there were two sets of sixteen values for the chill tests and 32 values for the other tests. The 'effect totals' cited in the tables were obtained by subtracting the sum of all 32 values for the chill test and all 64 values for the other tests for each main effect and each interaction, when the element or elements were at the lower percentage level, Table 2, from the sum of the same number of values when the element or elements were at the higher percentage level, Table 2. The analysis of variance permitted the consideration of masses of data without the need of recording the individual values.

The fact that only a few of the total number of main effects and interactions are significant does not necessarily mean that the elements have no effect. It means only that the experimental error was not small enough to permit the effects to assert themselves.

Only lead and bismuth showed consistently significant results. A few other main effects and interactions showed significance in some properties, but except for Te and Al-Te the significance values are not strong. Furthermore, most of the interactions showing significant values contain one of the elements shown to be harmful, lead or bismuth.

In view of this, it does not appear that the interactions are important in themselves, but that the presence of one potent element may mask the effect of others.

Actually, the interactions may be more beneficial than harmful. If it can be assumed that the effect totals are qualitatively correct, Table 5 indicates that increasing the number of elements added with the copper tends to decrease the harmful effects.

In addition to the two factorially designed experiments which have just been described, a large number of tests were made with only one element at a time added with copper in order to determine the amount that might be tolerated. The percentages of the elements added individually to the iron with 1 per cent of copper are listed in Table 6, and the results of the tests are summarized in the graphs, Fig. 1 to 10, inclusive, and the discussions for the individual elements.

Table 5.—Percentages of Beneficial Results Shown in Effect Totals for Factorial Experiments

Number of Elements Added With Copper	Number of Beneficial Effects	Number of Harmful Effects	Per Cent Beneficial Effects
		Set No. I	
One	7	18	27
Two	30	20	60
Three	27	23	54
Four	18	7	72
		Set No. II	
One	3	22	12
Two	26	22	52
Three	12	38	24
Four	11	14	44

Table 4.—Significance of Main Effects and Interactions from the Addition of Aluminum, Beryllium, Bismuth, Cadmium, and Tellurium with Copper to Gray Iron

	Transverse Strength		Deflection		Resilience		Impact Strength		Chill Depth	
Elements	Effect Total (x 0.1)	Significance	Effect Total (x 1000)	Significance	Effect Total (x 10)	Significance	Effect Total (x 10)	Significance	Effect Total (x 100)	Significance
Al	22		−149		−138		−140		12	
Be	−26		−363		−468		−640		−16	
Bi	−562	0.01	−1017	0.01	−2182	0.01	−820	0.1	278	0.1
Cd	−124		−445		−702		−20		32	0.01
Te	−306		−367		−1164	0.1	770	0.1	514	
Al-Be	−144		−139		78		−280		−42	
Al-Bi	216		247		692		320		−48	
Al-Cd	−2		−37		260		0		10	
Al-Te	212		93		442		1570	0.01	0	
Be-Bi	−292		−311		−822		−460		12	
Be-Cd	−274		−359		−730		−120		−58	
Be-Te	−64		−17		−224		290		−44	
Bi-Cd	18		55		136		−160		−108	
Bi-Te	180		357		618		−130		170	
Cd-Te	82		165		34		230		56	
Al-Be-Bi	−342	0.1	−451		−788		−260		70	
Al-Be-Cd	−92		−243		−208		−80		−96	
Al-Be-Te	−206		−209		−498		−370		10	
Al-Bi-Cd	136		163		586		−500		−6	
Al-Bi-Te	−42		−331		−304		−250		12	
Al-Cd-Te	−236		−379		−824		−450		−46	
Be-Bi-Cd	−452	0.05	−519	0.1	−1292	0.1	−420	0.1	102	
Be-Bi-Te	−146		−145		−450		470		80	
Be-Cd-Te	−336	0.1	−341		−902		−470		−90	
Bi-Cd-Te	264		605	0.1	904		290		28	
Al-Be-Bi-Cd	150		−255		134		−300		180	
Al-Be-Bi-Te	−220		−29		−284		50		50	
Al-Be-Cd-Te	158		503		1000		230		−48	
Al-Bi-Cd-Te	−42		−159		−346		−750		10	
Be-Bi-Cd-Te	−2		143		128		−210		−26	
Experimental error — based on standard deviation between duplicate molds	±170 lb./bar		±0.03 in.		±6 ft.lb./bar		±4 ft.lb./bar		±0.13 in.	

TABLE 6.—QUANTITIES OF CONTAMINANTS ADDED INDIVIDUALLY TO GRAY IRON

Element	Test Run No.	Per Cent Added to Iron With 1 Per Cent Copper
Al	56	0.1
	59	0.001, 0.01, 0.05, 02
As	55	0.005
	57	0.001
	58	0.01
	59	0.0005, 0.01
Be	56	0.04
	59	0.004, 0.02
Bi	56	0.02
	59	0.0001, 0.001, 0.01, 0.1
Cd	56	0.01
	59	0.0001, 0.0006, 0.001, 0.006
Pb	55	0.1
	57	0.005, 0.010, 0.020, 0.030
	58	0.04—held for time effect
	59	0.0001, 0.001, 0.01, 0.025, 0.05, 0.075, 0.1, 0.2
Sb	55	0.005
	57	0.001
	58	0.01
	59	0.0005, 0.01
Sn	55	0.1
	58	0.02
	59	0.001, 0.01, 0.1, 0.2
Te	56	0.005
	59	0.0005, 0.001, 0.0025
Zn	55	0.1
	57	0.005, 0.010, 0.020, 0.030
	58	0.04—held for time effect
	59	0.001, 0.01, 0.025, 0.05, 0.075, 0.2

Also, a few commercial copper alloys were used in order to compare their effects with those of the synthetic alloys.

Since the test data were obtained from several different base irons, no two of which had identical chemical or physical properties, it appeared advisable to express the effects of the various additions in terms of the percentage of change in properties from those of the base iron with 1 per cent copper alone. These percentile values were then plotted in Figs. 1 to 10, inclusive, and examined for trends.

The results of the two factorially designed experiments conducted in duplicate, appear in the figures as crosses joined by broken lines, while those of experiments in which the elements were added individually appear as solid circles with trends shown by solid lines. Other variations imposed in the tests, such as using inoculants, are indicated on the figures.

Experimental Error

Some caution should be exercised in interpreting the curves. Where the factorial tests indicate that the experimental error is greater than the effect of the addition, the slope of the curve is, at best, only an estimate. Furthermore, the experimental error is often greater than the maximum effect indicated by the curves for the entire percentage range covered by the elements added individually.

In testing the irons, the arbitration bars were first sand blasted lightly. They were then broken by transverse loading on 18-in. centers in accordance with ASTM specifications. Bars were placed on the machine with the cope side up, and deflections were determined with an autographic deflection gauge. Impact tests were made on the ungated half of the bars from the transverse test using a six-in. span and an energy input of 74 ft.-lb.

The inner halves of the broken impact specimens were machined to provide tensile specimens, with button ends to fit the grips of the tensile testing machine and with a reduced section of 0.750 in. in diameter. Sections from these pieces 0.75 in. long were also used for the hardness tests. Depth of chill was determined by measuring the white zone on the broken chill test. Resilience was calculated from the area underneath the transverse load-deflection curve.

Effect of Aluminum and Copper. The data represented in Fig. 1 indicate that the effect of raising the aluminum content from 0.003 to 0.03 per cent in the iron was within the limits of the experimental error and, therefore, is not significant. In fact, no appreciable effect occurred until the aluminum addition approached 0.1 per cent. A greater effect resulted from the 0.2 per cent addition, but such a large addition would not be obtained by using a commercial copper-base alloy in the charges.

Aluminum Addition Functions

It may be inferred that all additions of aluminum within the range of percentages shown are beneficial because the tensile, transverse, and impact strengths, deflection, and resilience are above the corresponding values for the base iron with copper only. The Brinell hardness is relatively unaltered, but the chill depth is cut about one-half.

Many investigators have reported beneficial results from the use of small additions of aluminum. Aluminum is generally considered to be from one-third to one-half as effective as silicon as a graphitizer. It may also function as a deoxidizer.

Massari [3] recommends that aluminum should not be added in excess of 0.1 per cent because of the danger of formation of pinholes. Smith and Aufderhaar [4] likewise suggest that aluminum be kept 0.1 per cent when used as a softener or deoxidizer, and below 0.5 per cent when used as a graphitizer. Ploye [5] reported that 0.01 to 0.02 per cent additions of aluminum increased the fluidity of gray iron, probably by reducing the oxygen content.

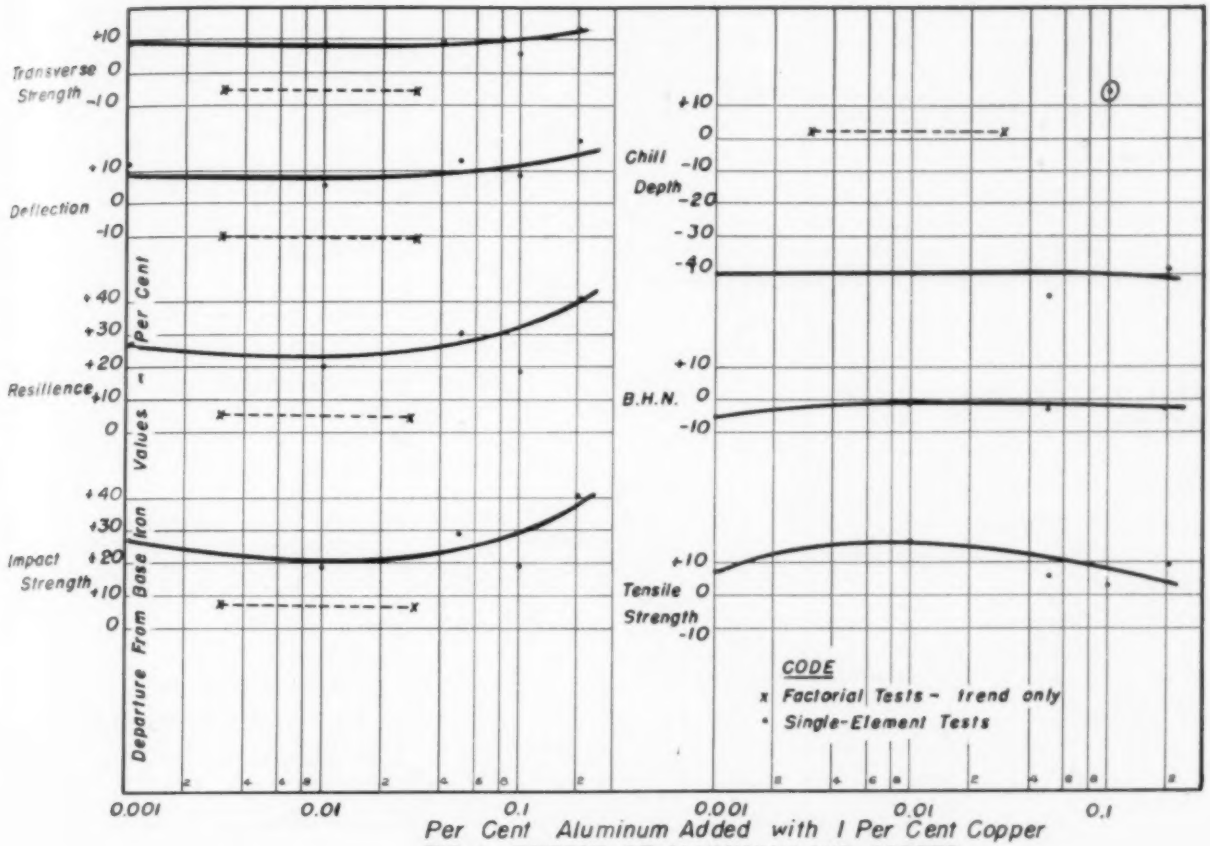
Antimony is a normal component of most pig irons and can generally be found in cast iron as a tramp element. It is not often added deliberately.

Spencer and Walding [6] found that antimony lowered both the transverse and impact strengths and increased the hardness of cast iron.

The curves shown in Fig. 2 conform to the findings of Spencer and Walding, although the percentages of antimony used are much smaller. Significance of the downward trend for impact strength was indicated in the analysis of variance.

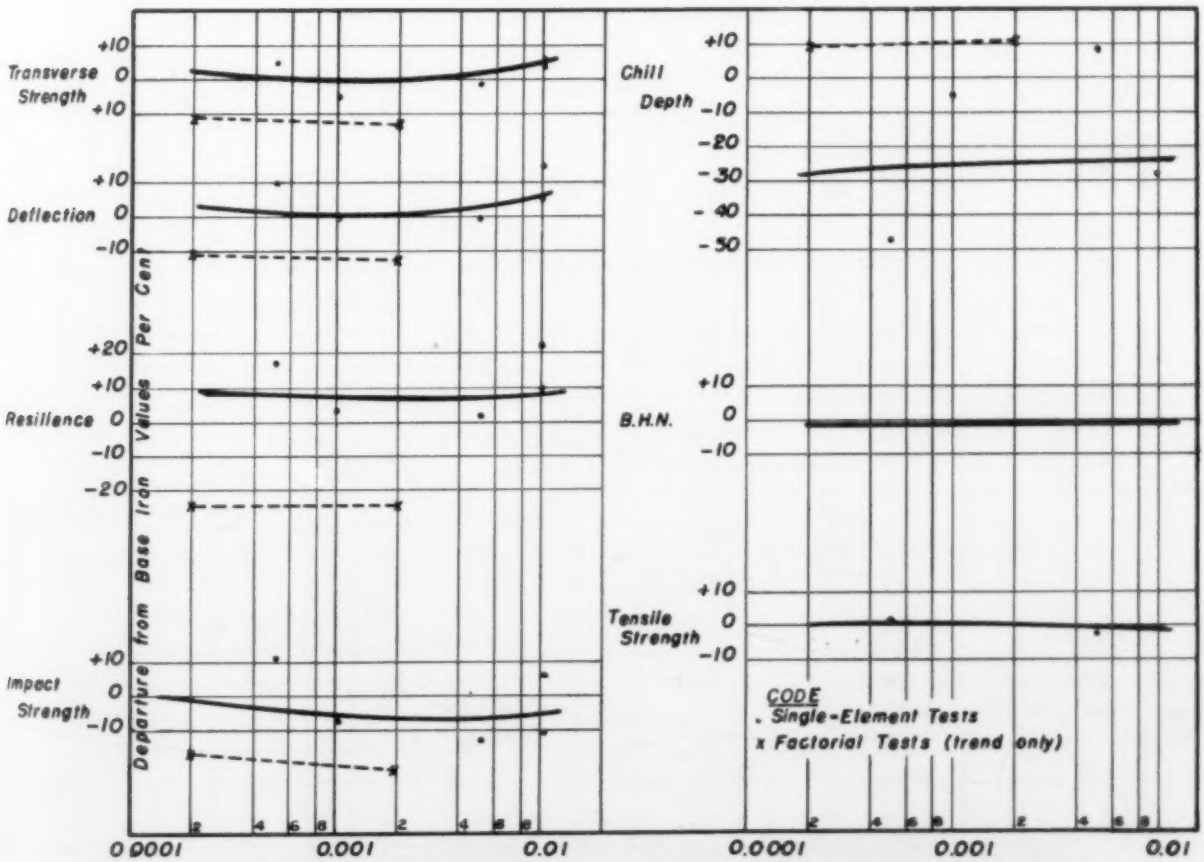
Antimony cannot be considered beneficial, in the light of these data, but the percentages likely to be introduced from copper alloy are small.

Like antimony, arsenic is found in small amounts in pig iron. Spencer and Walding, Smith and Aufderhaar, Hurst,[7] and others believe that arsenic causes no bad effects up to 0.1 per cent in gray iron. This is well



Per Cent Aluminum Added with 1 Per Cent Copper

FIG. 1 - EFFECT OF ALUMINUM AND COPPER.



Per Cent Antimony Added with 1 Per Cent Copper

FIG. 2 - EFFECT OF ANTIMONY AND COPPER.

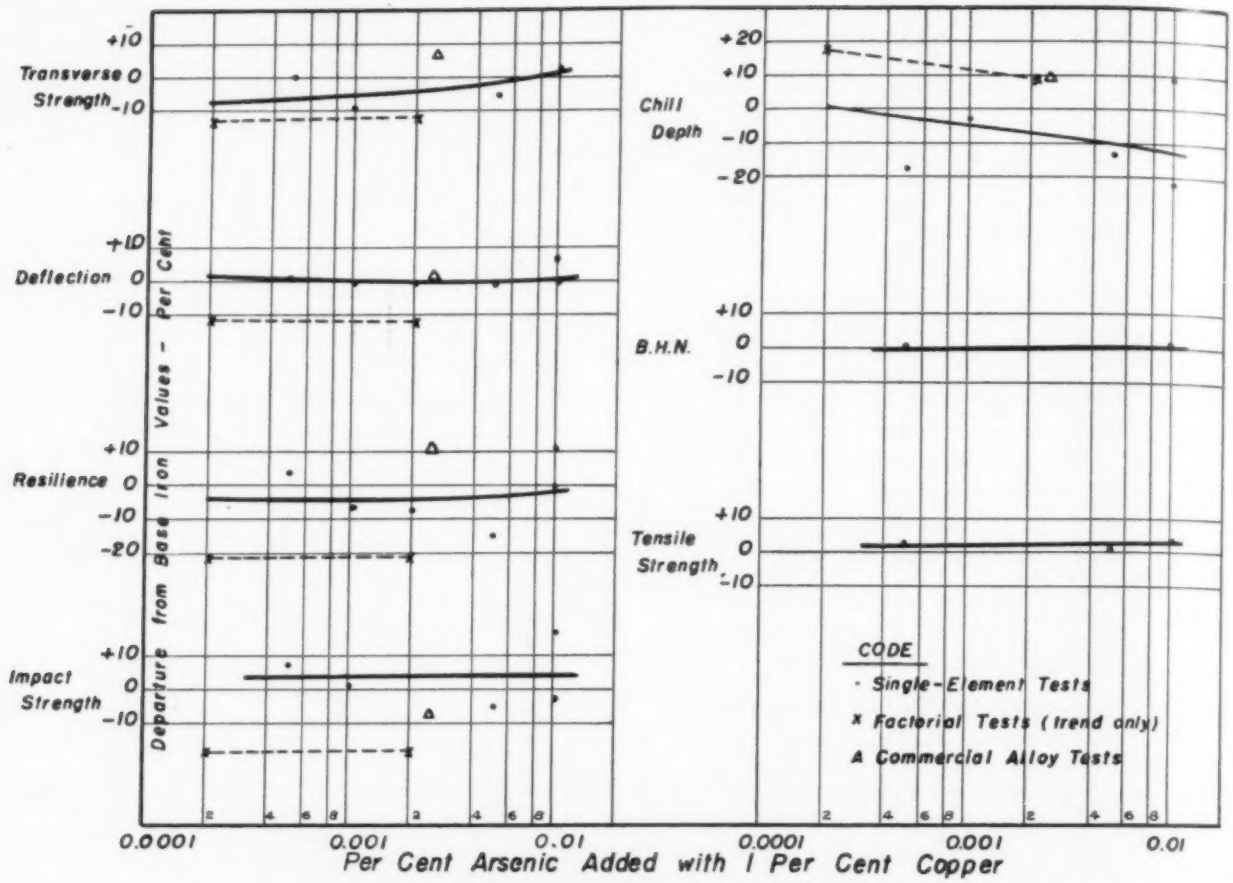


FIG. 3 - EFFECT OF ARSENIC AND COPPER.

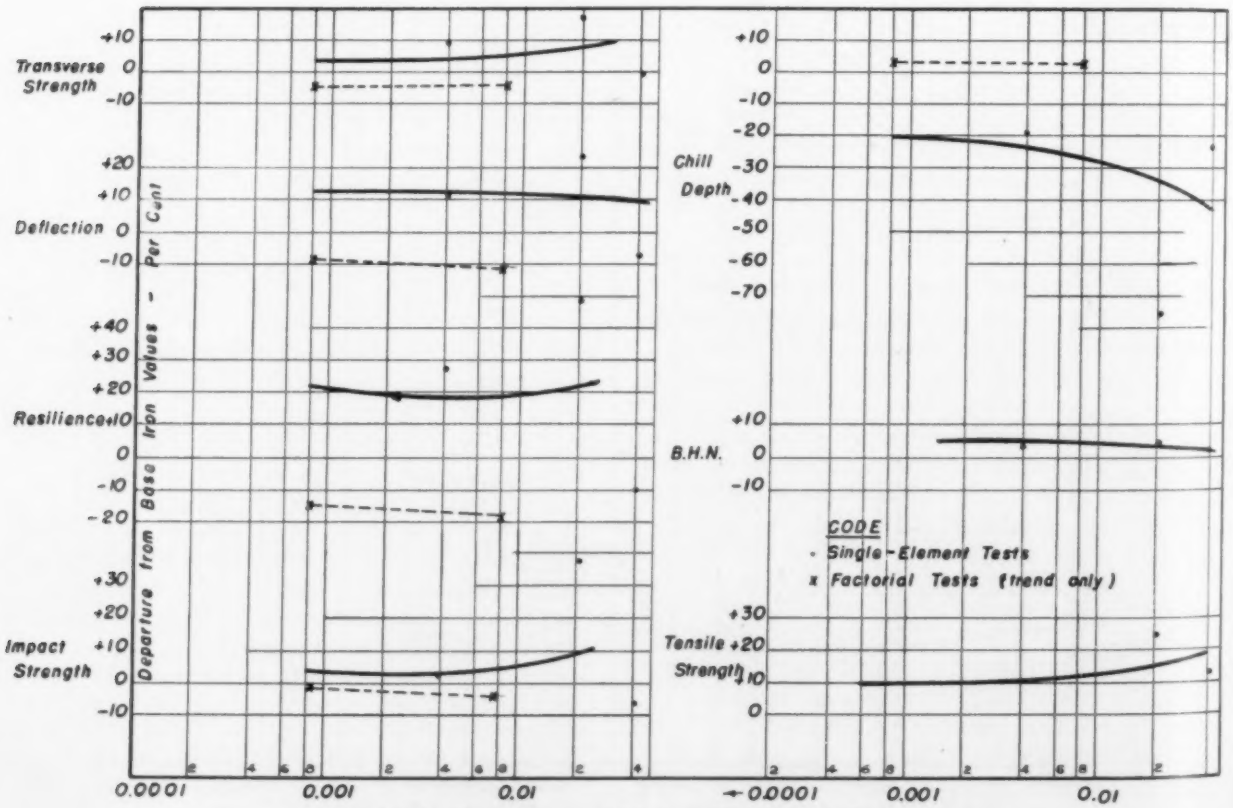


FIG. 4 - EFFECT OF BERYLLIUM AND COPPER.

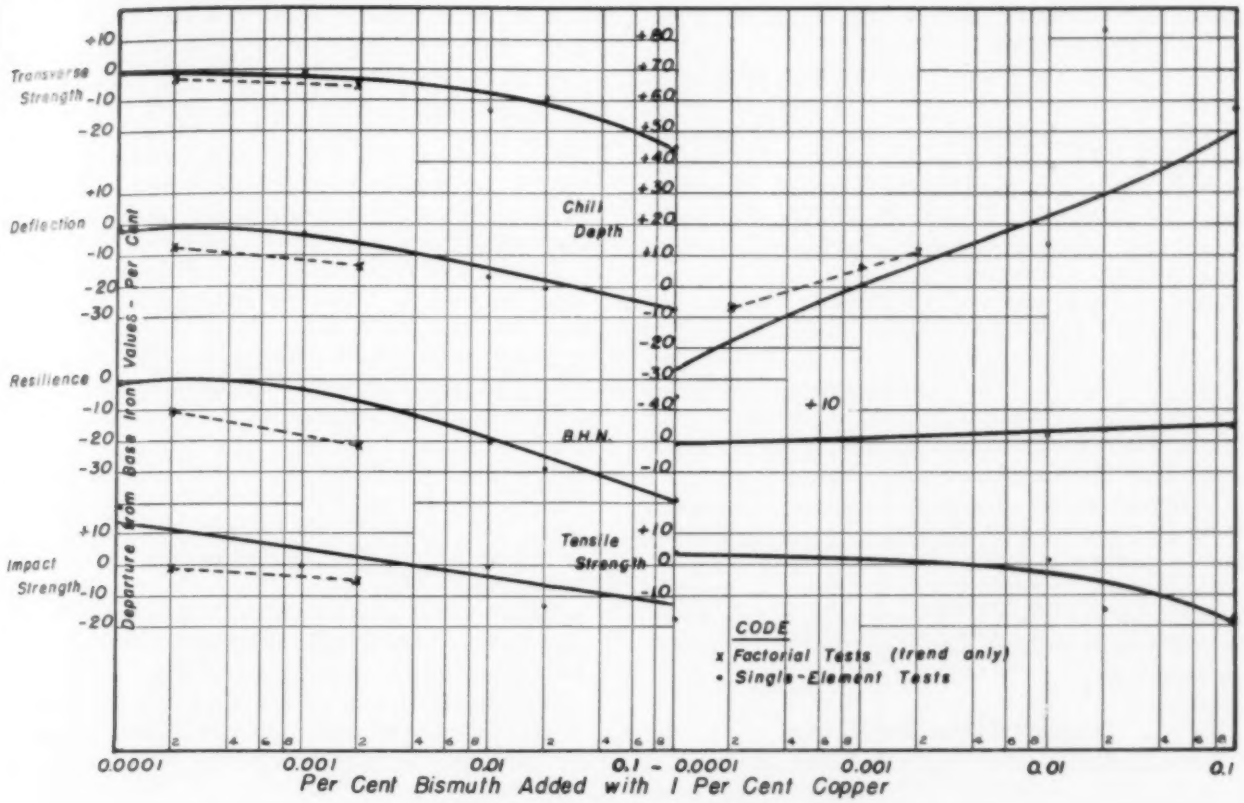


FIG. 5 - EFFECT OF BISMUTH AND COPPER.

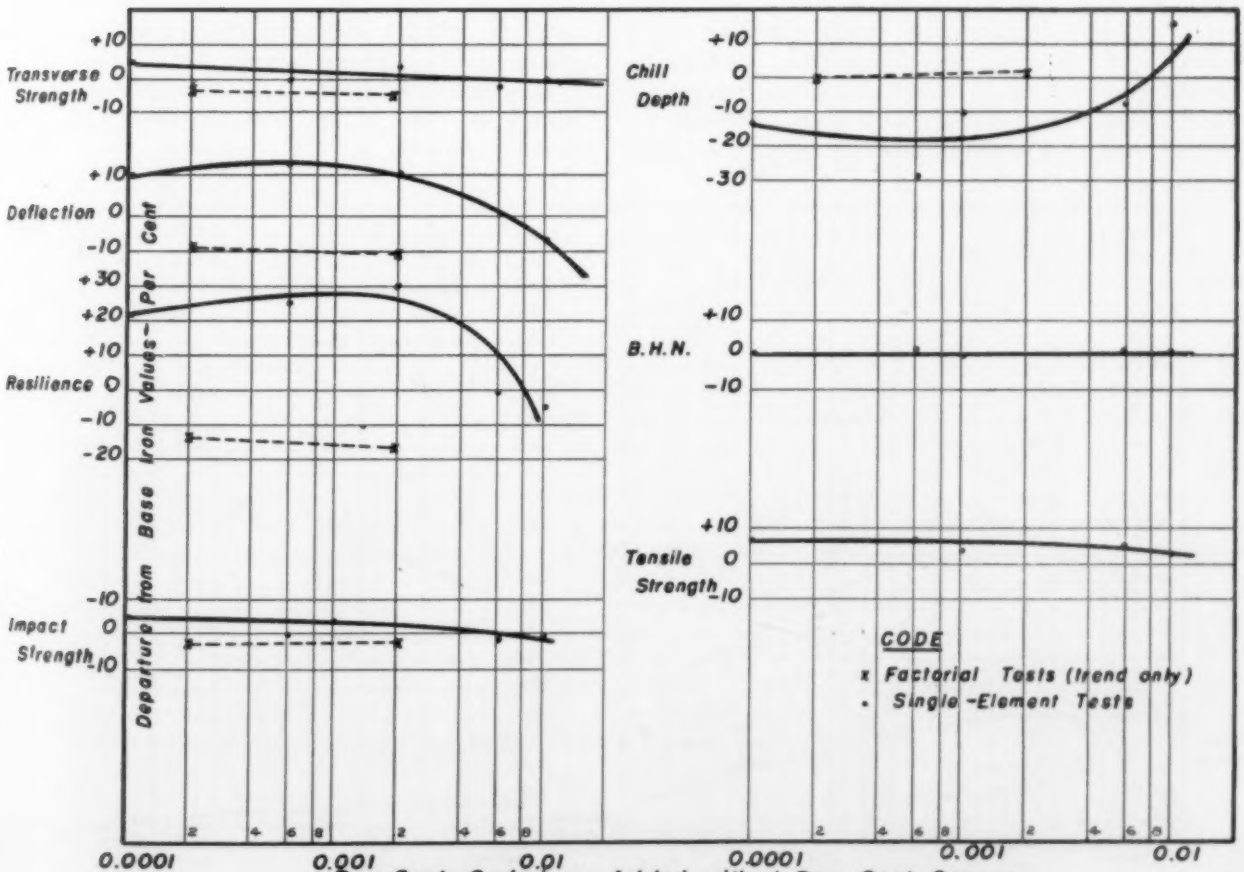


FIG. 6 - EFFECT OF CADMIUM AND COPPER.

outside the maximum amount that would be introducted by copper alloys in the melt.

The curves in Fig. 3 show no appreciable effect from the addition of arsenical copper. There appears to be a distinct trend toward a decrease in chill with an increase in the amount of arsenic added, which is contrary to the reports of other investigators. However, the entire range of the points shown in Fig. 3 is within the limits of experimental error, which are fairly wide for a chill test of this type.

Beryllium is effective as a deoxidizer, and it seems unlikely that the amounts represented in Fig. 4 would be sufficient to do more than serve as a deoxidizer. No significant effect was detected when the percentage of beryllium was increased from 0.0008 to 0.008 per cent, although the beryllium-treated iron appeared to be better than the iron with copper alone.

There is some indication that additions of beryllium above 0.01 per cent are decidedly beneficial, but the data are not conclusive.

In summary, it appears that no harm would come from the use of beryllium copper as a source of copper, but the results probably would not justify the cost of the beryllium.

Mechanical Properties Affected

Smith and Aufderhaar [4] reported that additions of bismuth increased the "life" of molten gray iron. However, the mechanical properties suffered greatly, although virtually no residual bismuth could be found in the iron.

Figure 5 shows effects of adding bismuth (with copper) in quantities ranging from 0.0001 to 0.1 per cent. Results of the factorial test indicate that the effects represented by the slope of the dotted lines are probably quantitatively correct. This corresponds to a decrease in transverse and impact strength, resilience, and deflection, and to an increase in the chill depth. The tensile strength also decreases as the bismuth content of the metal is raised.

Bismuth may be harmless and perhaps even beneficial if no more than 0.001 per cent is added. Quantities in excess of this amount will undoubtedly affect the physical properties of the iron adversely. In small amounts, bismuth may improve the iron by serving as a deoxidant, as suggested by Chubb.[8]

Spencer and Walding [6] reported that cadmium additions lowered the transverse and impact strengths of cast iron, and raised the hardness.

These conclusions are confirmed by the data represented in Fig. 6. None of the factorial tests showed mathematical significance, but the trends are consistent.

It would appear from Fig. 6 that cadmium does not become harmful until the addition exceeds 0.005 per cent. This is one-half the amount that would be obtained by adding the conventional one per cent cadmium-copper alloy.

Lead appears to be a very potent element so far as its effect on gray iron is concerned, in spite of the fact that none was found in the lead-treated iron.

The data from which Fig. 7 was drawn point conclusively to the harmful effects of lead. The trends shown by the slopes of the dotted lines were statistically significant.

The curves indicate that additions of lead not exceeding 0.005 per cent (0.01 lb. per ton) are probably harmless. When lead is intentionally added to copper-base alloys, even to improve the machinability, it exceeds this amount, and most leaded copper alloys are considerably richer in lead.

Throughout all the tests in which lead was a component, the effect of lead masks the effect of the other elements present.

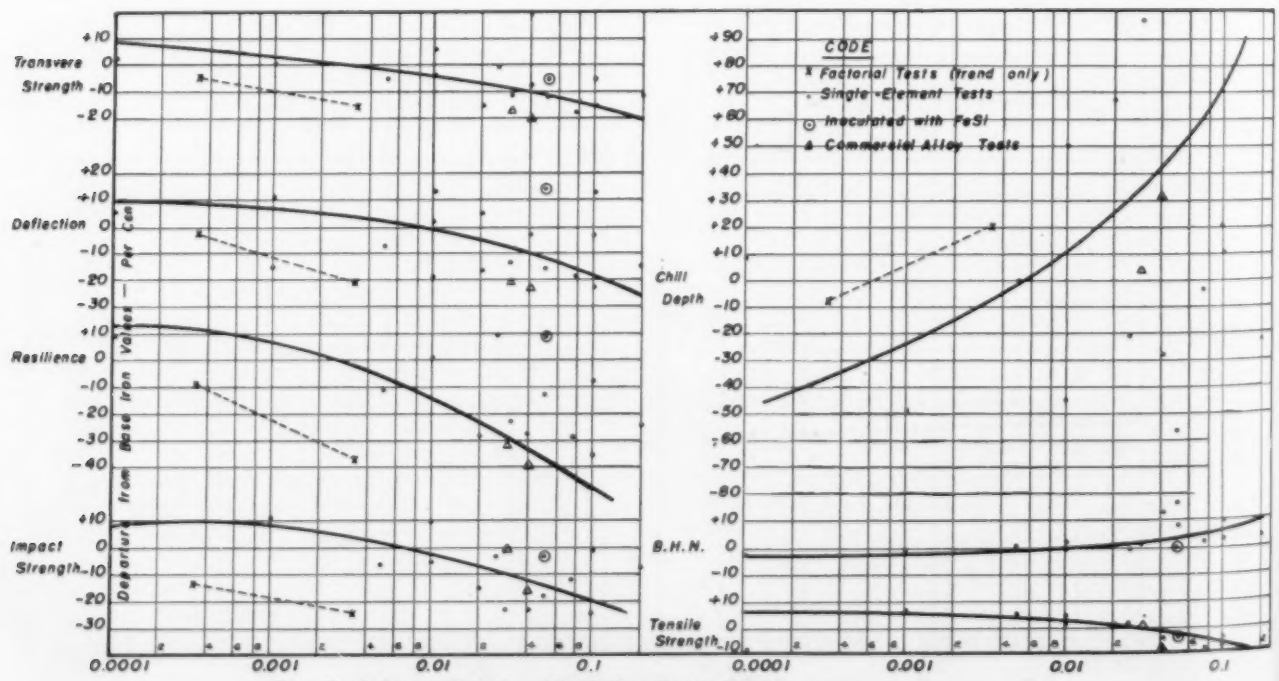


FIG. 7 - EFFECT OF LEAD AND COPPER.

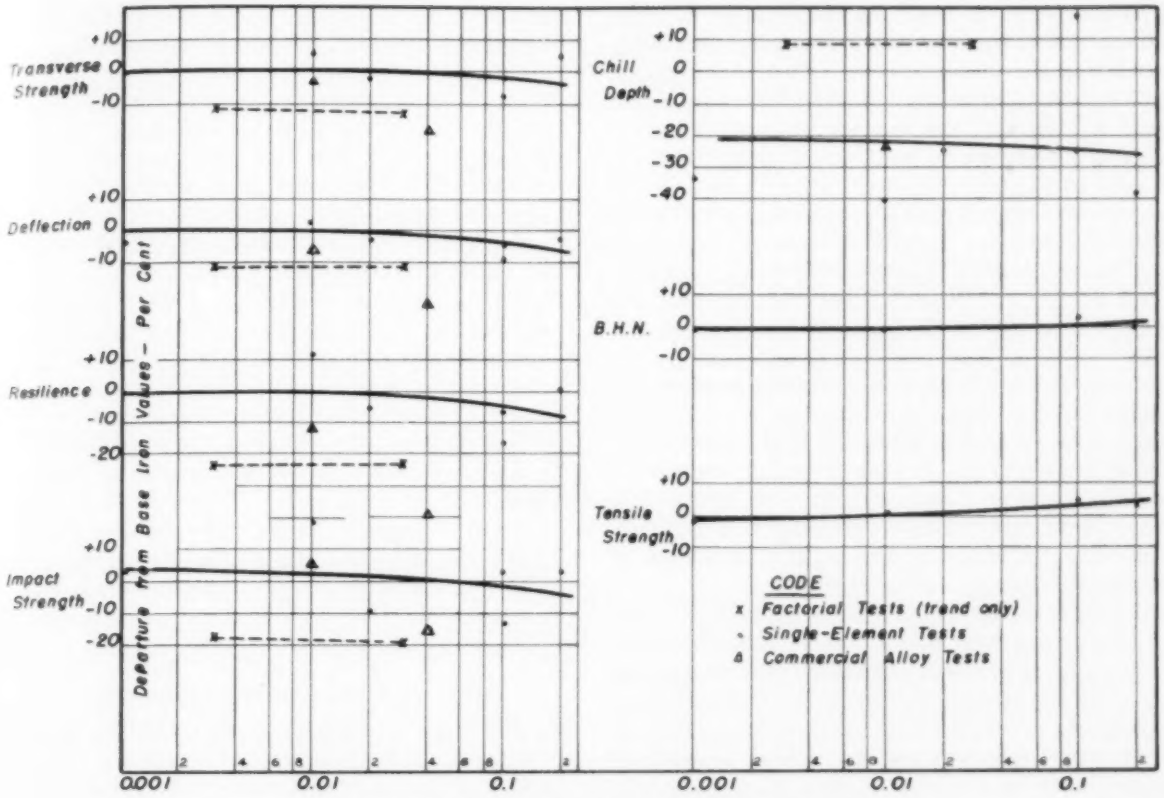


FIG. 8 - EFFECT OF TIN AND COPPER.

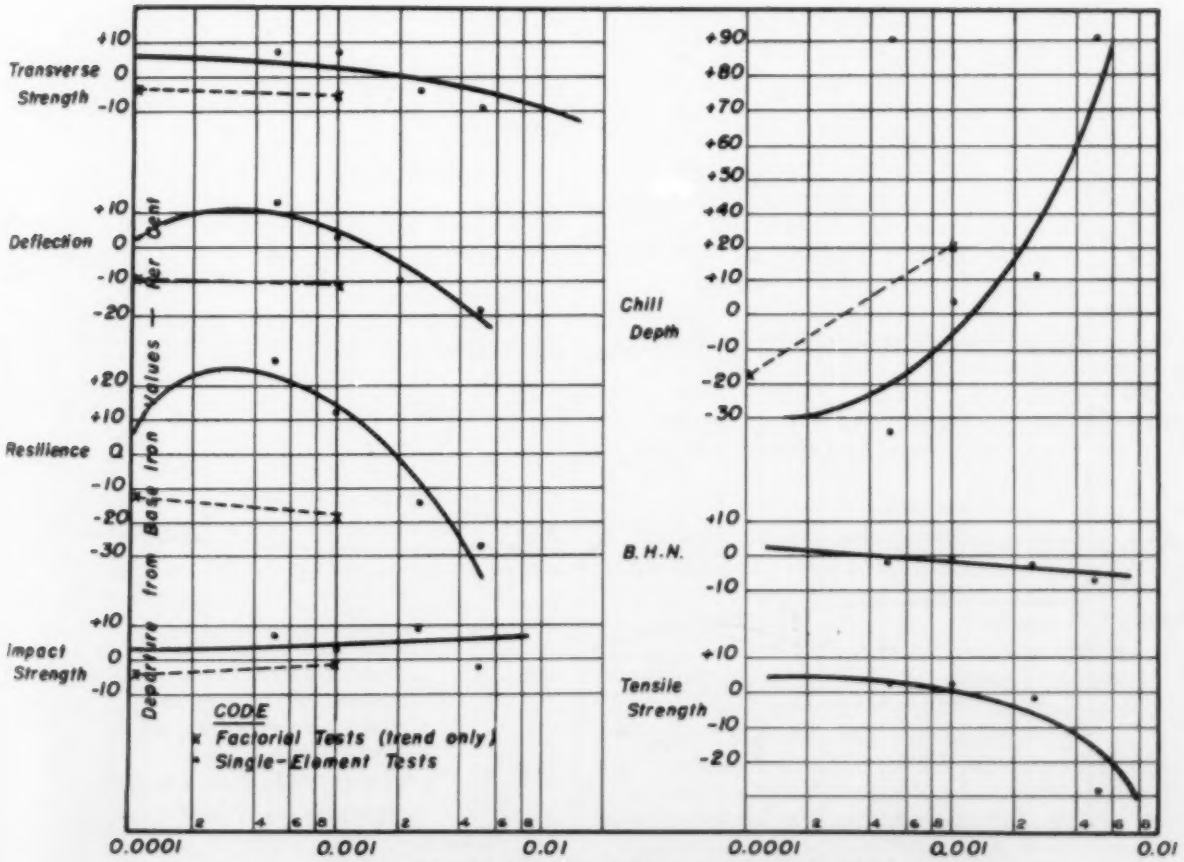


FIG. 9 - EFFECT OF TELLURIUM AND COPPER.

Very little effect was noted on the hardness of lead-treated specimens, but the chill increased greatly with an increase in the lead addition. Other investigators reported that lead increases the retention of carbides.

Figure 8 indicates that tin has little effect but is slightly detrimental to cast iron. In all probability, tin introduced by the addition of copper-tin alloys would not be harmful. Most authorities cite tin as a hardener.

Williams, Sims, and Newhall [9] claimed that tin is retained in iron melted in an electric furnace, but that lead and zinc are eliminated. They believed that less than 1 per cent tin in the iron had no effect on the graphite.

Much interest has been shown during recent years in the strong chill-forming tendencies of tellurium-treated gray irons. The CAST METALS HANDBOOK [3] states that as little as 4 grams of pure tellurium per ton of iron will produce an appreciable increase in the chill depth. The extreme potency of tellurium, together with its volatility, makes control a difficult problem in tellurium-treated irons.

Even with tellurium additions below 0.005 per cent, marked effects are indicated in Fig. 9. The effects for resilience, impact strength, and chill depth were found to be statistically significant. However, the significance refers only to the net effect of changing from 0.0001 to 0.001 per cent tellurium and the actual presence of the hump in the curves is not verified, though strongly indicated by the data.

Based on these data, tellurium appears to be harmless until the percentage added exceeds 0.001 per cent, but this undoubtedly would be changed if the actual recovery were known.

In these tests, where tellurium was added as a copper alloy, there was little evidence of the fuming which generally results from use of the pure element.

There is little reason to conclude that tellurium alone is a useful addition except for its chill increasing powers. However, small amounts, added with copper, do not appear to be difficult to control.

Zinc volatilizes so far below the temperature of molten cast iron one would not expect much to be retained. Spencer and Walding [6] found 0.04 per cent zinc remaining in iron to which brass had been added, and 0.15 per cent zinc after a similar treatment using Tobin bronze. The curves in Fig. 10 indicate that zinc has a negligible effect when the amount added is less than 0.05 per cent.

Table 7 suggests limits for the ten elements studied. These percentages represent the approximate point at which harmful effects predominate, or at which the curves in Fig. 1 through 10 cut downward through the line of zero departure.

Effects on Microstructure

Aluminum, antimony, arsenic, beryllium, cadmium, tin, and zinc did not appear to exert any important effect on the microstructure. The graphite flakes in the lead-treated iron were somewhat more ragged than usual, but the matrix was normal and there was no un-

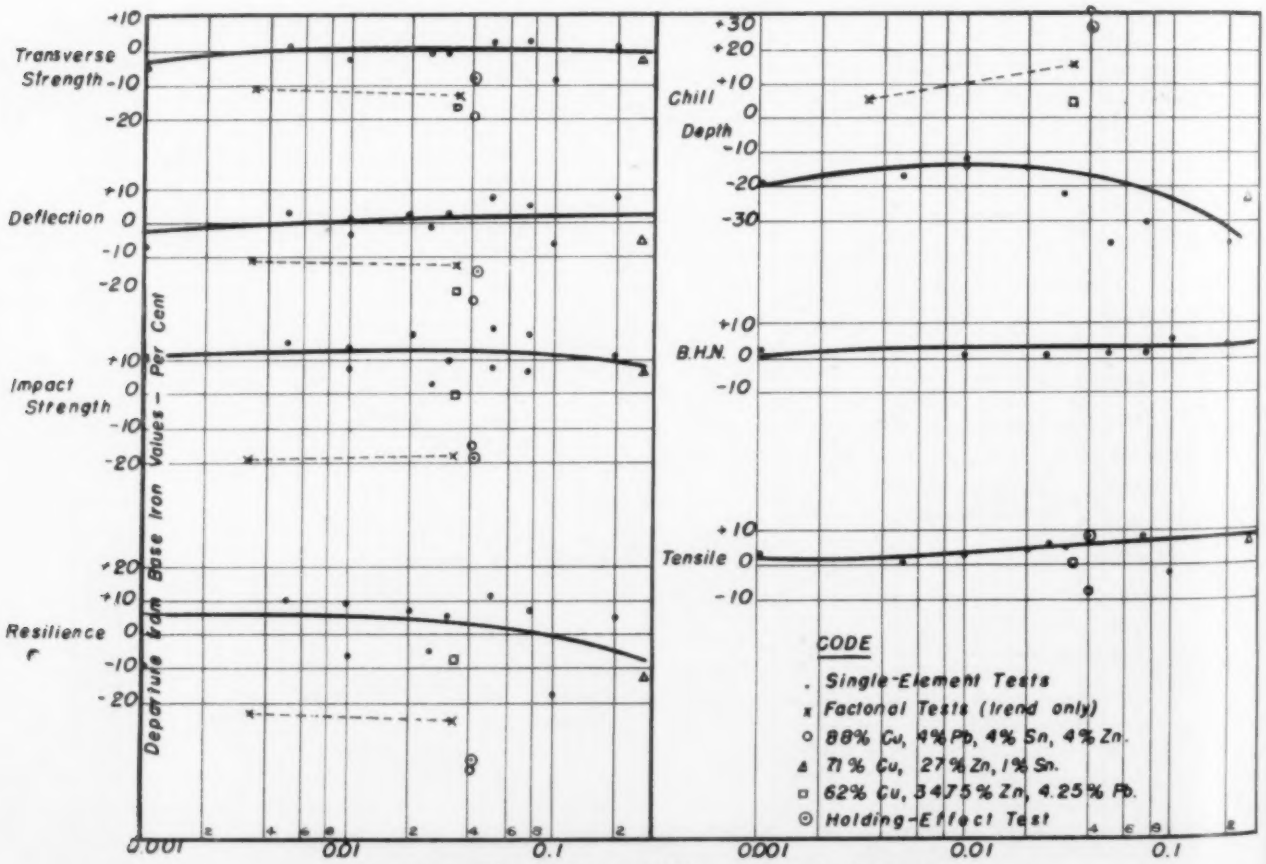


FIG. 10 - EFFECT OF ZINC AND COPPER.

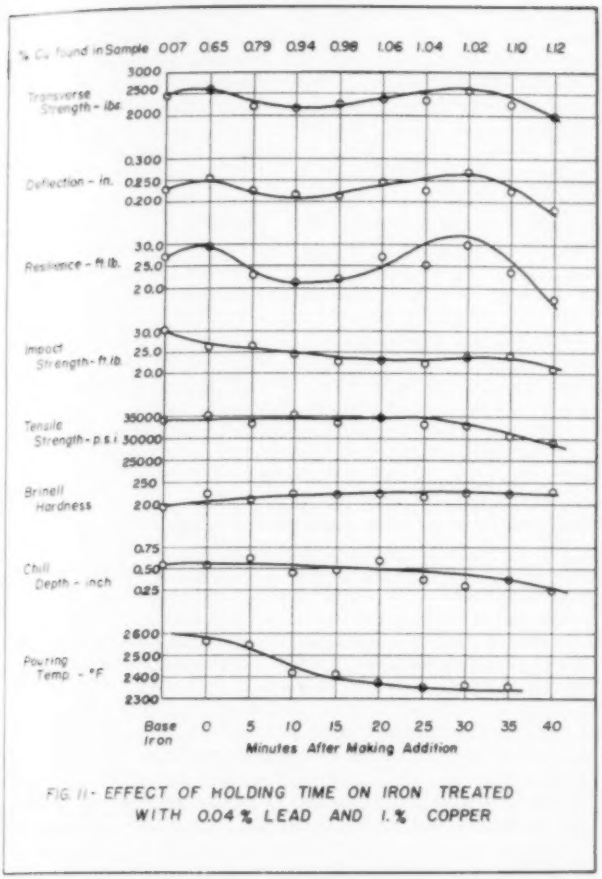


FIG. 11 - EFFECT OF HOLDING TIME ON IRON TREATED WITH 0.04 % LEAD AND 1. % COPPER

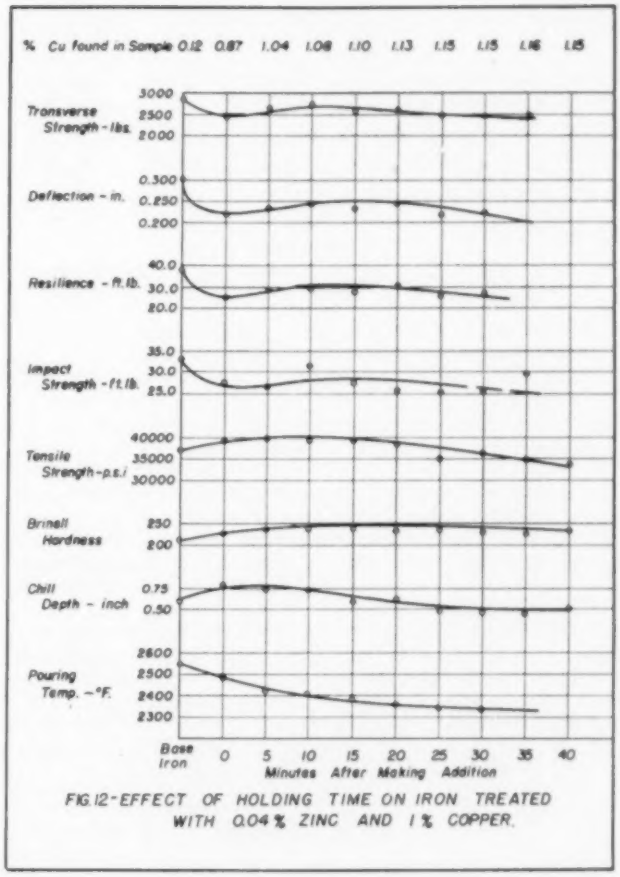


FIG.12-EFFECT OF HOLDING TIME ON IRON TREATED WITH 0.04 % ZINC AND 1 % COPPER.

duly large amount of eutectiform (AFA-ASTM Type D) graphite.

Bismuth appeared to promote the eutectiform type of graphite. In a specimen from the bar treated with 0.1 per cent bismuth, the graphite was almost exclusively of the Type D classification, in a predominately ferritic matrix.

The graphite was somewhat finer than usual in the tellurium-treated iron, but otherwise there was nothing out of the ordinary in the microstructure.

Effect of Holding Time on Lead and Zinc

In order to investigate the possibility of eliminating contaminating elements by allowing them to volatilize, 1200 lb. of iron was tapped into each of two insulated ladles. One mold was poured from the base iron in each ladle. One ladle was treated with 0.04 per cent lead and 1 per cent copper. A mold was poured as soon as possible after the addition was made, followed by others at five-minute intervals for 40 min.

The second ladle was treated similarly with an addition of 0.04 zinc instead of lead.

Figure 11 shows the results of the test in which lead was added. The first effect was an increase in the mechanical properties of the iron, followed by a drop, possibly as a result of more intimate mixing of the lead. The second drop after 30 min. is probably a result of the decreasing temperature.

These curves indicate a rapid drop in properties when the addition is first made, followed by a slow recovery. After 20 min. the properties begin to drop again, probably because the pouring temperature was too low.

Bismuth would be expected to respond similarly to zinc, but no tests were made.

TABLE 7.—SUGGESTED MAXIMUM PERMISSIBLE PERCENTAGES OF FOREIGN ELEMENTS THAT CAN BE ADDED WITH COPPER TO CAST IRON

Element	Maximum Per Cent Permissible
Aluminum	0.1+
Antimony	0.01+
Arsenic	0.01+
Beryllium	Indefinite, but probably above 0.2
Bismuth	0.001
Cadmium	0.005
Lead	0.005 or less
Tellurium	0.001
Tin	0.01
Zinc	0.01

Conclusions

Most of the elements likely to be introduced by the use of copper alloy scrap as a source of copper for cast iron will probably cause no harm.

A notable exception is lead, which should be kept very low. Lead might even pass through the melting unit in quantities large enough to be harmful. In all probability, a few of the other elements would be retained if they were melted with the charge.

Interactions appear to be less important than the effects of single elements. In fact, interactions tend to be beneficial, rather than harmful. Where the total contaminative-element content is low, heterogeneity of scrap types would appear to be desirable.

The suggested limiting percentages for each of the

foreign elements are given as follows:

a. Lead—0.005 or less
b. Cadmium—0.005
c. Bismuth, tellurium—0.001
d. Antimony, arsenic, tin, zinc—0.01
e. Beryllium—indefinite, but probably above 0.02
f. Aluminum—0.1

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DISCUSSION

Chairman: H. BORNSTEIN, Deere & Co., Moline, Ill.
Co-Chairman: J. E. BOWEN, Chevrolet Gray Iron Foundry, General Motors Corp., Saginaw, Mich.

W. J. SOMMER[1] *(Written Discussion)*: The paper is a very interesting one; it not only opens the way for the use of copper-base alloys for their copper content, it calms many foundrymen's fears of the non-ferrous scrap which quite often contaminates our present day purchased scrap. It appears, though, that the copper-base bearing alloys might well be ruled out because of the high lead content in most of them. All of the yellow and red brasses (normally copper, zinc, and tin with low lead content) would be suitable since, according to Table 3, the interactions of other metals combined with lead and copper greatly reduce the harmful effects of the lead. Reference to the effects of Pb, Pb-Zn, Pb-Sn, and Pb-Sn-Zn in the same table substantiate the latter statement.

Has any further work been carried on along the lines of the original data? Since bismuth finds its way into the metal with bearing metals and it is here also that the alloys are normally richer in lead, it would be interesting to note the effects of copper with lead and bismuth. It is noticeable in Table 4 that, while all effects of bismuth alone are harmful and four out of five with tellurium are harmful, the Bi-Te combination has produced three beneficial effects and reduced the magnitude of the harmful ones; the same results have been pointed out in the combinations of lead and other elements. It is, therefore, quite conceivable that the interactions of lead and bismuth might result in mildly beneficial effects. If this were true, might small additions of bismuth be in order where high lead content is suspected or known to exist?

A review of the effects of Bi, Al, and Bi-Al in Table 4 indicate that aluminum too might be a successful addition where high lead is present.

The potent chill-forming tendencies of tellurium are not to be questioned. An addition of 0.1 per cent pure tellurium has been known to change the fracture of a wedge test piece (½-in. base and 1-in. altitude) from a complete gray to a complete white.

While determining the effect of holding time on lead and zinc, it is assumed that the lead or zinc were added separately and not as an alloy. Taking into consideration the wide difference in temperatures at which these elements melt it appears that as the copper went into solution, as can readily be detected from the copper content shown, the beneficial effects of the copper overcame the harmful effects of the lead and zinc to a marked degree. Would not the addition of a copper-lead or copper-zinc alloy have leveled out these curves?

J. T. MACKENZIE:[2] Does this paper give the effect of adding one per cent of pure copper? Apparently in some of those tests on the early addition of one per cent copper with very small amounts of the other elements, the effect of the copper has changed the iron 30 per cent as in the chill diagram with tellurium (Fig. 9).

CHAIRMAN BORNSTEIN: Does your zero point on the curve represent the addition of one per cent copper?

MR. ROSE: It does. Our base irons were from ladles to which we had added one per cent of copper only. They are the base irons from which we determined the deviations. I believe your question was, do we show anywhere the effect of the copper alone? No, the nearest we come to doing so is shown in Figs. 11 and 12, where points on the curves indicate the change in properties immediately after the alloy was added, but, of course, the alloy addition was not pure copper, and we cannot say when the contaminating elements first became effective.

DR. MACKENZIE: That answers the question but it is very difficult to see how 0.0001 per cent tellurium could knock the chill test 30 per cent.

MR. ROSE: The curves in Fig. 9 represent, so far as we could tell, the effect of the tellurium copper addition (an addition of 0.0001 per cent tellurium) as compared with an addition of one per cent copper alone.

CHAIRMAN BORNSTEIN: Some years ago at one of our foundries a portion of a heat was entirely white when we wanted it gray and the only thing we found out at that time was that the metal had two per cent copper and we did not try to add any copper. The copper came from the scrap. At that time we did not have any particular way to determine tellurium. We assumed it was tellurium. Maybe it was lead that was detrimental.

DR. MACKENZIE: We found that lead usually knocks the chill. It does depend somewhat on the base iron and furthermore, lead is bad about coming through the cupola, whereas you can charge high-zinc brass in the cupola and not get any zinc in the iron at all. Apparently it has no effect. But you had better not get lead, antimony and tin in the cupola because they will come through and the effect of combined antimony and tin is far worse than the effect of either one of them by itself.

CHAIRMAN BORNSTEIN: How does the author account for the relatively poor results of lead addition? Some years ago, leaded steels were quite common. While I suppose there was some dropping off in physicals, there was nothing like the author shows on cast iron and we would naturally expect less effect of cast iron, that has a lot of notches in it already, like graphite particles. Does the author have any theory for the effect of the lead?

MR. ROSE: No, that is still a mystery. We analyzed some of the gray irons for lead, and although a trace was reported from the spectrographic analysis, certainly not much lead was present. So far as the microstructure was concerned, it did not reveal anything that seems to offer an explanation. The graphite flakes were somewhat more ragged than usual, but that was the only departure from what could be observed in the structure of the normal irons.

J. S. VANICK:[3] I would like to begin by acknowledging the work the authors have done in clearing up to some extent the effects of these contaminants on alloy additions. We call them contaminants if they do no good. If they do good, they become alloys. Now presumably all of these contaminants have been charted as pointed out within the range indicated in the paper and a good many show a downward trend in properties.

A small addition of nickel silver which contains perhaps 15 per cent of zinc, less than half the amount that is in yellow brass, has a bad habit of filling the metal full of gas regardless of its coincident effect upon physical properties. The gas may be a zinc compound but it gets bad enough so that even though you find less than 0.01 per cent of zinc in the analysis, the castings may be worthless. In one of my own experiences, we

[1] Plainville Casting Co., Plainville, Conn.

[2] American Cast Iron Pipe Co., Birmingham, Ala.
[3] International Nickel Company, Inc., New York.

undertook to melt pure materials to produce Ni-Resist in a Detroit type brass melting electric furnace that had been kept empty and heated all day to drive the zinc fume out. It was somewhere in the evening when we charged the furnace and poured the castings. Everything behaved well and the metal laid nice and quiet until the molds were filled. Then it spewed right up and shot all over the place, clear up to the rafters, coming out the gates and risers. A very high back pressure obviously developed by the reduced solubility of the zinc vapor as solidification temperatures were approached. While it is known that the solubility of zinc in liquid iron is very low the initial efforts to analyze sample taken from the blow-holed test bars that were poured, failed to record more than a "trace." With five-gram samples instead of one-gram samples a zinc content of 0.075 per cent was obtained. This amount would represent the effects of a 0.25 per cent addition of 70:30 Cu:Zn brass to an alloy of the Ni-Resist type which because of its high nickel content I believe, is less readily damaged than regular cast iron would be.

Intangible properties such as this are not dealt with in the paper except in connection with the risk of pinholes from aluminum.

There are advantages that at times some contaminant such as phosphorus in phosphor-copper may be carried in by means of its carrier—copper in this case—and in that way reacts differently from the course it might follow if carried in by the pure metal addition route.

Now I come to Mr. Sommers' discussion. I find he makes a comment which is liable to misinterpretation. He says apparently all of the yellow and red brasses would be suitable, according to Table 3, and then he refers to the low lead content in the brass. I am wondering whether he is referring strictly to the lead content and overlooking the zinc, because a yellow brass with, we will say, a 60:40 copper-zinc content if added in an amount of say one per cent will bring in 0.40 per cent of zinc and the suggested maximum percentage (Table 7) limit on zinc content are 0.01 per cent. Those of us who try to analyze for zinc in the 0.01 per cent content ranges know it is difficult to find such small amounts of zinc there. Thus the amount of yellow brass that could be brought in by way of an addition will have to be less than 1/40 of one per cent to finish up with 0.01 per cent zinc as the total limiting zinc content.

In the refining of metals by the secondary metal men, refinements in analysis such as spectrographic analyses, ought to enable them to in many cases deliver a product suitable for cast iron alloying purposes.

Mr. Rose: With regard to the leaded brass, I believe the effect of the lead would be much more important than the effect of the zinc.

Lest there be some misunderstanding as to what we mean by 0.01 per cent zinc, let me explain that we did not analyze for it. We added 0.01 per cent, but we do not know how much stayed in the iron. There certainly was not very much.

Spencer and Walding reported adding quite a bit of zinc as a 60-40 alloy, but they found only 0.04 per cent remained in the iron. This was only a small part of the amount added, so the zinc loss must have been very high.

We made one spectrographic analysis for zinc, and found only a trace.

Mr. Rose *(author's closure):* The comments by Mr. Sommers, Mr. Vanick, and Dr. MacKenzie are indeed welcomed because they deal with some points not fully covered in the paper.

The additions of various elements with copper were made in the form of copper-base alloys, and not as simple mixtures of the individual elements. In order to have sufficient latitude for making the additions, it was necessary to use relatively rich master alloys. After the master alloys had been combined to give the desired proportions of contaminants, copper shot was added to make up the amount required for a one per cent addition to the iron.

As Mr. Sommers suggests, more information would be desirable on the effects of lead and bismuth together, but we had no way of anticipating this when the experiments were planned. We did investigate the interactions of lead and tellurium with copper. The effect of the lead predominated strongly, as one might predict from the results of the other tests in which lead was added.

In studying the effects of interactions, we found that certain pairs of elements apparently were beneficial, whereas other pairs were harmful. A comparison of the alloy systems for 15 of the pairs of elements and the effects of them appears in Table 8.

Table 8.—Relation Between Alloy Type and Effects Derived from Interactions of Two Contaminating Elements Added with Copper

Binary Alloy Type	Pairs Showing Beneficial Effects	Pairs Showing Harmful Effects
Eutectic Forming	2	2
Solid Solution Forming	1	1
Limited Miscibility	2	1
Intermetallic Compound Forming	6	None

It may be seen that all six of the pairs of elements forming an intermetallic compound (or at least capable of doing so according to their equilibrium diagrams) produced an apparent improvement in the mechanical properties of the treated iron. Two of these improvements were found to be mathematically significant by the analysis of variance.

One should not draw unqualified conclusions from Table 8 because, in most instances, it is not known whether or not the properties reported are truly representative. However, it appears that the formation of an intermetallic compound might somehow reduce the potency of the individual elements; perhaps by partially removing them from the system.

If this be true, we would expect that bismuth and lead together would still be harmful, because they form an eutectic type of binary alloy, and do not form an intermetallic compound.

INFLUENCE OF INCLUSIONS ON PROPERTIES OF SAND CAST ALUMINUM-BASE ALLOYS[1]

By

G. Sachs,[2] A. W. Dana [3] and L. J. Ebert [4]

ABSTRACT

Six common aluminum-base alloys have been studied as to their tendency to form inclusions and also as to the manner in which their properties are affected by the size and location of the inclusions. It was found that of the alloys investigated, the CN21 alloy was most prone to form inclusions, while the S2 alloy formed the least number of inclusions.

The presence of an inclusion reduced the tensile strength of the alloy by causing a premature termination of the stress-strain curve. The degree to which the tensile strength was reduced was dependent somewhat on the alloy but, in general, the per cent reduction in strength was two to four times the per cent of total cross section occupied by the inclusion. Inclusions which extended to the surface of the cast test bar were found to cause a greater reduction in strength than those which were wholly included in the bar.

IN THE FIELD OF CASTING it has long been recognized that inclusions may occur under the best of casting control, and that their effect is, in general, one of reducing the strength and ductility of the casting. However, little is known as to the specific effects of their size and position in the casting. For example, little information is available as to the relative detriment of an inclusion wholly within the casting, as compared to an inclusion (of the same type and size) which intersects the surface of the casting.

Some previous investigations[1] have shown the effect of inclusions in the reduction in tensile strength of a 12 per cent aluminum-silicon alloy. Some other investigations have been made on the effect of pinhole porosity (which has an effect similar to that of inclusions) on the tensile properties of brass and copper castings.[2, 3, 4, 5] However, little data are available regarding the effect of inclusions on properties of various aluminum alloys which find extensive use in foundries today.

The work presented herein represents an attempt to obtain some quantitative data on the effect of inclusion size and position on the strength properties of several sand cast aluminum alloys.

Material and Procedure

The alloys studied are listed in Table I with the chemical analyses for the alloys. Two to three grades of each of the alloys were used. The first grade was designed so as to contain a maximum amount of minor constituents, and heats of this grade are referred to as *special* heats. The second grade contained amounts of minor constituents likely to be found in commercial alloys, heats of this grade being termed *stock* heats. The third grade was designed so as to keep the quantities of minor constituents at a practical minimum; heats of this grade are referred to as *pure* heats.

Tensile test bars cast under controlled commercial foundry conditions were used for the investigation.* The maximum melting temperature was kept below 1350 F in all cases except for the CN21 alloy which was melted up to 1400 F. All test bars were poured between 1300 and 1250 F. The test bars were cast in green sand molds of the design shown in Fig. 1, appropriate precautions being taken for skimming and fluxing. All bars were prepared for testing merely by removing the slight roughness from the cast surface of the reduced section by a light polishing. Approximately 3300 tensile test bars were available for the study. In all, 560 bars with inclusions were sorted out from this total number of bars by means of visual inspection at a magnification of five.**

Examination of Fractures

The fractured surfaces of the broken bars which contained inclusions were then projected on a screen (magnified 10 times). The area of the inclusion was

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[1] This paper is one of a series of reports in the research program on cast aluminum alloys conducted at Case School of Applied Science for the Aluminum Research Institute, Chicago, Illinois.

[2] Professor of Physical Metallurgy, Department of Metallurgical Engineering, Case School of Applied Science.

[3] Research Assistant, Department of Metallurgical Engineering, Case School of Applied Science.

[4] Senior Research Engineer, Department of Metallurgical Engineering, Case School of Applied Science.

* The tensile bars used in the investigation were cast for the determination of physical and mechanical properties of some aluminum alloys, in an investigation for the Aluminum Research Institute, Chicago.

** It should be noted that a large number of the inclusions were of such small size that they would have escaped detection except for the careful inspection of the magnified image of the fractured cross section.

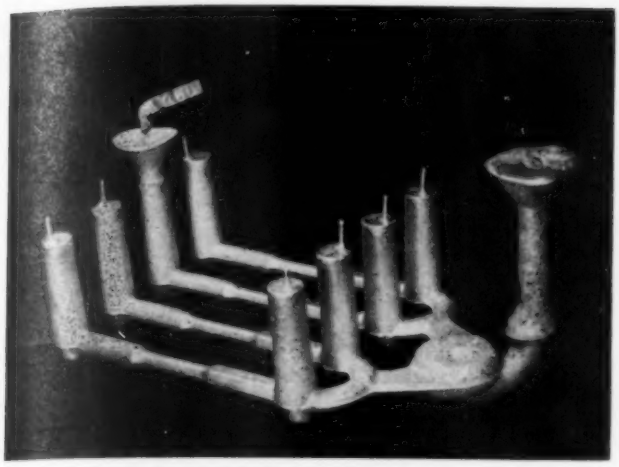


Fig. 1—Tensile test bar casting.

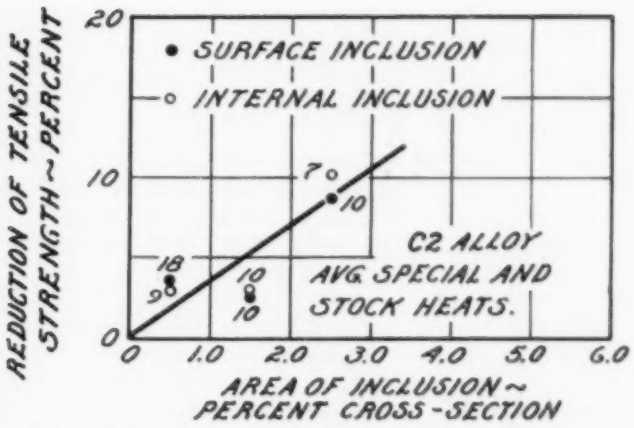


Fig. 2—Relation between inclusion size and reduction of tensile strength for the C2 alloy. Figure at each point indicates number of specimens.

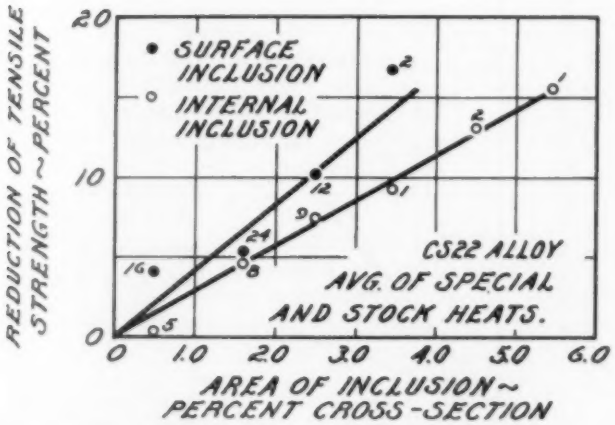


Fig. 3—Relation between inclusion size and reduction of tensile strength for CS22 alloy.

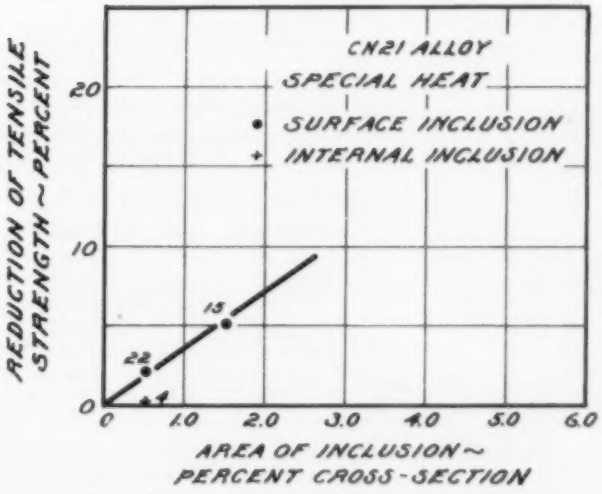


Fig. 4—Relation between inclusion size and reduction of tensile strength for CN21 alloy.

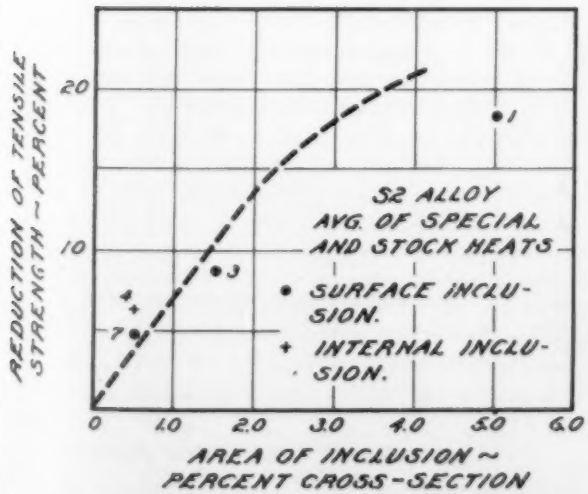


Fig. 5—Relation between size of inclusion and reduction of tensile strength for S2 alloy.

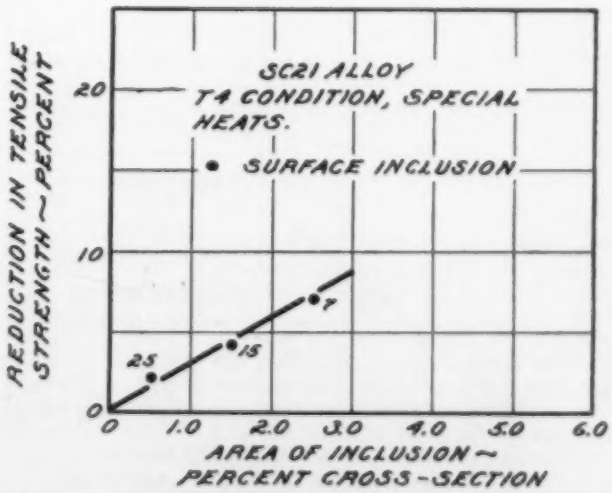


Fig. 6—Relation between size of inclusion and reduction of tensile strength for SC21 (TG) alloy.

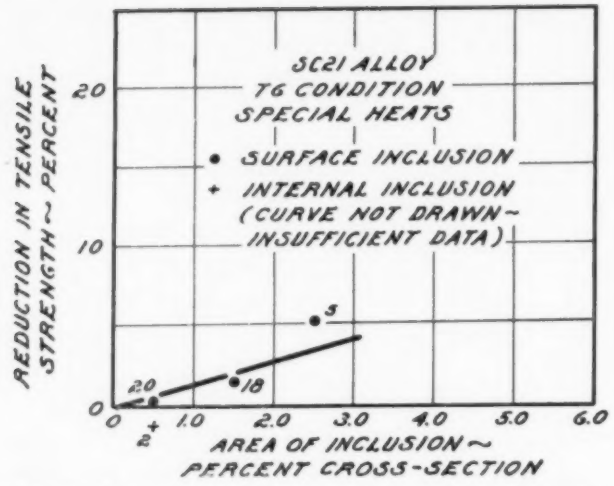


Fig. 7—Relation between size of inclusion and reduction of tensile strength for SC21 (T4) alloy.

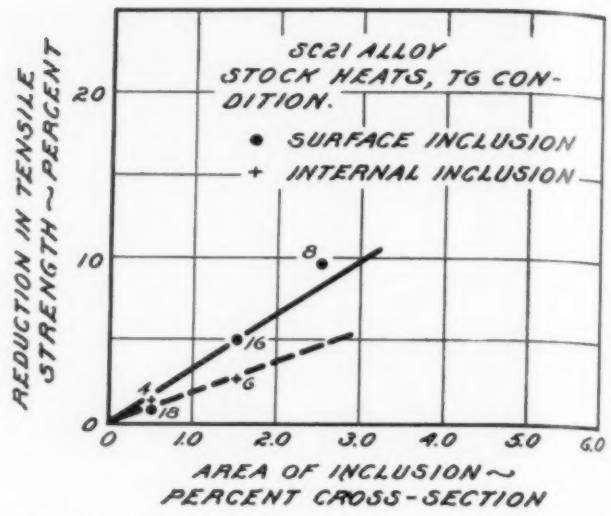


Fig. 8—Relation of inclusion size and reduction of tensile strength for SC21 alloy stock heats.

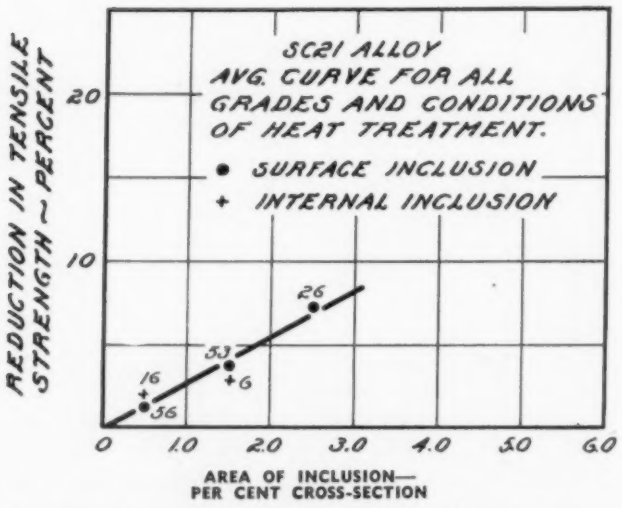


Fig. 9—Relation of inclusion size and reduction of tensile strength for SC21 alloy.

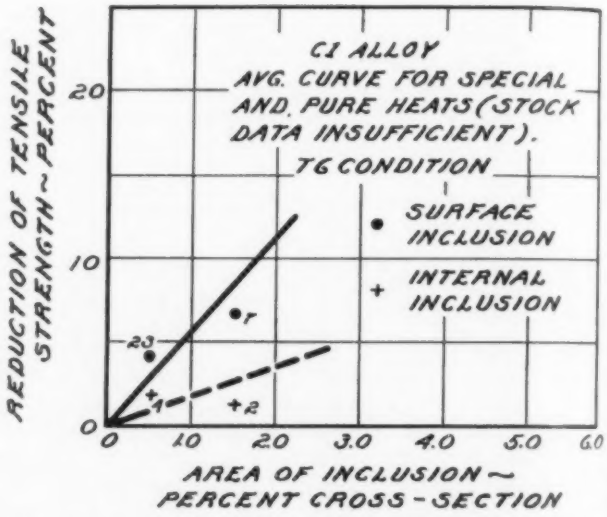


Fig. 10—Relation between inclusion and reduction of tensile strength for C1 alloy.

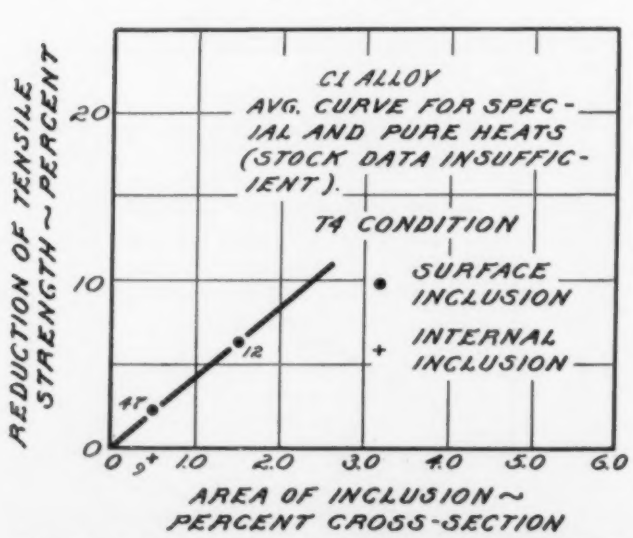


Fig. 11—Relation between inclusion and reduction of tensile strength for the C1 alloy.

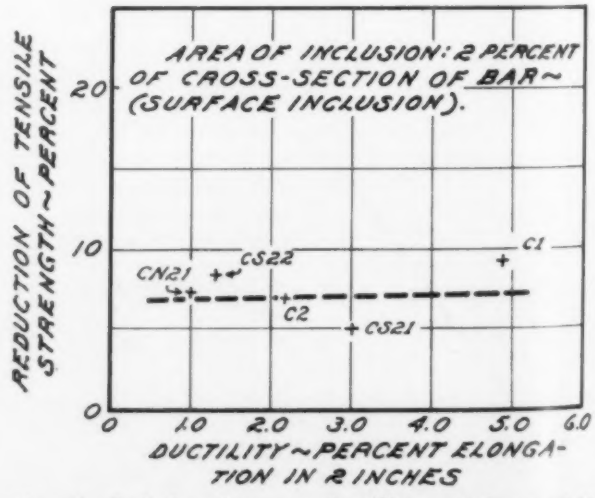


Fig. 12—Relation between ductility and per cent reduction of tensile strength for a given size of inclusion.

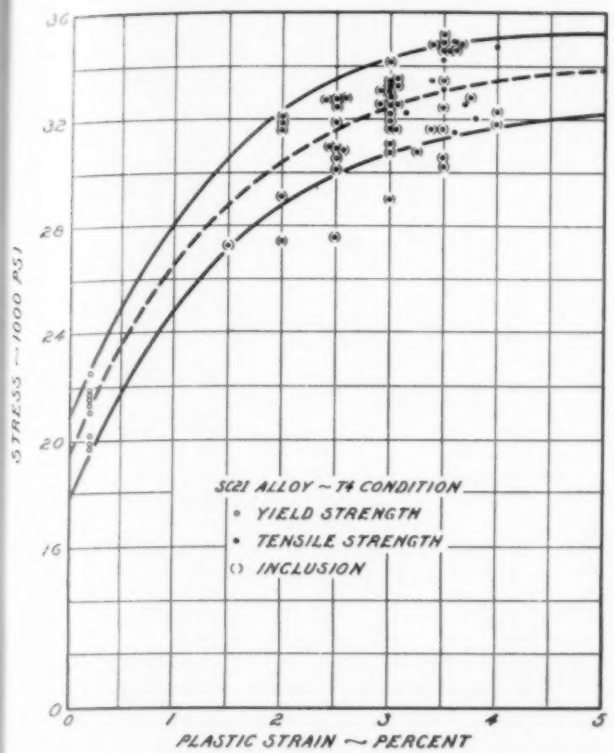


Fig. 13—Effect of inclusions on termination of tensile stress-strain curve.

measured by comparing the projected image of the inclusion to circles of known area. The accuracy of this method of measurement was ±0.001 square inch. Each inclusion was measured on both fractured surfaces of a given test bar. A notation was made as to the position of the inclusion in the fractured cross section.

No attempt was made to determine the composition of the inclusions. However, it is well known that the inclusions occurring in cast aluminum alloys are typically aluminum oxide. In the case of the magnesium-rich alloys, the inclusions may be a mixture of aluminum and magnesium oxide. The vast majority of the inclusions in this investigation were noted to have the typical appearance of aluminum oxide. Practically no flux inclusions were found, and only in few bars were inclusions of foreign material observed.

The test bars were available in various lots, each lot being cast at the same time for the same heat of metal. For the heat treatable alloys, each lot was heat treated as a unit. It was thus possible to determine the loss in properties of the bars with inclusions in any one particular lot, by comparing the properties of any bar containing an inclusion with the average properties of the bars of the particular lot without inclusions. For the sake of universal application of the results, these losses were always listed as percentages.

Results and Discussion

Effect of Inclusions on Tensile Properties. The effect of the size of the inclusion on the reduction in tensile strength is shown in Figs. 2 to 11, Table II. Of the alloys studied, the tensile strength was reduced approximately the same amount for the C2, CS22, and CN21 alloys by surface inclusions* of a given size in cases where reliable data were available. In these cases the per cent reduction in tensile strength was about four times the size of the inclusion (in terms of per cent of the fractured cross section).

The tensile strength of the C1 alloys was more affected by these surface inclusions than the tensile strength of the other, above mentioned alloys, while the tensile strength of the silicon-rich SC21 alloys was less affected. The second silicon-rich alloy investigated, the S2 alloy, formed so few inclusions that no reliable data could be obtained.

Very little data were available on the effect of internal inclusions on the reduction in tensile strength. However, from the data on the alloys CS22 and C1 (T4), in Table II it can be seen that the internal inclusions brought about only half the reduction in tensile strength caused by surface inclusions.

The reduction of tensile strength is apparently not related to the ductility of these two latter alloys, since the strength of the more ductile (T4)** condition of the SC21 alloy was more affected than that of the less ductile (T6)*** condition, while the effects were reversed in the case of the two conditions for the C1 alloy. This independence of the reduction in tensile strength caused by inclusions from the ductility of the alloy is further illustrated in Fig. 12.

* The inclusions were of two types: 1) those which intersected the surface of the bar were termed "surface inclusions" and 2) those which were contained wholly within the bar were termed "internal inclusions."

** Solution heat treated.

*** Solution heat treated and artificially aged.

For all alloys investigated, the yield strength of the test bars with inclusions was practically the same as that for bars with no inclusions, regardless of the size or the position of the inclusion.

TABLE I.—CHEMICAL ANALYSES* FOR VARIOUS ALUMINUM ALLOYS

Alloy† and Type	Alcoa Alloy Type	Composition, Per Cent Cu	Si	Fe	Mg	Ni	Mn	Sn	Pb	Zn	Ti	Cr
C2	108											
Special		4.30	3.08	0.95	0.03	0.23	0.40	0.05	0.05	0.38	0.04	0.05
Stock		4.03	3.13	0.70	0.03	0.17	0.45	0.02	0.04	0.25	0.07	0.07
CS22	.....											
Special		7.25	1.78	0.98	0.06	0.18	0.36	0.07	0.09	1.44	0.04	0.04
Stock		7.36	1.98	0.98	0.05	0.21	0.39	0.12	0.04	0.80	0.05	0.11
S2	43											
Special		0.48	5.29	0.62	0.02	0.02	0.42	0.01	0.02	0.25	0.04	0.05
Stock		0.32	5.27	0.50	0.02	0.03	0.08	.....	.....	0.07	.....	.....
C1	195											
Special		4.38	1.01	0.70	0.02	0.02	0.41	0.01	0.02	0.35	0.12	0.01
Stock		4.30	1.12	0.69	0.03	0.09	0.40	0.02	0.03	0.30	0.08	0.12
Pure		4.27	0.76	0.53	0.01	0.01	0.13	0.01	0.01	0.07	0.08	0.02
SC21	355											
Special		1.30	5.15	0.53	0.45	0.06	0.49	0.01	0.01	0.30	0.12	0.06
Stock		1.39	5.11	0.46	0.56	0.06	0.13	0.02	0.01	0.07	0.09	0.10
Pure		1.31	5.12	0.34	0.55	0.01	0.06	0.01	0.01	0.03	0.11	0.12
CN21	142											
Special		4.01	0.41	0.66	1.51	1.97	0.03	0.01	0.02	0.04	0.12	0.07
Stock		4.08	0.47	0.63	1.59	2.07	0.08	0.05	.....	0.04	0.14	0.19

* Average of four to six heats.

† All alloys in this paper are referred to by the standard ASTM designations.

The inclusions reduced the elongation of the test bars for all alloys. However, the majority of the inclusions were of such small size that the reduction in elongation rarely exceeded the accuracy of the measurement. Consequently, no accurate correlation could be made between the size or position of the inclusion and reduction in elongation.

Effect of Inclusions on Stress-Strain Curve

Mechanism by which inclusions cause reduction in the tensile strength and elongation is evidently one of terminating the tensile stress-strain curve prematurely. This can be seen from Figs. 13 to 15 in which the tensile strength and elongation from tests on bars with inclusions are plotted on the average stress-strain curves for tests on bars without inclusions.

This premature termination of the tensile stress-strain curve might be explained by the fact that the inclusion carries no load and/or acts as a notch. If the inclusion carries no load, it reduces the effective cross sectional area by an amount equal to the area occupied by the inclusion. If the effect were simply of this nature, the per cent reduction in tensile strength should be less than the area of the inclusion in per cent of the total cross section.*

However, the data represented in Figs. 2 to 11, Table II, shows that the reduction in tensile strength is two to four times this amount. Consequently, it

* A hole in a very ductile metal reduces the strength by slightly less than corresponding reduction in effective cross section.

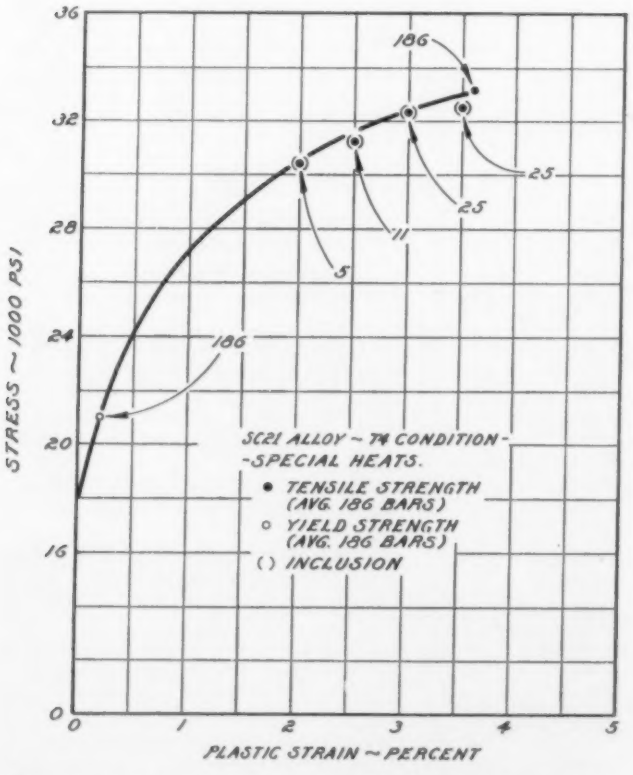


Fig. 14—Average curve for Fig. 13 showing premature termination of stress-strain curve. Figure at each point indicates number of specimens.

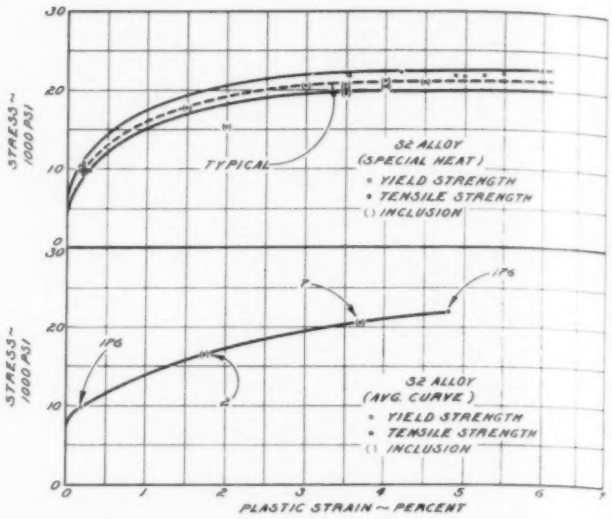


Fig. 15—Premature termination by inclusions of tensile stress-strain curve for S2 alloy. Figure at each point indicates number of bars.

appears that the inclusion acts as a notch, causing a slight stress concentration and resulting in an embrittlement of these alloys of relatively low ductility. No attempt has been made to analyze further this notch effect of inclusions.

Number of Inclusions, Location and Size

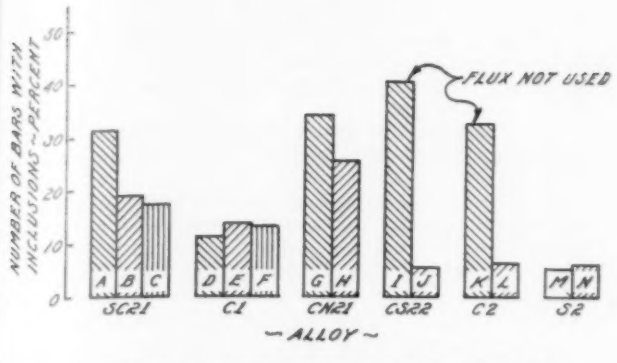
Number of inclusions found in bars of the various alloys is shown in Fig. 16. The various aluminum alloys exhibited different tendencies to form inclusions. The S2 alloy possessed the smallest tendency to form inclusions, while the largest number of inclusions occurred in the CN21 alloy.

Attempts were made to correlate the number of inclusions with the amounts of major and minor constituents, either singly or in combination. The major conclusion that could be reached was that if magnesium was present in amount of 0.5 to 1.5 per cent, the number of test bars with inclusions was considerably larger than for alloys containing less than 0.1 per cent magnesium, Fig. 17. In general, the tendency to form inclusions seems to increase progressively with increasing amount of magnesium.

TABLE II.—RESULTS OF INCLUSION INVESTIGATION ON ALUMINUM ALLOYS

Alloy	Ratio: Per Cent Reduction of Tensile Strength / Area of Inclusion in Per Cent Cross Section — Surface Inclusion	Internal Inclusion
C2	3.5	3.5*
CS22	4.1	2.8
CN21	3.75	*
S2	6.6*	*
SC21—T4	3.0	*
T6	1.3	
C1—T4	4.25	1.3
T6	5.75	*
Average	3.7	2.0

* Insufficient data to be accurate. Not included in averages.



LETTER	ALLOY	TYPE OF HEAT	NUMBER OF BARS INVESTIGATED
A	SC21	SPECIAL	350
B	SC21	STOCK	150
C	SC21	PURE	436
D	C1	SPECIAL	316
E	C1	STOCK	135
F	C1	PURE	532
G	CN21	SPECIAL	151
H	CN21	STOCK	40
I	CS22	SPECIAL	200
J	CS22	STOCK	200
K	C2	SPECIAL	168
L	C2	STOCK	245
M	S2	SPECIAL	176
N	S2	STOCK	200
		TOTAL	3319

Fig. 16—Graphical representation of number of inclusions present in the various alloys investigated.

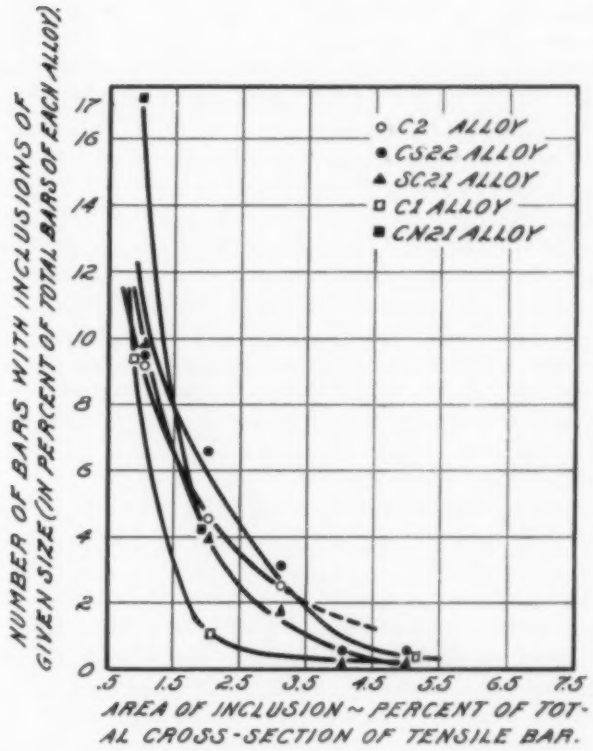


Fig. 18—Frequency distribution curves for size of inclusions in various sand-cast aluminum alloys.

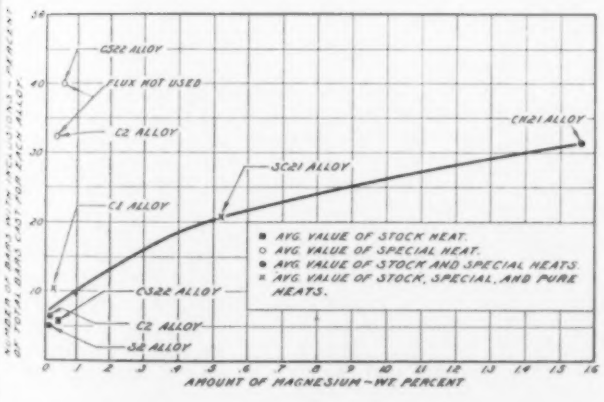


Fig. 17—Effect of magnesium content on percentage of bars with inclusions.

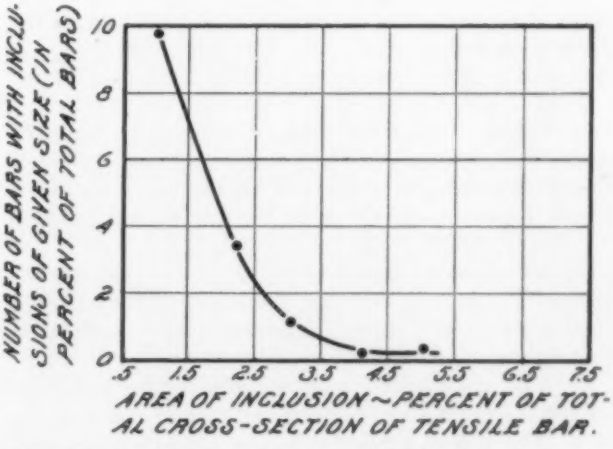


Fig. 19—Composite frequency distribution curve for size of inclusions in sand-cast aluminum alloys.

Because of the horizontal position of the test bars in the mold, Fig. 1, the majority of the inclusions were of the surface type, Table II. It is known that the most common type of inclusion found in aluminum castings, aluminum oxide, is heavier than molten aluminum and usually settles out. However, if any air or other gas is entrapped in the porous aluminum oxide, the inclusion might become effectively lighter than the molten aluminum, and so rise to the surface. This would explain why the majority of the inclusions in the horizontally cast test bars were of the surface type.

It is interesting to note, Figs. 18 and 19, that the majority of inclusions found in the cast bars were quite small. From the frequency distribution curves it can be seen that about 80 per cent of the inclusions occupied less than two per cent of the cross section of the test bar. Because of the fact that most of the inclusions were very small, the mechanical properties of only five per cent of the bars with inclusions fell

below the specified minimum values. The mechanical properties of 15 per cent of the bars with inclusions fell below an arbitrary minimum set at such a level that the properties of one per cent of bars without inclusions also fell below it.

The number of inclusions was also found to be dependent upon the manner in which the molten metal was handled before pouring. The special heats of the C2 and the CS22 alloys represented in Figs. 16 and 17 were cast without flux by a foundry practice in which the molten metal was "skimmed-rich." In this procedure, the metal was skimmed in such a manner that, to insure the removal of as much of the oxidized surface as possible, a considerable amount of molten metal was also removed from the surface. For all other heats represented, a neutral flux was used in the melting practice.

The molten metal was always allowed to stand for at least three to five minutes after it was removed from the melting furnace and before it was finally skimmed and poured.* The results show that the heats which were not fluxed contained a much higher number of inclusions than those which were fluxed, in spite of the precautions taken in skimming. This would seem to indicate that the flux provided the necessary agitation or vehicle for a separation of the inclusions from the molten metal by a mechanism of either floating to the top or sinking to the bottom of the crucible.

Conclusions

1. This study has shown that relative ratings of the investigated alloys as to their tendency to form inclusions is as listed below (the first alloy listed has the least tendency to form inclusions).

1) S2; 2) C2; 3) CS22; 4) C1; 5) SC21; 6) CN21.

2. Various alloys are affected differently by inclusions of a given size. The relative manner in which the tensile strength is lowered for the various alloys is listed below (in order of increasing embrittlement).

1) SC21; 2) C2; 3) CN21; 4) CS22; 5) C1.

3. In general, inclusions which intersected the surface of the casting reduced the tensile strength more than those which were contained wholly within the casting (for a given size inclusion).

4. The yield strength is not affected by inclusions of the type and size encountered in this investigation.

5. The use of a neutral flux is a much more effective way of eliminating inclusions than the use of ordinary skimming precautions.

Acknowledgments

The authors are indebted to the Aluminum Research Institute for permission to publish these data, and to Messrs. M. L. Fried and A. R. Toole, under whose direction the test bars were cast.

* In all cases, the test bars were poured directly from the melting crucible. About 10 or 16 lb. of metal was left at the bottom of the crucible at the completion of pouring, to prevent any heavy inclusions from being poured into the mold.

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DISCUSSION

Chairman: W. E. SICHA, Aluminum Co. of America, Cleveland

Co-Chairman: J. C. DEHAVEN, Battelle Memorial Institute, Columbus, Ohio

H. E. TRIMBLE[1]: What is the chemical composition of the material used as a flux in this operation?

MR. EBERT: We used a solid flux, which contained sodium chloride, potassium chloride and a small amount of cryolite.

MR. TRIMBLE: The weight of aluminum oxide and aluminum being nearly the same is the biggest cause of our trouble with aluminum oxide inclusions in aluminum castings. I would like to know if there has ever been a determination made as to the relative weights of the two?

MR. EBERT: I do not have information on this subject. However, we did notice that although the aluminum oxide and aluminum had relatively the same densities, at least so it is given in the handbooks, there seems to be a difference in the effective density or the effective specific gravity. It seems there must be some air entrapped in the dross or oxide that is formed. At any rate, a portion of it usually floats to the surface. In all our work we skimmed twice, first, when we pulled the crucible from the furnace and again when we were ready to pour the metal. As a result we seemed to be able to get a lot of it off.

MR. TRIMBLE: Why is it such a difficult task to get all the oxide out of the aluminum? I wondered if at Case you have made a study relative to the weights of oxide and metallic aluminum?

MR. EBERT: Our problem was that of determining the effect of the inclusions.

D. BASCH[2]: May I ask how you determined the oxide?

MR. EBERT: We did not run a check on the analysis but from experience and from the appearance of the inclusions we deduced they were oxides. The flux inclusion has a distinctly different appearance. Consequently, we assumed that the remaining inclusions were oxide inclusions.

CHAIRMAN SICHA: Mr. Ebert did touch on the characteristic of these oxide inclusions but there has been sufficient work done on the subject so it is very well established that the oxide has a higher specific gravity than the aluminum molten metal. That would mean that the oxides, if they were true oxides, would tend to sink. However, they are almost invariably of such a nature that they are porous and they contain a certain amount of occluded and trapped gas and that can be readily observed by examination of a section under a microscope. You can see they are quite porous and with that trapped gas the specific gravity is lower than that of the melt and therefore the dross particles tend to rise although the difference in specific gravity is such that the tendency is not marked.

W. E. MARTIN[3]: I want to confirm your findings that the yield strength is not affected by inclusions. In testing several thousand routine sand cast aluminum alloy test bars at Sperry Gyroscope Co., Inc., we made yield strength determinations and definitely established there is no correlation between yield strength and inclusions. The ductility and tensile strength were affected by inclusions, as you have shown. In surmising as to the effect of inclusions on load-carrying ability of aluminum castings, I would say that the inclusions do carry an elastic load. That is, as long as the load is purely within the elastic region, the inclusion is carrying its portion of the load. It is only when plastic flow starts that the effect of the inclusions

[1] U. S. Naval Ammunition Depot, Crane, Ind.
[2] Almin Ltd., Schenectady, N. Y.
[3] Wm. F. Jobbins, Inc., Aurora, Ill.

becomes detrimental. I think the inclusion is not malleable. It will not flow plastically. These remarks are meant to apply only in cases of static loads, not repeated on fatigue loads.

MR. EBERT: In agreement with your comments on the effect of the small plastic strains, we also found the yield strength was unaffected.

MEMBER: In thousands of test bars that we have pulled the yield strength is absolutely unaffected by inclusions in the normal situations encountered. We have had, though, the advantage of having had a good many inclusions along the axis of the bar and they have no effect. That would indicate again the non-sensitivity such as you have indicated.

We also would like to check the effect that any alloy containing magnesium vastly increases the tendency to oxide inclusions. Sometimes, depending upon the magnesium content, you have to resort to rather unusual foundry practices to pour inclusion-free castings.

It would be well to have a more adequate description of the authors' melting practice, particularly with regard to conclusion No. 5, that the use of a neutral flux is so important to obtain inclusion-free bars with some of these alloys. Most of the alloys used here were the most castable there are. Usually fluxes are not necessary, providing the use of the flux of the type that the authors used is understood. People have used fluxes and gotten into difficulties with regard to inclusions and we do not blame the flux for it, but the manner in which the flux was handled. In other words, there always seems to be the necessity for the strong right arm on the stirring rod after the flux application. This stirring of the flux which the foundryman feels necessary actually tends to stir in some of the surface oxide which normally would have remained where it could have been handled. In those instances, sometimes leaving the flux out has actually helped the situation.

You have used a surplus liquid flux in your melting operation but you did all your melting under this molten flux layer and rather than stirring the flux in after the bath was completely melted.

MR. EBERT: The metal was melted in an 80-lb. capacity graphite crucible in an oil-fired pot furnace. The charge was 80 lb.; we always melted a full crucible. The melting itself took anywhere from ½ to 1 hr. The flux was added at approximately between 1250° and 1300° F. when the metal was completely molten. It was added in the solid form and melted. After the metal had reached equilibrium it was pulled from the furnace and skimmed. In both cases (melting with and without flux) it was skimmed after it was pulled from the furnace. The pot was then taken out to the foundry room and allowed to stand until it cooled down to the pouring temperatures that we wanted. This usually required from three to five minutes, and in half that length of time the metal was again skimmed. We did not have to worry about the amount of metal that we wasted in skimming, so we skimmed liberally. Then the metal was poured directly from that crucible into molds.

Regarding the comments about whether it is necessary to flux or not, I will not say. All that we attempted to do was to present our data and for the particular alloys that we used and with the foundry practice we used, which I think is probably average or a little better than average, we found that the number of inclusions decreased when we used this neutral flux. I can point out again that the influence of inclusions is small regarding specifications. All but five per cent of the bars with inclusions passed specifications. However, the inclusions did affect the properties somewhat.

HIRAM BROWN[4]: The paper was very interesting. It brings out a lot of practical applications for the practical foundryman. Have you noticed any particular color to these oxide inclusions? I was quite interested in what you said about aluminum oxide being heavier. I notice that England has done a lot of work on that. They indicate that the oxide is lighter. Is there any disagreement with their findings or is it a different type?

MR. EBERT: It is rather hard to describe the color of the inclusions. They were a little darker gray than the fracture.

MR. BROWN: Did you notice any brown or black ones?

MR. EBERT: I think in some we found black inclusions.

CHAIRMAN SICHA: I have not seen the results of this British work but feel quite sure it does not indicate that the specific gravity of aluminum oxide is comparable to that of the metal.

MEMBER: In our experience, the aluminum oxide inclusions present in aluminum alloys containing no magnesium are invariably a gray color. In the alloys containing magnesium, such as the alloy 355, 356 and 142 types, particularly after heat treatment, you cannot mistake them. They are almost black. In fact, they go in some parts of the country by a very descriptive term, "niggerheads," because of that distinctive color.

J. E. GILL[5]: Do you have data on material extremely loaded with oxide? A company in the East wanted me to put aluminum oxide in the melt and cast a rotor plate for a potato peeler with 25 per cent oxide. What effect might that have on the tensile strength? According to your statements it would reduce it to no strength whatever.

MR. EBERT: We made no specific attempt to add inclusions in our work. The largest area that was covered by inclusions was about 5 or 6 per cent of the total cross section. With regard to the effect of 25 per cent inclusions, I do not have data but I would judge the properties would be very low.

MR. GILL: Would you guess as to what alloy would carry the inclusions best?

MR. EBERT: From the data we have presented, the ductility in the range we have investigated seems to have no influence on the effectiveness of inclusions. However, I rather suspect that if you could get an alloy with a large amount of ductility, it would have a better chance of carrying the very high non-uniform load. We surmised that the inclusions acted as stress raisers and introduced some transverse tensions. If that is the case, we know from the large amount of work we have done in notch testing that the more ductile alloys are more immune to the severe test of stress raisers.

J. D. TRONCALE[6]: We made some specimens out of 40-E alloy. It has a small percentage of Ti, Cr, magnesium-zinc alloy. We made some specimens of 100 per cent virgin metal on the first pour and we found that we had more inclusions in the inversion material in the first specimens. Then the second group of specimens were made from 50 per cent virgin and 50 per cent gates and risers, and there were no inclusions in the test bars. The metal was melted in a silicon carbide crucible. I really believe that some of the metal might have absorbed some of the silicon carbide in the crucible.

[4] Solar Aircraft Corp., Des Moines, Iowa

[5] Lakeshore Pattern Works, Erie, Pa.

[6] Aluminum Brass Foundry, Los Angeles, Calif.

TABLE III—TYPICAL DATA SHOWING SCATTER OF RESULTS FROM ONE SPECIAL HEAT OF INCLUSION-FREE TEST BARS

Three Point Tensile Test Results for CN21 Alloy Solution Heat Treated and Stabilized

Pouring Temp. °F.	Minimum Dia. In.	Yield Strength 1000 psi.	Tensile Strength 1000 psi.	Elongation in 2 in. Per Cent
1320	0.501	28.1	30.7	1.0
1320	0.494	28.6	30.8	1.0
1320	0.500	27.9	29.6	1.0
1320	0.500	27.5	28.9	1.0
1315	0.495	27.0	29.5	1.0
1315	0.497	27.2	28.2	1.0
1315	0.503	27.5	29.4	1.0
1315	0.496	28.0	29.4	1.0
1310	0.497	27.4	30.1	1.0
1310	0.500	27.2	29.8	1.0
1305	0.496	26.5	29.8	1.0
1305	0.501	27.0	29.7	1.0
1305	0.497	27.3	28.7	1.0
1300	0.501	28.1	30.6	1.0
1300	0.493	27.8	29.4	1.0
1300	0.499	28.2	28.7	1.0
1300	0.499	27.8	29.0	1.0
1300	0.503	27.8	30.2	1.0
1300	0.494	27.6	30.2	1.0
1300	0.502	28.0	29.7	1.0
Average		27.6	29.6	1.0

TABLE IV—AVERAGE TENSILE PROPERTIES OF INCLUSION-FREE TEST BARS

Alloy	Condition	Yield Strength 1000 psi.	Tensile Strength 1000 psi.	Elongation in 2 in. Per Cent
	Special Heats			
C2	As Cast, 24 hr.	12.8	24.2	2.8
C2	As Cast, 2–3 wk.	17.9	24.7	1.7
CS22	As Cast, 24 hr.	16.8	24.4	1.6
CS22	As Cast, 2–3 wk.	21.7	26.7	1.4
S2	As Cast, 24 hr.	9.9	22.2	5.4
S2	As Cast, 2–3 wk.	10.0	22.1	4.8
SC21	T4	21.1	33.2	3.6
SC21	T6	28.0	36.9	2.7
C1	T4	18.8	34.3	5.2
C1	T6	26.6	37.8	3.0
CN21	SHT and Stabilized	27.9	31.0	1.0
	Stock Heats			
C2	As Cast, 24 hr.	13.1	24.3	2.8
C2	As Cast, 2–3 wk.	16.8	26.1	2.0
CS22	As Cast, 24 hr.	17.3	24.5	1.4
CS22	As Cast, 2–3 wk.	23.1	27.3	1.1
S2	As Cast, 24 hr.	10.2	21.3	5.4
S2	As Cast, 2–3 wk.	10.2	21.3	5.5
SC21	T6	27.0	32.0	2.5
C1	T4	17.0	33.0	6.1
C1	T6	24.1	35.2	4.1
CN21	SHT and Stabilized	—	44.0	2.5
	Pure Heats			
SC21	T6	24.4	34.5	3.3
C1	T4	16.1	33.5	8.7
C1	T6	25.6	37.1	5.1

TABLE V—AVERAGE MOLDING SAND PROPERTIES

Permeability	25
Strength (Compression)	4.7 psi.
Moisture	6.0 per cent
Temperature	72° F.

CHAIRMAN SICHA: I strongly suspect there were some differences in melting practice that would be accountable for the oxide. You can with a damaged silicon carbide crucible actually get silicon carbide inclusions in the melt.

I feel as a matter of record it would be very interesting to have the physical properties of the molding sand that was used in these tests included.* If someone does similar work at a later date, it might be a factor in the results obtained.

With reference to the second conclusion of the paper, it stated that various alloys are affected differently by inclusions of different sizes. That sounds just a little too positive to me. It would seem to me, from the data presented that the conclusion might rather be that mechanical properties of the various alloys are affected to much the same extent by inclusions of a particular size.

Another point—it might amplify the story a little bit to have some actual mechanical properties. All of this work is properly based on percentage figures so as to provide good comparisons. However, just as a matter of interest, I think it would help to include some of the actual properties on inclusion-free test bars, principally to show the degree of scatter in those inclusion-free bars because this sort of analysis is very tricky and if there is a tremendous scatter in the inclusion-free bars it might raise a question about the actual numerical values attached to the effects of the inclusions.

W. BONSACK[†]: The analysis you gave was quite interesting, in particular the fact that most of your inclusions were found in the circumference or the surface of the casting. May there not be a possibility that these surface defects are due to turbulence in the mold or because of the molding sand? It might not be in the metal at all.

MR. EBERT: That may very well be. I do not think I can answer that directly because we have made no measurement of turbulence. Our chief aim was one of finding the effect of the inclusions, no matter how they got there.

MR. BONSACK: You made the statement that when the alloy contains magnesium, the more magnesium the more inclusions. You may find that just the moisture in the molding sand creates more inclusions in the magnesium bearing alloys than in magnesium-free alloys. This should be taken into consideration.

MR. EBERT: That may very well be.

* Note: The author did so by including Tables 3, 4 and 5.
† National Smelting Co., Cleveland

SEGREGATION IN MANGANESE BRONZE

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ABSTRACT

Under some conditions, manganese bronze melts contain a sludge consisting of iron, silicon, aluminum and manganese. Formation of the sludge is associated with silicon contents in excess of 0.1 per cent. There is a tendency for elongation to decrease as silicon increases. The sludge will cause thin castings to misrun.

THE FOLLOWING INCIDENT called attention to the effect of small amounts of impurities in manganese bronze.

In pouring several molds from the same ladle of molten manganese bronze, the molder noted that the metal seemed to cool faster than usual. He also noticed a tendency toward formation of a sludge, which floated in the metal. It was assumed that the furnaceman had not brought the metal up to the desired temperature, but he insisted that he had done so. Pyrometers were checked and found to be in order. Several days later the same thing occurred again, and once more the temperature was checked and found to be as ordered. An analysis of the metal showed that it conformed to required specifications. When some of the floating sludge was analyzed, it was discovered to be higher in iron and silicon content than the original metal. These questions immediately arose:

What part, if any, did the silicon play in the formation of the high iron sludge?

What amount of silicon can be tolerated, with no detrimental effect?

What is the source of the silicon contamination?

Test Procedure

In testing to locate the source of the silicon contamination, ingots from several shipments of manganese bronze were sectioned. The section surfaces were prepared for macroetching by grinding on a 230 grit belt. The surfaces were then etched in a 10 per cent ammonium chloride solution for two weeks. By that time rusty colored patches had developed on the surfaces, where there were local segregations of iron.

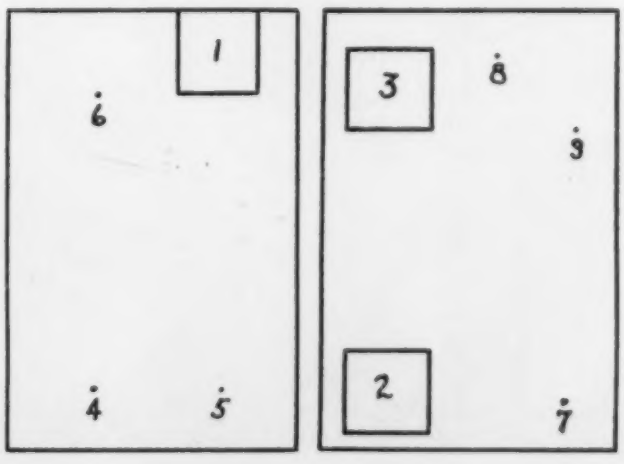
In Fig. 1 are photographs, at two magnifications, of two ingot sections etched for two weeks in a 10 per cent ammonium chloride solution. A shows no evidence of iron segregation and was the best lot of ingot examined. B shows a marked segregation toward the top of the ingot; it was the worst lot of ingot examined. Other ingot shipments ranged between.

From the sections shown in Fig. 1 samples ½ x ½ in. were taken for microscopic examination and chemical analysis. Figure 2 shows the location from which the samples were taken.

Figure 3A, location A1 of Fig. 2 at 100 magnifications, shows a beta-structured manganese bronze with the iron compound well distributed. Figure 3B shows the same location at 500 magnifications. Note that the areas around the large iron compound segregations are largely free of the small iron compound segregations. Table 1 gives the average analysis of this section, sample number 2A1.

Figure 4A, location 2B of Fig. 2, at 100 magnifications, shows an alpha beta structured manganese bronze, with the iron compound well distributed. Figure 4B shows the same location at 500 magnifications. Table 1, under sample number 2B2, gives the average analysis of this section.

Fig. 2—Sketch of ingot sections, A, left and B, right, shown in Fig. 1, showing location of chemical, spectrographic and microscopic samples. Numbers 1, 2 and 3 are the microscopic and chemical samples. Numbers 4, 5, 6, 7, 8 and 9 are the spectrographic spark locations.



NOTE: This paper was presented at a Brass and Bronze Session of the 51st Annual Meeting, American Foundrymen's Association, at Detroit, April 28-May 1, 1947.

* The opinions or assertions contained herein are those of the writer and are not to be construed as official or revealing the views of the Navy Department or the Naval Service at large.

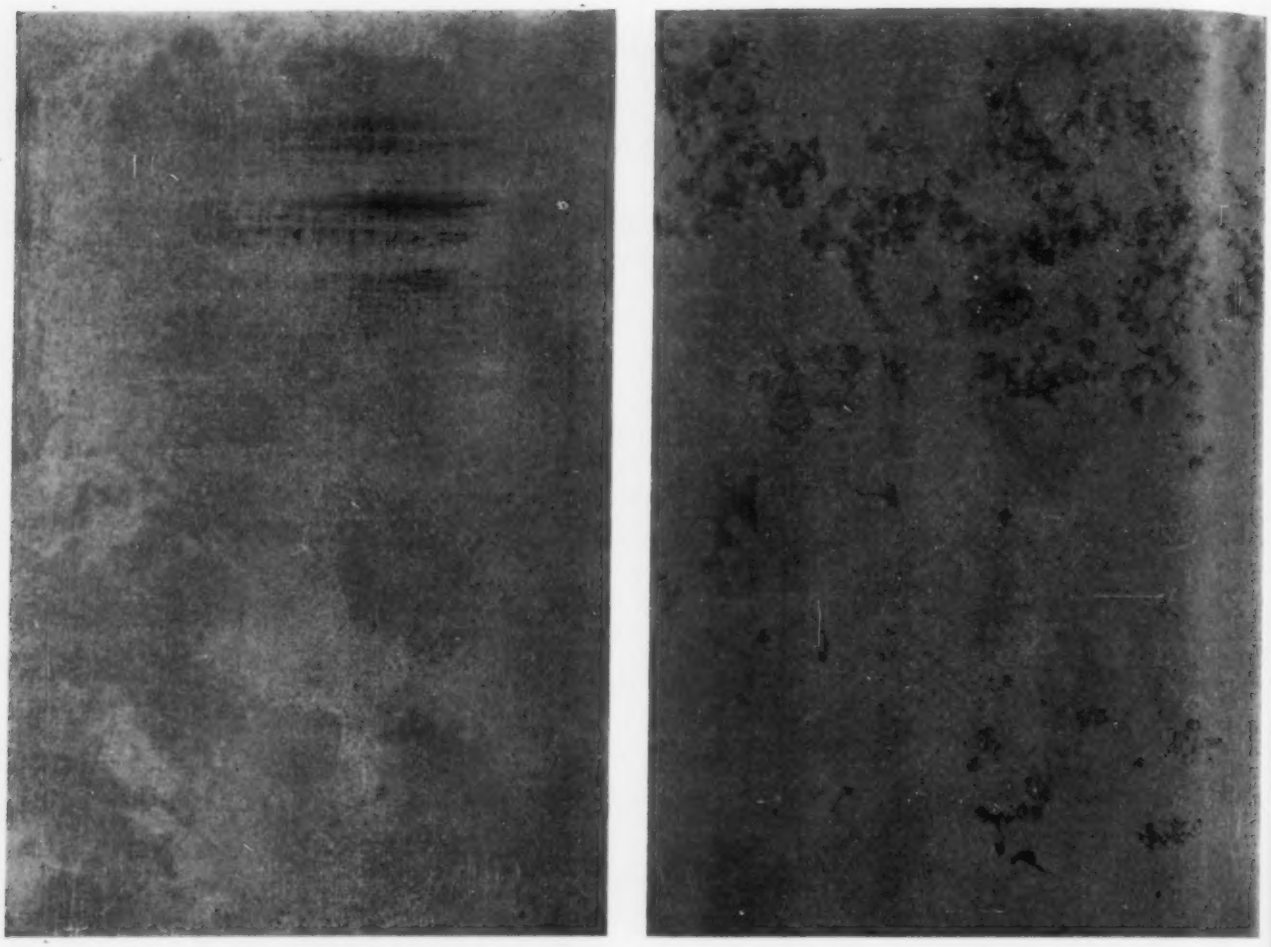
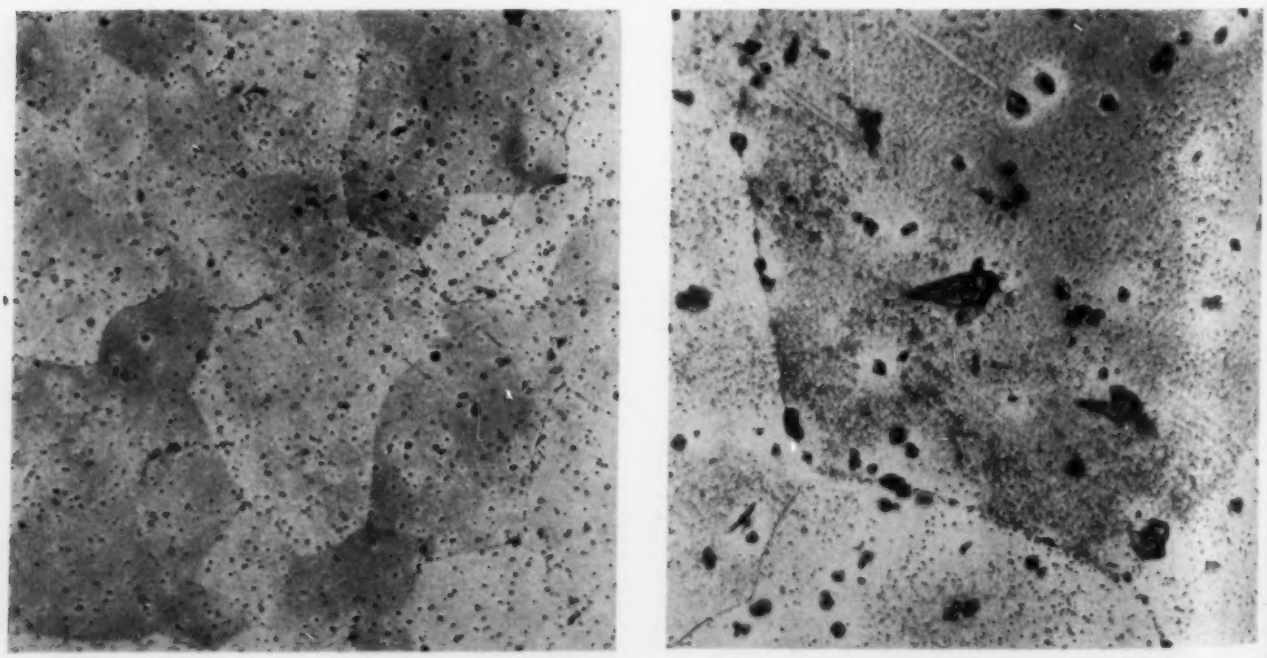
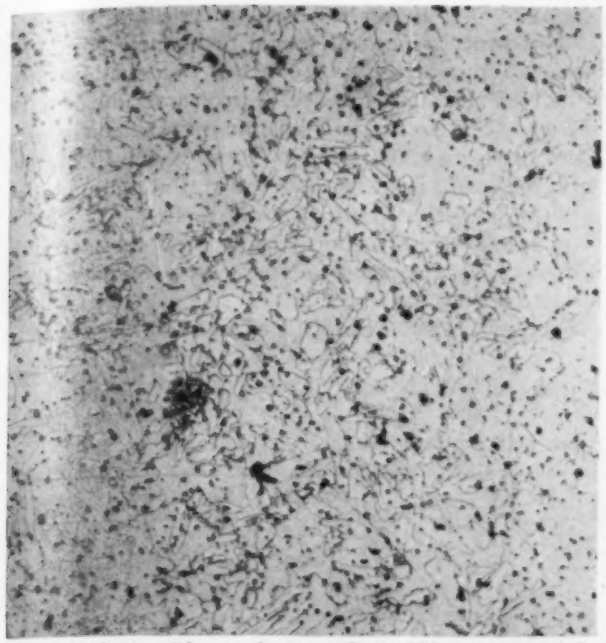


Fig. 1—Vertical sections of two manganese bronze ingots at approximately 2× etched in 10 per cent ammonium chloride solution for two weeks. A, left, shows no evidence of iron segregation, and B, right, shows marked segregation toward the top of the ingot.

Fig. 3A (left)—Photomicrograph at 100× of beta manganese bronze, polarized light, electrolytically polished and etched. Location 1A, Fig. 2. Fig. 3B (right)—Same as Fig. 3A, except magnification is 500×. Areas around the large iron compound segregations are largely denuded of the small iron compound segregations.





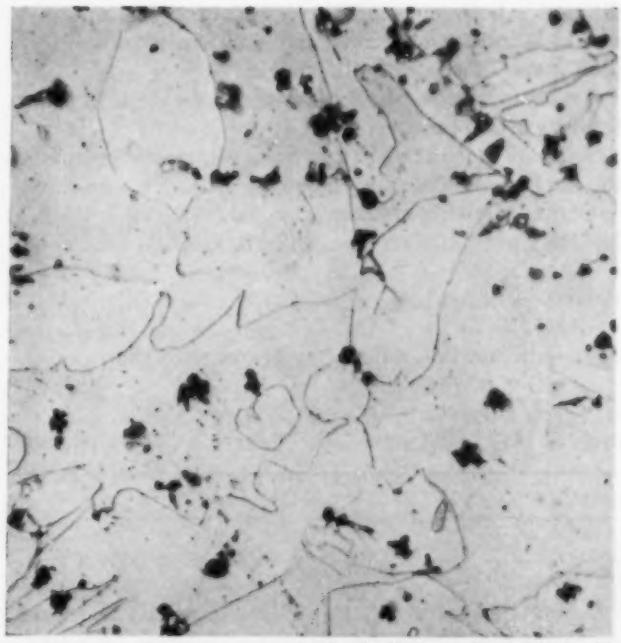
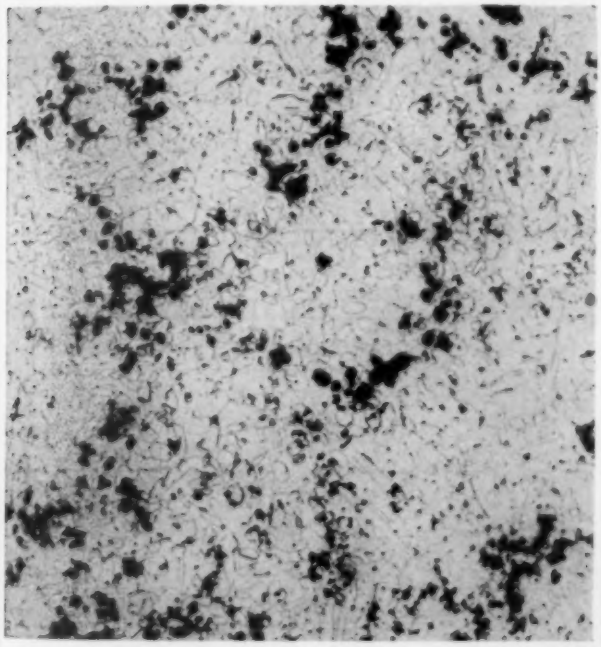


Fig. 4A (left)—Photomicrograph at 100× of an alpha-beta manganese bronze, polarized light, electrolytically polished and etched. Location 2B, Fig. 2, showing satisfactory iron compound distribution. Fig. 4B (right)—Same as Fig. 4A except magnification is 500×.

Fig. 5A (left)—Photomicrograph at 100× of an alpha-beta manganese bronze, polarized light, electrolytically polished and etched. Location 3B, Fig. 2, showing an unsatisfactory iron compound distribution. Fig. 5B (right)—Same as Fig. 5A except magnification is 500×.



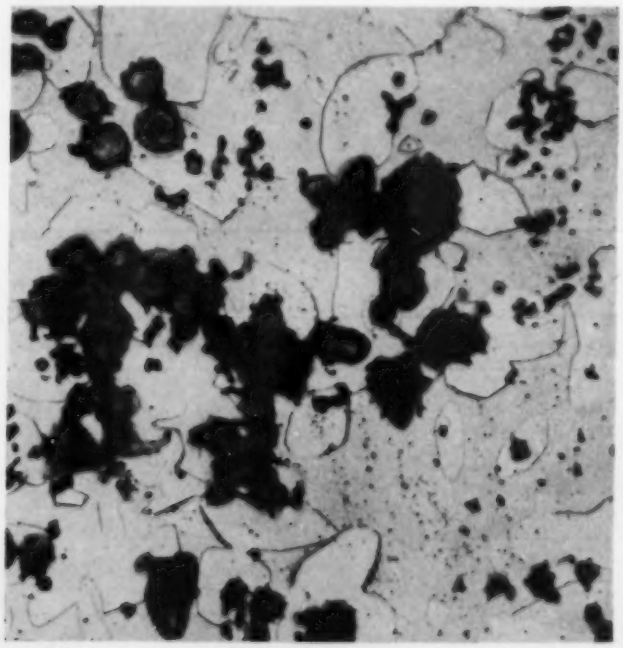


TABLE 1.—AVERAGE ANALYSIS OF SECTION SHOWN IN FIGURE 2.

Sample No.	2A1	2B2	2B3
Cu	55.71	58.83	58.29
Sn	0.30	0.40	0.30
Zn	41.62	38.03	37.55
Zn_e	48.25	44.25	44.81
Pb	0.31	0.23	0.24
Ni	Tr	0.05	0.05
Fe	0.81	1.00	1.96
Al	0.80	0.67	0.70
Mn	0.42	0.78	0.79
Si	0.03	0.01	0.03

Figure 5A, location 3B, of Fig. 2 at 100 magnifications, shows an alpha beta manganese bronze, with a marked segregation of the iron compound. Figure 5B is the same location at 500 magnifications. See Table 1, sample number 2B3, for average analysis of this section.

The symbol Zn_e in the tables is used to denote the zinc equivalents, which were calculated by using Gillet's equivalents.

Spectrographic examination was made of the sections shown in Fig. 1. The location of the spectrographic sparks is indicated in Fig. 2. There was no discernible

difference between sparks 4, 5 and 6 on section "A." Sparks 8 and 9 were much higher in iron and silicon than spark 7 on section "B."

An examination of Fig. 1 would indicate that these results were to be expected.

Some of the corrosion products were removed from the surface of 2B3 and spectrographically examined with the following results:

	2B2 Metal	2B3 Corrosion
Fe	0.70 %	20.0 %
Si	0.008	1.10
Ratio Fe/Si	88.	18.

This indicates that silicon segregates with the iron.

Laboratory Melting Tests

Test heats using commercial manganese bronze ingots of known analysis were melted in a small gas fired crucible laboratory furnace. Silicon was added to the different heats as a calculated amount of silicon bearing alloy, such as PMG (copper 71.06, silicon 22.24, and iron 6.16 per cent). A 50/50 silicon aluminum alloy was also used to add silicon to the melt.

A pyrometer having a chromel alumel thermocouple was used to determine temperatures. Standard keel block test bars were poured.

Tables 2, 3 and 4 show the effect of adding silicon to the manganese bronze heats.

TABLE 2.—PMG ALLOY USED FOR SILICON ADDITIONS.

HEATS	A	B	C	D
		Furnace Charges		
Ingot	100%	99.32%	98.70%	96.60%
PMG	———	0.45	0.90	2.25
Zinc	———	0.23	0.4	1.15
Total charge	100.0%	100.0%	100.0%	100.0%
Furnace temperature	1875°F	1870°F	1885°F	1880°F
Pouring temperature	1840°F	1840°F	1840°F	1820°F
		Physical Properties		
Yield point	32000	30000	29500	26000
Tensile strength	77600	77600	81300	59000
Elongation	11.5%	15.0%	16.0%	5.0%
		Chemical Analysis		
Cu	55.41	55.41	55.26	55.49
Sn	0.10	0.10	0.10	0.10
Zn	42.16	42.21	42.55	42.55
Zn_e	49.36	48.75	48.82	49.97
Pb	Tr	Tr	Tr	Tr
Ni	Tr	Tr	Tr	Tr
Fe	0.85	0.85	0.82	0.70
Al	1.00	0.92	0.89	0.88
Mn	0.48	0.51	0.38	0.22
Si	Tr	Tr	Tr	0.14
Si expected	0.0	0.10	0.20	0.50

TABLE 3.—PMG ALLOY USED FOR SILICON ADDITIONS.

HEATS	E		F		G		H	
			Furnace Charges					
Ingot	100.0%		99.32%		98.64%		96.60%	
PMG	—		0.45		0.90		2.25	
Zinc	—		0.23		0.46		1.15	
Total charge	100.0%		100.0%		100.0%		100.0%	
Furnace temperature	1870°F		1875°F		1880°F		1880°F	
Pouring temperature	1845°F		1840°F		1840°F		1860°F	
	Metal	Sludge	Metal	Sludge	Metal	Sludge	Metal	Sludge
Furnace Products	98.8%	0.0%	97.9%	1.0%	93.7%	5.2%	90.9%	7.85%
			Physical Properties					
Yield Point	25000	—	22000	—	22000	—	25000	—
Tensile Strength	63000	—	63500	—	60700	—	62400	—
Elongation	43.0%	—	41.5%	—	41.5%	—	30.0%	—
			Chemical Analysis					
Cu	58.25%	—	59.36%	57.39%	59.11%	54.29%	59.53%	57.68%
Sn	0.35	—	0.22	Tr	0.34	Tr	0.37	Tr
Zn	38.37	—	38.82	—	38.47	—	38.34	—
Zn_e	45.56	—	45.16	—	44.36	—	45.50	—
Pb	Nil	—	Nil	—	Tr	—	Nil	Tr
Ni	Nil	—	Nil	Tr	Nil	Tr	Nil	Tr
Fe	1.35	—	1.18	4.65	0.95	8.77	0.62	2.59
Al	0.86	—	0.77	1.14	0.68	1.57	0.72	1.73
Mn	0.45	—	0.45	0.68	0.45	0.58	0.28	0.35
Si	Tr	—	Tr	0.33	Tr	1.15	0.14	0.60
Si expected	Nil	—	0.10	—	0.20	—	0.50	—

There was no evidence of a sludge formation in heat A. Heats B, C and D showed increasing sludge formation as silicon was increased. No attempt was made to remove the sludge from the metal in these heats.

The sludge which forms when silicon is added has a tendency to float close to the top of the molten metal, and one would expect the metal in the bottom of the castings and test bars to be more free from the sludge forming constituents than the top of the castings and test bar feeding heads. The samples for chemical analysis and the test bars were taken from the bottom of the castings. This might account for the silicon being lower than expected.

There were no noticeable differences in microstructures. The structures were beta with the iron compound well distributed.

There was no evidence of a sludge formation in heats E and I. However, heats F, G, H, J, K and L showed increasing sludge formation as the silicon additions were increased. The sludge was held back in the crucible by using a skimmer while pouring.

There were no noticeable differences in the microstructures on heats E to L inclusive. The structures were an alpha beta manganese bronze of approximately 50% alpha, with the iron compound well distributed.

It is difficult to account for all the iron, aluminum, manganese and silicon due to the non-homogeneous condition of the sludge and the metal. In general, as the silicon increases there is a tendency for the aluminum, manganese and iron to decrease in the metal, and to increase in the sludge. The sludge consists of a compound high in iron, with considerable entertainment of molten metal.

Summary and Conclusions

Small amounts of impurities in manganese bronze may result in the following conditions:

1. Iron, silicon, aluminum and manganese concentrate in sludge formed during the melting operation.
2. The sludge has a tendency to float close to the top of the molten metal.
3. A silicon content of 0.1 per cent or under will cause enough sludge to form so that it is noticeable on pouring the metal.
4. The amount of sludge formed increases as the silicon content increases.
5. The sludge formation increases as the metal cools from furnace temperature to pouring temperature.
6. There is a tendency for the elongation to decrease as the silicon increases.
7. Sludge in the metal will cause thin castings to misrun.
8. The higher the iron content of the charge the more it will concentrate in the sludge with a given silicon content increases.

Acknowledgment

The generous aid furnished by the Chemical and Metallurgical divisions of the Industrial Laboratory of Mare Island Naval Shipyard is gratefully acknowledged. The assistance of Joseph Bentley, Master Molder, and his staff was of material aid in the prosecution of the work.

Discussion

Chairman: H. M. ST. JOHN, Crane Co., Chicago

Co-Chairman: A. K. HIGGINS, Allis-Chalmers Mfg. Co., Milwaukee

GEO. P. HALLIWELL [1]: The author has brought to our attention a subject so frequently infused into discussions of manganese bronze that it would sometimes seem that they are synonymous. This is unfortunate, because segregations of iron-rich particles can be traced generally to faulty metallurgical practice, either in the original compounding of the alloy or in the foundry.

Iron is added to manganese bronzes to decrease the grain size with a resultant beneficial effect upon mechanical properties. It

[1] Director of Research, H. Kramer & Co., Chicago, Ill.

TABLE 4.—ALUMINUM SILICON ALLOYS USED FOR SILICON ADDITIONS.

HEATS	I		J		K		L	
Furnace Charges								
Ingot	100%		99.8%		99.6%		99.2%	
Silicon Aluminum	—		0.2		0.4		0.8	
Total Charge	100%		100.0%		100.0%		100.0%	
Furnace temperature	1880°F		1880°F		1875°F		1920°F	
Pouring temperature	1840°F		1840°F		1835°F		1840°F	
Physical Properties								
Yield point	—		21000		23000		28500	
Tensile strength	—		63500		63500		61000	
Elongation	—		50.0%		46.5%		21.0%	
	Metal	Sludge	Metal	Sludge	Metal	Sludge	Metal	Sludge
Furnace Products	98.9%	0.0%	96.5%	2.1%	95.1%	3.7%	88.6%	9.9%
Chemical Analysis								
Cu	60.08	—	60.45	54.54	60.55	55.19	60.61	54.23
Sn	Tr	—	0.05	0.05	0.05	0.05	0.05	0.05
Zn	36.94	—	36.76	31.60	36.73	34.56	36.53	32.15
Zn_2	43.58	—	45.01	—	45.85	—	46.43	—
Pb	Tr	—	Tr	Tr	Tr	Tr	Tr	Tr
Ni	Tr	—	Tr	Tr	Tr	Tr	Tr	Tr
Fe	1.47	—	0.99	10.89	0.79	6.93	0.68	9.44
Al	0.83	—	1.16	1.29	1.13	1.70	1.33	1.75
Mn	0.68	—	0.59	0.59	0.63	0.67	1.33	1.75
Si	Tr	—	Tr	1.04	0.12	0.90	0.19	1.67
Si expected	Tr	—	0.10	—	0.20	—	0.40	—

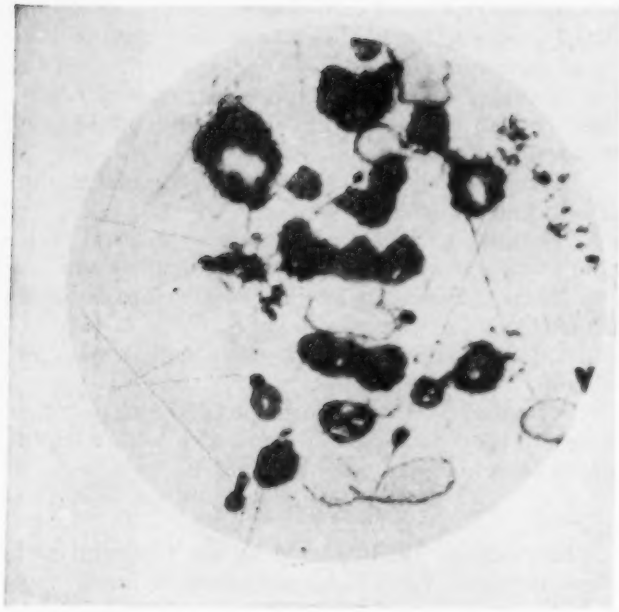


Fig. 6A—Unetched. Mag. 1500×.

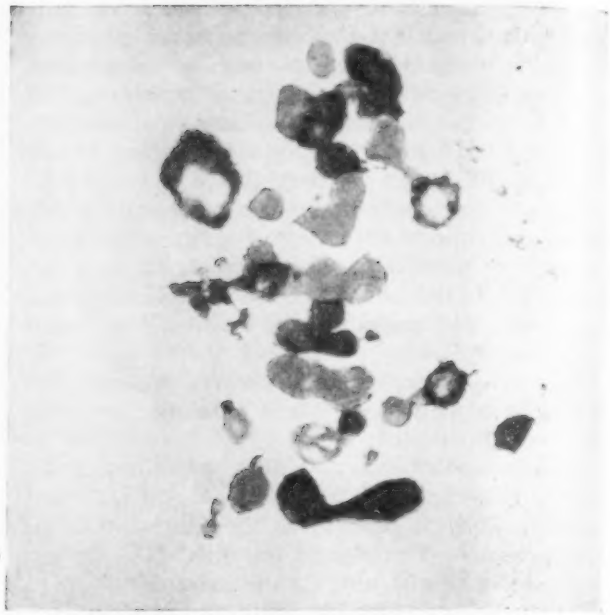


Fig. 6B—Etched. Mag. 1500×.

Fig. 6C—Unetched. Mag. 100×.

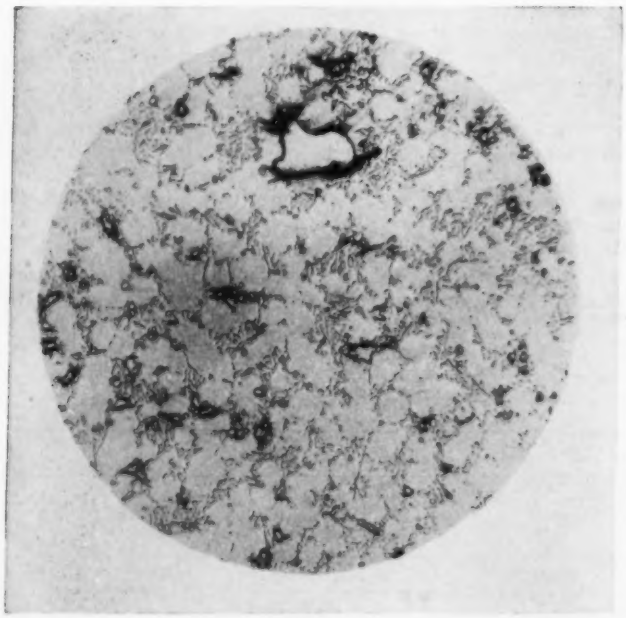
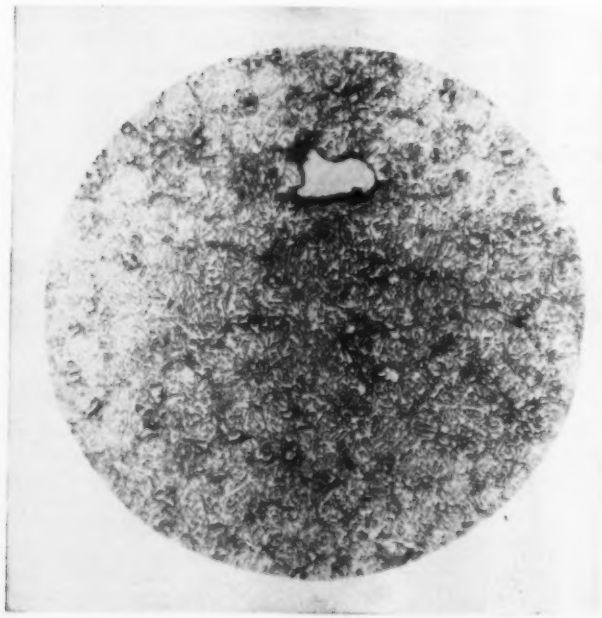


Fig. 6D—Nital etch. Mag. 100×.



may be added as metal or as a suitable hardener. It is claimed by some investigators that better mechanical properties are obtained by the use of hardeners, although this has not been the writer's experience. When properly alloyed, iron is soluble in the liquid state as discreet particles of the iron-rich compound, such as shown by Mr. Dalbey in Fig. 3 A.

It is unfortunate that the author used the electrolytic etch, as the conventional aluminum oxide and broad cloth technique produce better results. It is difficult to distinguish, in Fig. 3 B, between lead, holes, iron-compound and the effects of tarnish. The area around the large particles which appears to be denuded of iron may be real, but the writer has seen such a condition removed by a light mechanical polish on broad cloth.

If the iron has not been properly alloyed it will occur as segregations of hard metallic particles. The presence of silicon in some of these particles indicates that part of the iron and manganese has been oxidized and combined with silica. The source of the silicon may be the sand on returned gates, sprues, etc., or from the crucible or furnace lining. An area consisting of the normal iron-rich constituent and iron-rich silica particles is seen in Fig. 6 A and 6 B at 1500 diam. In the unetched condition (Fig. 6 A) the normal iron-rich constituent is seen as a light colored phase outlined in a back ground of about the same tone. The slag particles show as dark gray. Etched lightly with a weak solution of acidulated ferric chloride, the normal iron-rich particles become black and the slag particles remain essentially the same. In this manner the two constituents are readily distinguished. Such slag, however, should rise to the top of the metal and be skimmed off before stirring or pouring.

A second type of iron-rich material results when the iron or hardener is not fully dissolved. This frequently happens when attempts are made, after zinc and aluminum are added, to increase the iron content. A piece of copper iron hardener is seen in Figs. 6 C-E. The unetched condition is shown in Fig. 6 C and

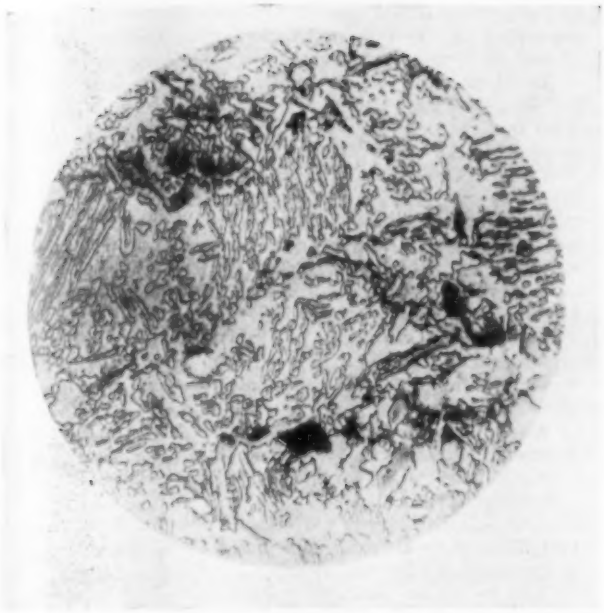


Fig. 6E—Etched. Mag. 500×.

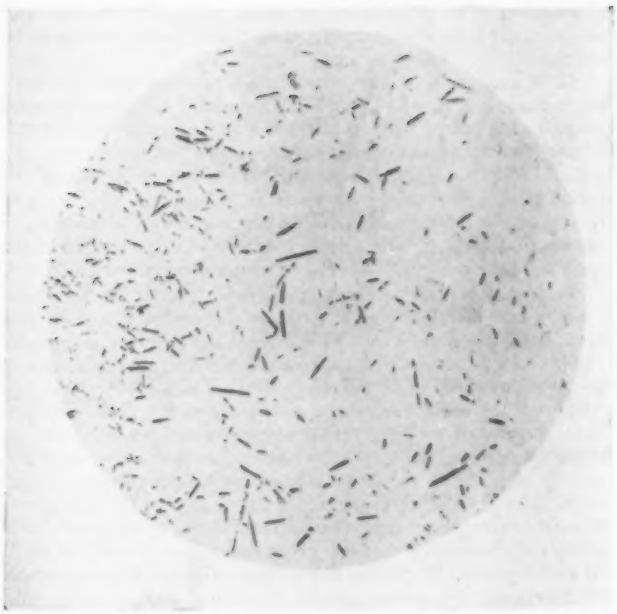
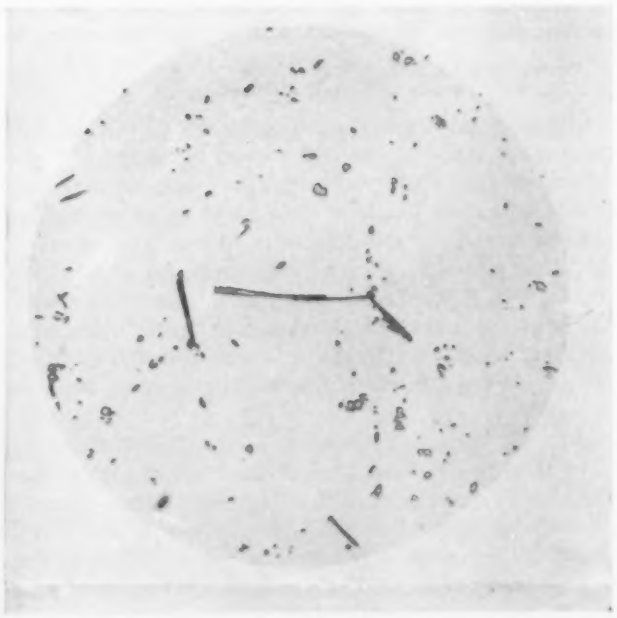


Fig. 6F—Unetched. Mag. 100×.

Fig. 6G—Unetched. Mag. 100×.



etched in Fig. 6 D at 100× and Fig. 6 E at 500×. High iron content is confirmed by its reaction to the etchant, 4 per cent nitric acid in alcohol (Nital). The internal structure appears to be the eutectoid (alpha plus eta) shown in the copper iron diagram (1).

Test bars containing such hard particles are difficult to machine and will usually show low mechanical properties. If the iron particles are of the order of a few thousandths of an inch in diameter, the fractured test bar will show small pin point mounds on one section of the bar and corresponding sharp pointed depressions on the opposite section. Such a fracture is accompanied by decreased properties, although they may not necessarily be below specifications limits. If silicon is present as an alloying element it is found precipitated as small needles or plates uniformly distributed throughout the alloy. Fig. 6 F shows its appearance in a low iron alloy and Fig. 6 G in a high iron alloy.

Several years ago, in a discussion of a paper by Dr. Gillett on "The Role of Silicon in Non-Ferrous Castings," [2] the writer presented data on the mechanical properties of manganese bronzes containing from 0 to 0.6 per cent silicon. For every 0.1 per cent silicon present the mechanical properties of a manganese bronze containing 58 per cent copper and one per cent each of aluminum and iron were affected as follows:

Tensile Strength	+1600 psi.
5% Yield Strength	+4000 psi.
% Elongation	—4.5
Brinell Hardness	+8

The rapid increase of the yield strength is of particular interest.

When silicon is desired in a manganese bronze containing normal amounts of iron, there will always be a loss of both silicon and iron, such as was found by the authors of the present paper. However, in the above mentioned alloys [2] no difficulty was experienced from the formation of a slag. That it was formed is evident from the loss of iron in the original alloy and the loss of about half the silicon added.

It is not clear from Mr. Dalbey's paper whether each heat of the same general copper content was made from the same batch of metal. If so, the progressive decrease in iron, heats E to H, would be expected. It is rather surprising that no silicon was retained in heats F and G. At least a few hundredths would be expected, unless the PMG hardener was not thoroughly immersed in the liquid.

In general, the properties given in the paper are lower than those the writer is accustomed to associate with this alloy and composition. This is especially true of the alloys with high silicon contents, heats H, K and L. This may be due in part to the use of a different test bar. We use the ⅝ in. web-webbert molded and poured flat.[3] The use of the term "yield point" is very misleading. It is generally accepted that a yield point does not exist in copper-brass alloys. The Navy specifies yield strength at 0.2 per cent offset, while A.S.T.M. recommends the yield strength at "0.5 per cent elongation while under load," as an alternative. Both these yield strengths are commonly used, but they should be and are generally identified, as they vary with respect to each other in the higher strength bronzes. Whereas the offset yield is lower in the low strength bronzes it is considerably higher than the 0.5 per cent yield strength in the 100,000-pound manganese bronze.

In conclusion, the writer should like to emphasize that segregations of hard spots in manganese bronze are not endemic to that alloy, but are the results of faulty metallurgical practice either when the alloy is first made or later melted. If any of the iron and manganese becomes oxidized, it is a metallurgical axiom that they combine with the nearest silica available to form a slag. Free

silicon is seldom present in the usual manganese bronzes and never in the high strength type, if optimum properties are to be obtained.

G. H. CLAMER[1]: I was particularly interested in Mr. Dalbey's paper because it may have a bearing upon the problem now under investigation before Subcommittee B-1 of Committee B-5, ASTM. I have been chairman of that subcommittee for many years. To a task group within that subcommittee has been delegated the problem of explaining hard spots in manganese bronze. The task group submitted a questionnaire to persons competent to discuss that subject. The majority were of the opinion that hard spots are caused by undissolved iron containing carbon.

I have never investigated manganese bronze for its silicon content, nor the effect thereof. There is no restriction on silicon content in the present ASTM specifications covering manganese bronze. As to silicon pickup, recently we had an experience in melting aluminum in an induction furnace with a siliceous lining. Although there was no silicon pickup when the aluminum was held around its melting point, when the temperature was increased considerably beyond the melting point there was a silicon pickup of about 2 per cent. Manganese bronze contains aluminum. The reaction of the same on the silicate of the lining may account for the presence of silicon in that alloy. Manganese bronze when molten reaches the reaction temperature. It is worth while to investigate silicon pickup in manganese bronze for that reason.

I have never previously heard of the silica sludge described by Mr. Dalbey as being present in manganese bronze.

J. F. EDNIE[2]: It might be helpful in this discussion to report some of our experiences, some years ago, in regard to hard spots in manganese bronze. We found that a hard spot was always associated with a segregation of iron; that is, the hard spot was always iron-rich. There was one puzzling feature, namely, the presence of such iron-rich segregations did not necessarily lead to machining difficulties. But always when machining difficulties were encountered, the iron-rich particles were responsible. We had occasion to isolate some of these hard spots which were causing machining difficulties and we found that the Brinell hardness of these spots varied considerably, but went as high as 300 or more.

In all cases the carbon content would range from 0.7 to 1.4 per cent, the manganese up to 10 per cent and the silicon up to 1 per cent. I think this information is a very important addition to what Mr. Dalbey has been bringing to our attention.

In conclusion, I think the method of manufacture of manganese bronze is an important consideration to remedy this trouble. It is possible to make the alloy containing many hard spots. Our company, after considerable research on this subject went exclusively to the use of electric furnaces for the manufacture of manganese bronze. Since that time this problem of segregation has been completely eliminated and we have never encountered a case of difficult machining.

MR. DALBEY *(author's closure)*: The author wishes to thank Messrs. Halliwell, Clamer and Ednie for their constructive discussions.

Replying to Mr. Halliwell's comment as to whether heats E to H were made from the same batch of metal. They were made from a lot of commercial ingot all of which had the same heat number stamped on each ingot.

It is believed that the PMG was in solution. It will be noted that the amount of iron in the sludge is considerably in excess of the iron added in the PMG.

The figures for yield point were arrived at by the divider method, and are approximate yield strengths.

[1] Ajax Metal Co., Philadelphia.
[2] Duquesne Smelting Corporation, Pittsburgh.

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1. *Metals Handbook*, 1939 edition, p. 377, ASM, Cleveland, Ohio.

2. H. W. Gillett, "The Role of Silicon in Non-Ferrous Castings," TRANSACTIONS, American Foundrymen's Association, vol. 46, p. 413 (1938).

3. G. H. Glamer, "Test Bars of 85-5-5-5 Their Design and Some Factors Affecting Their Properties," TRANSACTIONS, American Foundrymen's Association, vol. 54, p. 1 (1946).

SOME EFFECTS OF MELTING PRACTICE
ON PROPERTIES OF MEDIUM-CARBON LOW-ALLOY CAST STEEL

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ABSTRACT

The notched-bar impact properties and the hardenability of medium-carbon, manganese-molybdenum cast steels produced by eight different melting practices were investigated. Three of the practices identified as basic, fully oxidized and blocked; basic, fully oxidized, blocked, and finished under a reducing slag; and acid, fully oxidized and blocked, were studied more extensively.

The principal influence of melting practice on the notched-bar impact properties appeared to be through control of the sulphur content and sulphide distribution. In general, if sulphur content increased or sulphides were found as a network pattern in the grain boundaries, the notched-bar values were low.

In these tests the acid steels were, generally, lower in notched-bar impact values than steels produced by the basic practices. This difference, apparently, is related to the higher sulphur content of the acid steels.

Variations in hardenability in some steels could not be accounted for by the chemistry of the steels. However, there appeared to be no distinct or consistent influence of any one type of melting or deoxidation practice on hardenability.

FROM TIME TO TIME in the production of cast steel, heats are produced having unusual characteristics that cannot be attributed to composition alone. These peculiarities are then ascribed to some effect of the melting or deoxidation practice. Often the specific variable can be isolated by a thorough metallurgical study, but not infrequently the cause is only a matter of speculation.

The object of the present study was to compare the properties of steel produced by different melting methods but under otherwise comparable conditions, and to investigate those conditions that might account for the variations in properties not entirely accounted for by the chemical composition.

The research work was conducted on steels produced as 350-lb. melts in a three-phase, direct-arc furnace, rather than on steels selected from various industrial sources representing the different methods of production. While this means of comparison suffered the limitation of being on the laboratory scale, it was believed that the advantages of dealing with a fewer number of variables more than compensated for the small size of the melt, and would make it possible to solve some of the problems that made it hitherto impossible to compare quantitatively steels produced by commercial furnaces.

For the preliminary investigation of melting methods, four acid and four basic procedures were tried, some in each group being deliberately adjusted to what may be considered unsound practice. Later, attention was narrowed down to one acid and two basic methods, and a number of heats were made by each procedure to accumulate data of more significance than could be furnished by one test. The large variation in methods of preparing the preliminary heats was intended to cover the extreme ranges likely to be encountered in commercial foundries and to aid in revealing the causes for those discrepancies in properties not explained by chemistry.

It was not the purpose of the work to study the physicochemical reactions in the bath and slag as such, but rather to study what effects the reactions would have on the resultant products. Effects of final deoxidation were investigated by varying the aluminum addition and by using titanium along with the aluminum.

Mechanical Properties

The properties of primary concern were notched-bar impact value and hardenability. The toughness of the quenched and tempered steels was evaluated by notched-bar impact tests at testing temperatures from room temperature to −60 F (−50 C). This test has been shown to furnish information, not given by the tensile test, regarding the quality of the steel. A fracture test conducted on 1¼-in. sections of quenched and tempered bars was expected to furnish additional information on the quality of the steel.

The hardenability of the steels produced by the various methods was subject to considerable study since composition alone frequently has been inadequate to

*NOTE: This paper is based on work done in whole under Contract No. OEMsr-450 between Battelle Memorial Institute and the Office of Scientific Research and Development, which assumes no responsibility for the statements contained herein.

explain hardenability variations in commercial practice. By standardizing on a steel of one general analysis, it was possible to investigate the effect of melting and deoxidation practice on hardenability.

Experimental Investigations

A 400-lb. capacity three-phase direct-arc furnace was used to make both the acid and basic heats. Each heat totaled 350 lb. A majority of the heats were deoxidized initially with 2½ lb. aluminum per ton in the ladle. A cast was then made of two double-legged keel blocks, each weighing about 65 lb., and again after treating the remaining metal in the ladle with 3 lb. per ton of high-carbon ferrotitanium. Thus, for a given heat, cast blocks deoxidized with aluminum alone and also with aluminum plus ferrotitanium were available for testing.

Three heats received an initial ladle deoxidation of ¾ lb. aluminum per ton, with the second and third cast of these heats receiving the deoxidizing treatments outlined above. The final composition desired was 0.30 per cent carbon, 1.60 per cent manganese, 0.35 per cent silicon, 0.035 per cent maximum phosphorus and sulphur, and 0.35 per cent molybdenum.

The 1¼ in. square by 15 in. long keel legs used for the fracture study and notched-bar testing were heated at 1600 F (870 C) for 2 hr. and water quenched, followed immediately by a 1200 F (649 C) tempering treatment aiming for a Brinell hardness range of 240 to 260. All of this material was water quenched from the tempering temperature.

Fracture examinations were made on 1¼-in. square heat-treated bars notched and broken in two positions by loading them as a beam in a universal-type testing machine at its maximum speed.

Standard 1-in.-diameter bars for end-quench hardenability testing were machined from as-cast stock. These bars were heated in a graphite crucible to protect them from oxidation, and held at 1600 F (870 C) for 20 min. before transferring to the jig for end quenching.

Notched-bar testing temperatures, using the V-notched Charpy specimens, ranged from room temperature to –60 F (–51 C). Duplicate specimens were held at the testing temperature for a minimum of 15 min. Triple-notched Izod bars were also used but only for testing at room temperature.

Specimens for microscopic examination were taken from a keel from each cast of each heat. These were examined and rated for sulphide and oxide cleanliness and for the type of sulphide inclusion.

Preliminary Investigation

A preliminary investigation of the basic and acid practices consisted of using four different melting procedures for each type of lining. These are outlined as follows and are described in greater detail in the Appendix where heat logs representing each practice are given:

Type of Heat	Silicon and Manganese Additions
Basic or acid, fully oxidized and blocked.	All silicon and manganese added to the furnace at the block.
Basic or acid, fully oxidized, and not blocked.	All silicon and manganese added to the ladle.
Basic, fully oxidized, blocked, and finished with a reducing slag.	One-half of the manganese added to the furnace at the block and the other half plus the silicon added 5 min. before the tap.
Acid, fully oxidized, blocked, and with silicon reduced from slag.	One-half of the manganese added to the furnace at the block and the other half added to the ladle.
Basic, partially oxidized, blocked, and finished with a reducing slag.	One-half of the manganese added to the furnace at the block and the other half plus the silicon added 5 min. before the tap.
Acid, partially oxidized and blocked.	All the silicon and manganese added to the furnace at the block.

In the preceding tabulation, "fully oxidized" means that the melt-down carbon was between 0.35 and 0.45 per cent, and that sufficient iron ore or mill scale was fed into the furnace to obtain a vigorous boil which resulted in a carbon drop of 25 to 35 points. "Partially oxidized" means that the melt-down carbon was between 0.20 and 0.25 per cent and that no iron ore or mill scale was added; only a few points of carbon drop were obtained between melt-down and blocking.

Either pig iron and ferromanganese or pig iron, ferrosilicon, and ferromanganese were used to block the heats, and the holding time after the addition was timed to be 5 min. in all cases, except when a reducing slag was made in basic practice or when silicon was reduced from the slag in acid practice. In those two cases, the holding time after the block was about 60 min. and 20 min. respectively. The basic reducing slag was developed by removing the first slag before the block, adding about 3 lb. of ferromanganese (equivalent to 0.72 per cent manganese increase) with the pig iron, and building up a new slag with burnt lime and fluorspar.

Periodic Treatment of Slag

The new slag was periodically treated with powdered carbon until the water-quenched slag sample became white and gave off an acetylene odor. It was necessary in some heats to make further pig iron additions to adjust the carbon content. Silicon was reduced from the slag of the fully oxidized and blocked acid heat by adding half of the total ferromanganese one minute after the block and raising the bath temperature so that in about 21 min. after the block a tapping temperature of 3200 F (1760 C) was obtained.

Upon completion of the preliminary investigation of the heats outlined above, a more intensive study of several of the melting practices was undertaken. The study centered about a comparison of basic furnace heats which were fully oxidized, blocked, and finished with a reducing slag, with basic- and acid-furnace heats which were fully oxidized and blocked.

Four to five additional heats of each of these three types were made to see whether the preliminary results were a matter of chance or whether they were characteristic of the melting practice employed. The charge for these heats contained some high-sulphur steel scrap to build up the residual sulphur to that content most

CHARPY VALUES, FT.-LBS

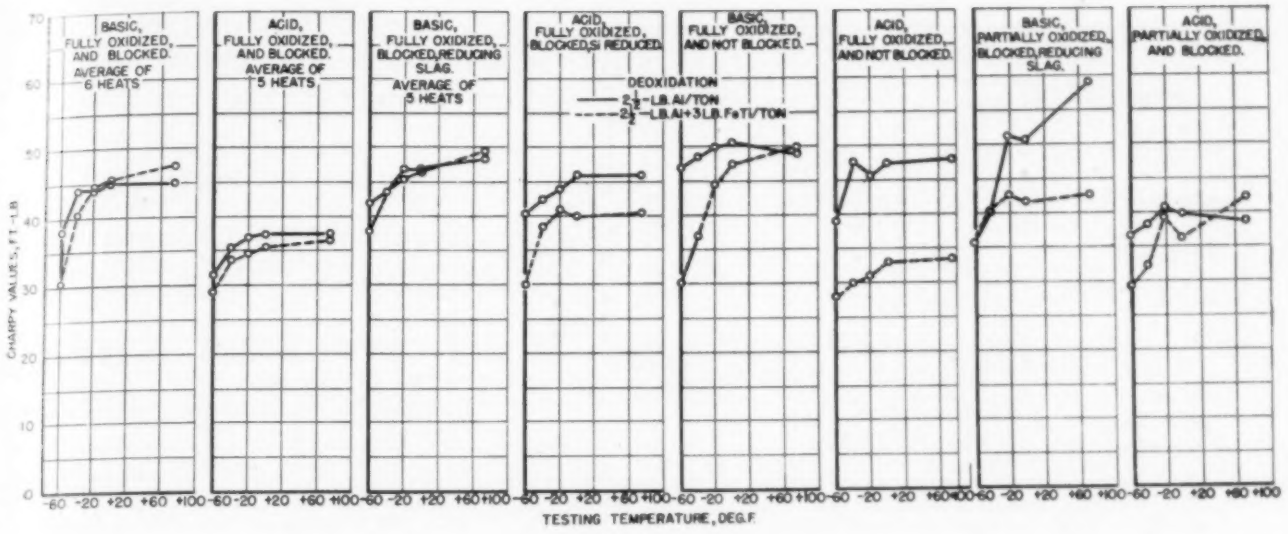


Fig. 1—Comparison of Charpy values of cast steels made with various melting and deoxidation practices. (Single heat results except where noted). Water quenched from the tempering temperature.

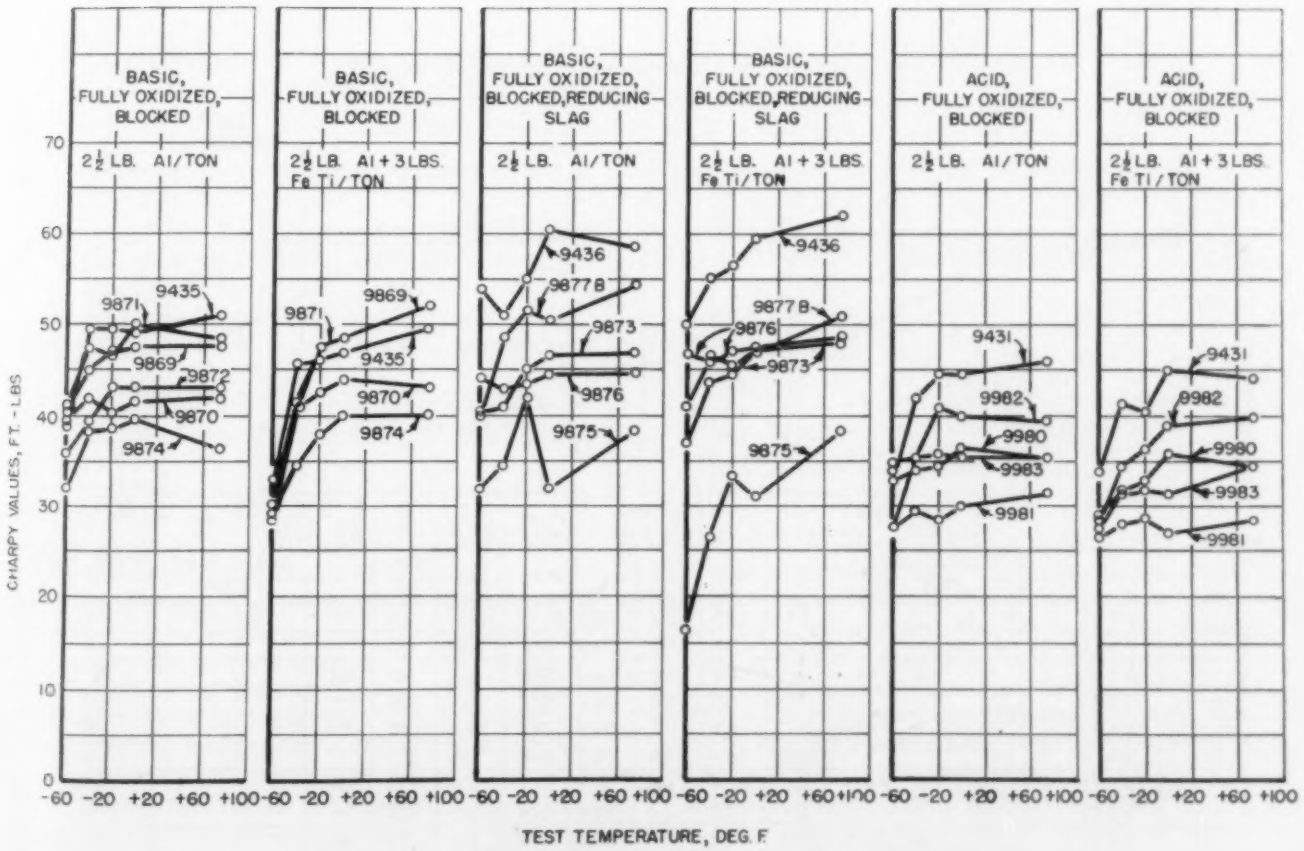
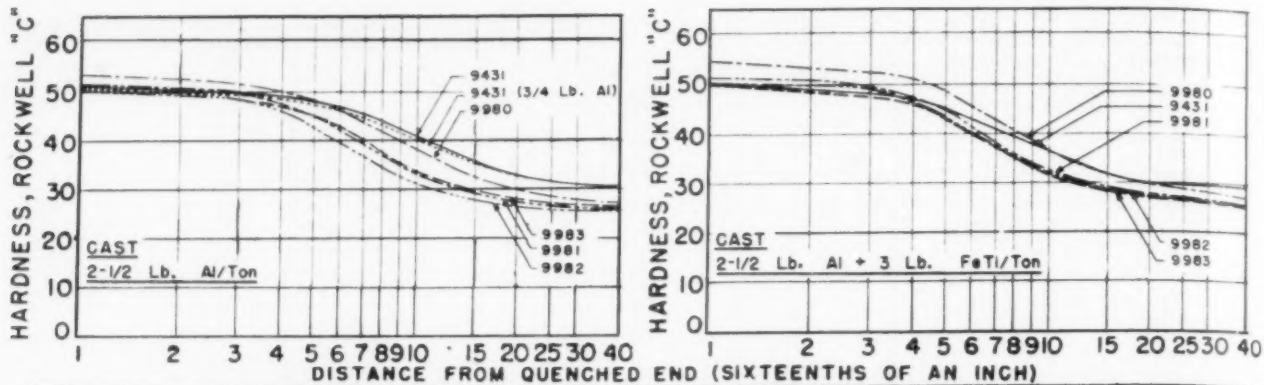


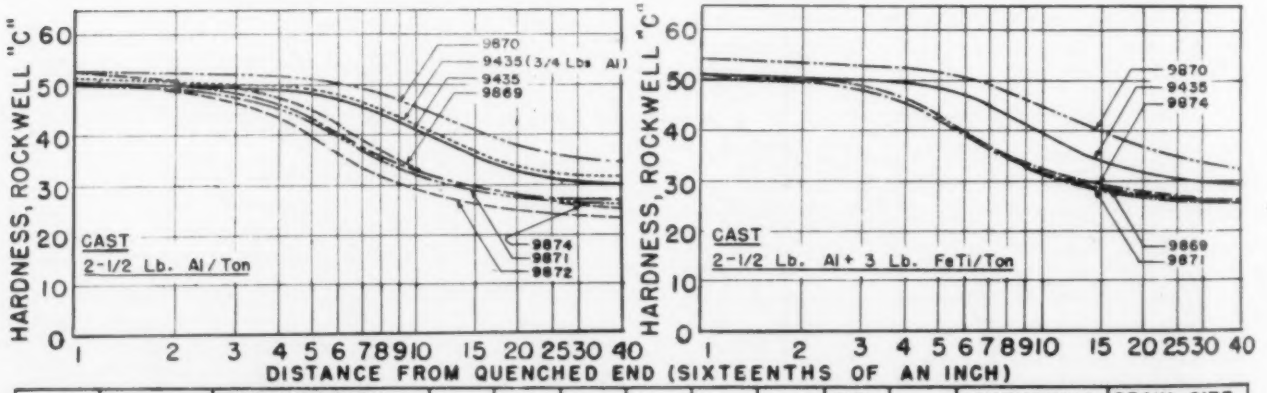
Fig. 2—Charpy values of individual heats of cast steels grouped according to melting and deoxidation practice. Water quenched from the tempering temperature.



KEY	HEAT NO.	DEOXIDATION, LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCH. TEMP.	GRAIN SIZE
.....	9431	3/4 Al	.29	1.81	.43	.020	.017	.37	.01	—	1600°F.	6
———	9431	2-1/2 Al	.29	1.81	.43	.020	.017	.37	.07	—	"	6
———	9431	" + 3 FeTi	.29	1.81	.43	.020	.017	.37	.10	.01	"	6-1/2
—·—·—	9980	"	.34	1.61	.20	.032	.028	.33	.04	—	"	8
—·—·—	9980	" + 3 FeTi	.34	1.61	.20	.032	.028	.33	.04	.01	"	7
—··—··	9981	"	.30	1.59	.33	.032	.027	.38	.06	—	"	7-1/2
—··—··	9981	" + 3 FeTi	.30	1.59	.33	.032	.027	.38	.06	.01	"	7
—·—·—	9982	"	.31	1.62	.44	.035	.028	.31	.05	—	"	7
—·—·—	9982	" + 3 FeTi	.31	1.62	.44	.035	.028	.31	.05	.01	"	6
—·—·—	9983	"	.32	1.63	.38	.032	.024	.33	.06	—	"	7
—·—·—	9983	" + 3 FeTi	.32	1.63	.38	.032	.024	.33	.06	.01	"	7

Fig. 3—End-quench hardenability curves for acid cast steel fully oxidized and blocked. Deoxidized with aluminum or aluminum plus ferrotitanium.

Fig. 4—End-quench hardenability curves for basic cast steel fully oxidized and blocked. Deoxidized with aluminum or aluminum plus ferrotitanium.



KEY	HEAT NO.	DEOXIDATION LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
.....	9435	3/4 Al	.31	1.63	.50	.020	.012	.33	.03	—	1600°F.	7
———	9435	2-1/2 Al	.31	1.63	.50	.020	.012	.33	.05	—	"	6
———	9435	" + 3 FeTi	.31	1.63	.50	.020	.012	.33	.03	.01	"	6-1/2
——·——	9869	"	.27	1.65	.37	.022	.017	.30	.06	—	"	9
——·——	9869	" + 3 FeTi	.27	1.65	.37	.022	.017	.30	.06	.01	"	6
——·—·	9870	"	.32	1.84	.46	.022	.018	.28	.06	—	"	9
——·—·	9870	" + 3 FeTi	.32	1.84	.46	.022	.018	.28	.06	.01	"	8-1/2
——··——	9871	"	.27	1.72	.34	.022	.019	.29	.05	—	"	7
——··——	9871	" + 3 FeTi	.27	1.72	.34	.022	.019	.29	.05	.01	"	6-1/2
———	9872	"	.30	1.81	.24	.024	.022	.22	.09	—	"	7
———	9872	" + 3 FeTi	.30	1.81	.24	.024	.022	.22	.09	.01	"	—
——·——	9874	"	.30	1.64	.32	.022	.021	.30	.07	—	"	7
——·——	9874	" + 3 FeTi	.30	1.64	.32	.022	.021	.30	.07	.01	"	7

common in production heats. Otherwise, the charge was the same as in the preliminary heats.

Discussion

In the discussion which follows, the principal emphasis is on the three methods of steel melting for which repeated heats were made. For the sake of brevity, these three methods will be referred to simply as "basic" for the basic heats fully oxidized and blocked; "basic reduced" for the basic heats fully oxidized, blocked, and finished with a reducing slag; and "acid" for the acid heats fully oxidized and blocked.

An effort was made to keep each heat close to the chosen composition of 0.30 per cent carbon, 1.60 per cent manganese, 0.35 per cent silicon, 0.035 per cent maximum sulphur and phosphorus, and 0.35 per cent molybdenum, but the rapid rates of reaction in the small furnace used for the melting and the variations in melting procedures made it extremely difficult to realize fully this objective.

Effect of Some Elements

Sulphur contents showed the expected variations depending on the melting practice used. Thus, as indicated in Table 1, sulphur was lowest in the "basic reduced" heats and highest in the "acid" heats. Phosphorus, also, was somewhat higher in the "acid" steels.

Nitrogen contents were affected to some extent by the reactions occurring during melting. For example, nitrogen contents of 0.021 and 0.018 per cent were found in the two heats, No. 9438 and 9433, only partially oxidized as compared to values of 0.010 and 0.015 per cent for the majority of the heats fully oxidized during melting. This trend in nitrogen content would be expected from the knowledge that a vigorous boil is the best known means of lowering nitrogen and preventing its re-solution.[1]

Table 2 and Fig. 1 summarize the data on Charpy notched-bar values for the cast steels made with the various melting practices. Fig. 1 is based on the average values of the repeated heats and the single preliminary heats. The individual heat results of the repeated series are presented in Fig. 2.

Too much weight should not be placed upon single heat results, and they are merely included for a general comparison.

The moderate scattering in Charpy values from heat to heat for one melting procedure may be expected from the nature of the test. However, where the spread is more pronounced, a reasonable explanation for the variation can sometimes be offered.

A study of the types and amounts of the nonmetallic inclusions is essential for a discussion of the quality of the steels as it is reflected in the notched-bar values. Cleanliness ratings, as determined by microscopic examination of the steels used for Charpy tests, are presented in Table 3. The sulphides are classified as to type following the system originated by Sims and Dahle,[3] and rated as to amount according to arbitrary standards. Oxides were rated for amount by setting up arbitrary standards for comparison. However, even the steels rated as dirty in the table would generally be classed as clean in commercial practice.

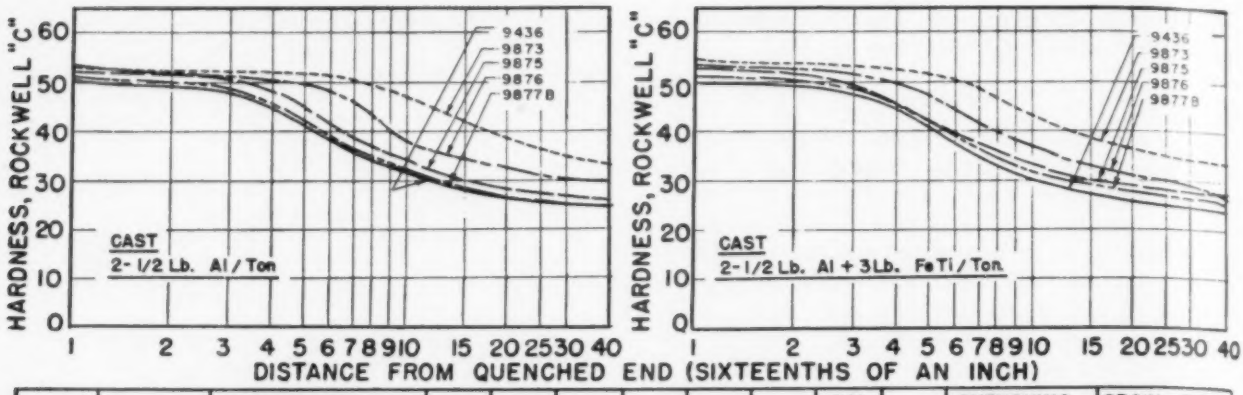
TABLE 1. CHEMICAL COMPOSITION AND GRAIN SIZE OF ALL THE HEATS

Heat No.	Chemistry, Per Cent C	Mn	Si	S	P	Mo	N	Fracture Grain Size Deoxidizer Per Ton ¾ Lb. Al	2½ Lb. Al	2½ Lb. Al Plus 3 Lb. FeTi
PRELIMINARY HEATS										
Basic—Fully Oxidized, but not Blocked										
9437	0.27	1.72	0.55	0.033	0.012	0.39	0.0110	–	6½	6
Basic—Partially Oxidized, Blocked, and Finished with a Reducing Slag										
9438	0.32	1.77	0.45	0.009	0.011	0.32	0.0210	–	7	6
Acid—Fully Oxidized, Blocked, and Silicon Reduced from Slag										
9434	0.35	1.52	0.36	0.024	0.016	0.41	0.0090	6	6½	7
Acid—Fully Oxidized, but not Blocked										
9432	0.27	1.62	0.33	0.023	0.012	0.37	0.0125	–	6	6
Acid—Partially Oxidized and Blocked										
9433	0.35	1.62	0.40	0.027	0.013	0.31	0.0180	–	7	7
REPEATED PROCEDURES										
Basic—Fully Oxidized and Blocked										
9435*	0.31	1.63	0.50	0.020	0.013	0.33	0.0105	7	6	6½
9869	0.27	1.65	0.37	0.022	0.017	0.30	0.0190	–	9	6
9870	0.32	1.84	0.46	0.022	0.018	0.28	0.0110	–	9	8½
9871	0.27	1.72	0.34	0.022	0.019	0.29	0.0140	–	7	6½
9872	0.30	1.81	0.24	0.024	0.022	0.22	0.0110	–	7	–
9874	0.30	1.64	0.32	0.022	0.021	0.30	0.0105	–	7	7
Basic Reduced—Fully Oxidized, Blocked, and Finished with a Reducing Slag										
9436*	0.28	1.63	0.41	0.011	0.007	0.34	0.0165	–	6	6
9873	0.32	1.62	0.33	0.011	0.021	0.32	0.0115	–	8	8
9875	0.28	1.65	0.41	0.011	0.020	0.27	0.0160	–	7½	7½
9876	0.32	1.74	0.27	0.011	0.018	0.36	0.0100	–	7½	8
9877B	0.28	1.61	0.35	0.017	0.018	0.31	0.0110	–	6	6½
Acid—Fully Oxidized and Blocked										
9431*	0.29	1.81	0.43	0.020	0.017	0.37	0.0105	6	6	6½
9980	0.34	1.61	0.20	0.032	0.028	0.33	0.0120	–	8	7
9981	0.30	1.59	0.33	0.032	0.027	0.38	0.0115	–	7½	7
9982	0.31	1.62	0.44	0.035	0.028	0.31	0.0195	–	7	6
9983	0.32	1.63	0.38	0.032	0.024	0.33	0.0135	–	7	7

* These heats were made as part of the preliminary series.

Sims and co-workers have shown the very important influence of distribution of sulphide inclusions upon the toughness of cast steel. When the oxide content of the steel is relatively high, the solubility of sulphides is low, and they precipitate as globules (Type I). When the oxide content is reduced by a critical amount of aluminum or other strong deoxidizers, the solubility of sulphides is increased and they precipitate as a eutectic at the grain boundaries (Type II).

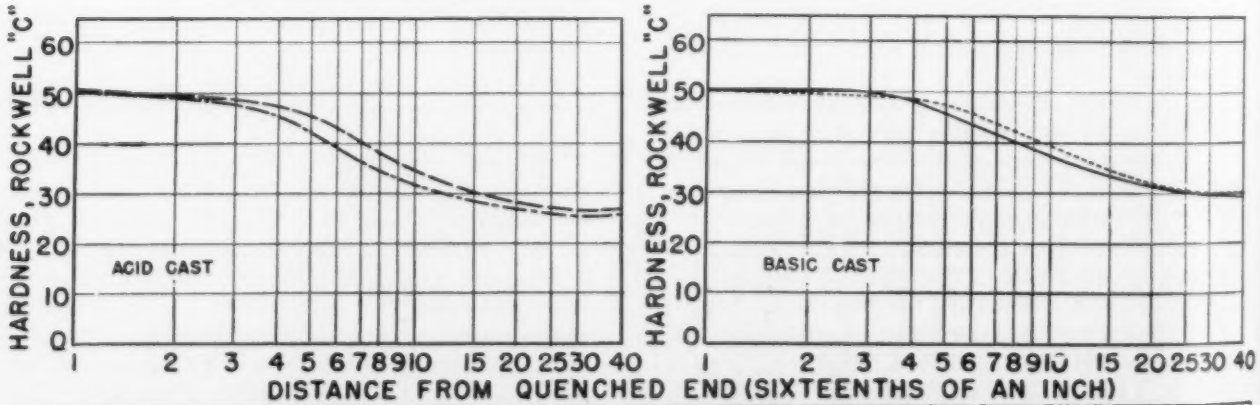
If the aluminum addition is greater than that required for deoxidation, complex sulphides are formed which have a low solubility and precipitate as large angular inclusions (Type III). Type II inclusions have a very detrimental effect on the notched-bar impact properties.



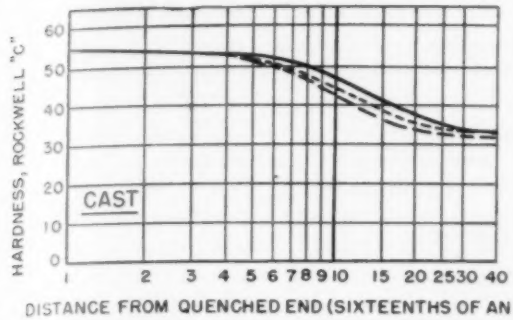
KEY	HEAT NO.	DEOXIDATION, LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
———	9436	2-1/2 Al	.28	1.63	.41	.011	.007	.34	.06	—	1600 °F.	6
———	9436	" + 3 FeTi	.28	1.63	.41	.011	.007	.34	.05	.01	"	6
- - - - - - -	9873	"	.32	1.62	.33	.011	.021	.32	.07	—	"	8
- - - - - - -	9873	" + 3 FeTi	.32	1.62	.33	.011	.021	.32	.07	.01	"	8
— — —	9875	"	.28	1.65	.41	.011	.020	.27	.09	—	"	7-1/2
— — —	9875	" + 3 FeTi	.28	1.65	.41	.011	.020	.27	.09	.01	"	7-1/2
———	9876	"	.32	1.74	.27	.011	.018	.36	.08	—	"	7-1/2
———	9876	" + 3 FeTi	.32	1.74	.27	.011	.018	.36	.08	.01	"	8
———	9877B	"	.28	1.61	.35	.017	.018	.31	.08	—	"	6
———	9877B	" + 3 FeTi	.28	1.61	.35	.017	.018	.31	.08	.01	"	6-1/2

Fig. 5—End-quench hardenability curves for basic cast steel fully oxidized, blocked and finished under a reducing slag. Deoxidized with aluminum or aluminum plus ferrotitanium.

Fig. 6—End-quench hardenability curves for basic and acid cast steel fully oxidized and not blocked. Deoxidized with aluminum or aluminum plus ferrotitanium.

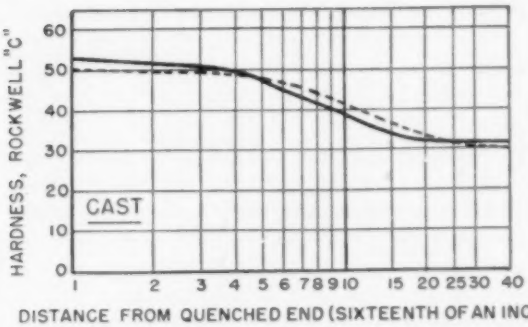


KEY	HEAT NO.	DEOXIDATION, LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
———	9437	2-1/2 Al	.27	1.72	.55	.033	.012	.39	.03	—	1600 °F.	
- - - - - - -	9437	" + 3 FeTi	.27	1.72	.55	.033	.012	.39	.04	.01	"	6
— — —	9432	"	.27	1.62	.33	.023	.012	.37	.03	—	"	6
— — —	9432	" + 3 FeTi	.27	1.62	.33	.023	.012	.37	.03	.01	"	6



KEY	HEAT NO.	DEOXIDATION, LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
———	9434	3/4 Al	.35	L52	.36	.024	.046	.41	.02	–	1600 °F.	6
———	9434	2-1/2 Al	.35	L52	.36	.024	.046	.41	.08	–	"	6-1/2
———	9434	" + 3 FeTi	.35	L52	.36	.024	.046	.41	.06	.01	"	7

Fig. 7—End-quench hardenability curves for acid cast steel fully oxidized and silicon reduced from the slag. Deoxidized with aluminum or aluminum plus ferrotitanium.



KEY	HEAT NO.	DEOXIDATION LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
———	9438	2-1/2 Al	.32	1.77	.43	.009	.011	.52	.08	–	1600 °F.	7
———	9438	" + 3 FeTi	.32	1.77	.43	.009	.011	.52	.09	.01	"	6

Fig. 8—End-quench hardenability curves for basic cast steel partially oxidized and finished under a reducing slag. Deoxidized with aluminum or aluminum plus ferrotitanium.

TABLE 2.—ROOM-TEMPERATURE IZOD, LOW-TEMPERATURE V-NOTCH CHARPY, AND BRINELL HARDNESS VALUES OF CAST STEEL MADE WITH VARIOUS MELTING AND DEOXIDATION PRACTICES

	2½ Lb. Al/Ton Deoxidation							2½ Lb. Al + 3 Lb. FeTi/Ton Deoxidation							¾ Lb. Al/Ton Deoxidation						
			Charpy Impact, Ft.-Lb.							Charpy Impact, Ft.-Lb.							Charpy Impact, Ft.-Lb.				
Heat No.	Izod, Ft-Lb	BHN	75 °F.	0 °F.	–20 °F.	–40 °F.	–60 °F.	Izod, Ft-Lb	BHN	75 °F.	0 °F.	–20 °F.	–40 °F.	–60 °F.	Izod. Ft-Lb	BHN	75 °F.	0 °F.	–20 °F.	–40 °F.	–60 °F.
BASIC HEATS—FULLY OXIDIZED AND BLOCKED																					
9435	50	243	51	49	49.5	49.5	39	50	249	49.5	47	46	45.5	33	50.5	241	51.5	49	48	49.5	47.5
9869	49.5	237	47.5	47.5	47	45	40.5	50	242	52	48.5	47.5	41	28.5							
9870	43.5	242	42	41.5	40.5	42	39.5	45.5	240	43	44	42.5	41	33							
9871	47.5	241	48.5	50	46.5	47.5	41	48.5	233	52	48.5	47.5	41.5	30							
9872	43.5	237	43	43	43	39.5	36	–	–	–	–	–	–	–							
9874	38.5	253	36.5	39.5	38.5	38.5	32	38	250	40	40	38	34.5	29							
Avg.	45.5	242	45	45	44	44	38	46.5	243	47.5	45.5	44.5	40.5	30.5							
BASIC HEATS—FULLY OXIDIZED, BLOCKED, AND REDUCING SLAG																					
9436	60.5	229	58.5	60.5	55	51	54	58	233	62	59.5	56.5	55	50							
9873	42.5	250	47	46.5	45	41	40.5	47.5	253	48	47	44.5	43.5	37							
9875	41	249	38.5	32	42	34.5	32	43.5	243	38.5	31	33.5	26.5	16.5							
9876	45	246	44.5	44.5	43.5	43	44	48.5	253	48.5	47.5	47	46	46.5							
9877B	50	248	54.5	50.5	51.5	48.5	40	47.5	239	51	47	45.5	46.5	41							
Avg.	48	244	48.5	47	47	43.5	42	49	244	49.5	46.5	45.5	43.5	38							
BASIC HEAT—FULLY OXIDIZED AND NOT BLOCKED																					
9437	50	240	49	50.5	50	48.5	47	47.5	241	50	47.5	44.5	37	30							
BASIC HEAT—PARTIALLY OXIDIZED, BLOCKED, AND REDUCING SLAG																					
9438	55	245	59	50.5	51	40	35.5	45.5	241	42.5	41.5	42.5	40.5	35.5							
ACID HEATS—FULLY OXIDIZED AND BLOCKED																					
9431	45	238	46	44.5	44.5	42	34	39.5	234	44	45	40.5	41.5	34	31.5	238	30.5	30.5	28	28	27.5
9980	37	241	35.5	36.5	34.5	34	33	36	241	34.5	36	33	32	29							
9981	37	235	31.5	30	28.5	29.5	27.5	31.5	238	28.5	27	28.5	28	26.5							
9982	45	240	39.5	40	41	35.5	35	45	251	40	39	36.5	34.5	28.5							
9983	42.5	241	35.5	35.5	36	35.5	27.5	36	240	34.5	31.5	32	31.5	27.5							
Avg.	41.5	239	37.5	37.5	37	35.5	31.5	36	241	36.5	35.5	34	33.5	29							
ACID HEAT—FULLY OXIDIZED, BLOCKED, AND SI REDUCED																					
9434	48	241	46	46	44	42.5	40.5	40.5	245	40.5	40	41	38.5	30	33	241	35	35	31.5	32	29
ACID HEAT—FULLY OXIDIZED AND NOT BLOCKED																					
9432	48.5	239	48	47.5	45.5	47.5	39	35.5	242	33.5	33	31	30	28							
ACID HEAT—PARTIALLY OXIDIZED AND BLOCKED																					
9433	40	240	38.5	39.5	40.5	38	36.5	38.5	245	42	36	39	32	29							

TABLE 3.—OXIDE AND SULPHIDE CLEANLINESS RATINGS AND TYPE OF FRACTURE OF THE CAST STEELS

Grouped According to Melting Practice

Heat No.	Deoxidation, Lb. per Ton	Background Oxides	Sulphides		Type of* Fracture (Aver. of 2 Breaks)	Per cent IG in Charpy Bars
			Type	Amount		
BASIC—FULLY OXIDIZED AND BLOCKED						
9435	¾ lb. Al	Dirty	III	Fairly clean	F	0
9435	2½ lb. Al	Slightly dirty	III	Clean	F	0
9869	Ditto	Ditto	III	Fairly clean	Trace of IG	0
9870	Ditto	Ditto	III	Ditto	F	0
9871	Ditto	Ditto	III	Clean	F	0
9872	Ditto	Ditto [1]	III	Fairly clean	F	0
9874	Ditto	Dirty [2]	III	Ditto	Trace of IG	½
9435	2½ lb.Al+	Slightly dirty [1]	III	Fairly clean	F	0
9869	3 lb. FeTi	Ditto	III	Ditto	Trace of IG	0
9870	Ditto	Dirty [2]	III	Ditto	Trace of IG	0
9871	Ditto	Slightly dirty	III	Clean	F	3
9874	Ditto	Dirty [3]	III	Fairly clean	Trace of IG	½
ACID—FULLY OXIDIZED AND BLOCKED						
9431	¾ lb. Al	Slightly dirty [2]	II	Slightly dirty	F	0
9431	2½ lb. Al	Ditto	III	Fairly clean	Trace of IG	0
9980	Ditto	Fairly clean	III, II	Slightly dirty	F, D	0
9981	Ditto	Dirty	II, III	Dirty	F, D	0
9982	Ditto	Fairly clean [2]	III	Slightly dirty	F, D	0
9983	Ditto	Dirty [1]	III	Dirty	F, D	0
9431	2½ lb.Al+	Fairly clean	III	Fairly clean	Trace of IG	Trace
9980	3 lb. FeTi	Ditto	III, II	Slightly dirty	F, D	0
9981	Ditto	Dirty	II, III	Dirty	F, D	0
9982	Ditto	Fairly clean [2]	III	Ditto	F, D	0
9983	Ditto	Ditto [1]	III	Ditto	F, D	0
BASIC—FULLY OXIDIZED, BLOCKED, REDUCING SLAG						
9436	2½ lb. Al	Fairly clean	III	Very clean	Trace of IG	8
9873	Ditto	Slightly dirty [1]	III	Ditto	10% IG	5
9875	Ditto	Clean	III	Ditto	40% IG	22
9876	Ditto	Slightly dirty [1]	III	Ditto	Trace of IG	3
9877B	Ditto	Ditto	III	Ditto	F	Trace
9436	2½ lb.Al+	Slightly dirty	III	Very clean	F	0
9873	3 lb. FeTi	Ditto [1]	III	Ditto	Trace of IG	4
9875	Ditto	Very clean [3]	III	Ditto	30% IG	14
9876	Ditto	Slightly dirty [2]	III	Ditto	Trace of IG	Trace
9877B	Ditto	Fairly clean	III	Ditto	90% F, 10% Facets	1
ACID—FULLY OXIDIZED, BLOCKED, Si REDUCED						
9434	¾ lb. Al.	Fairly clean [2]	II	Dirty	F	0
9434	2½ lb. Al	Ditto [1]	III	Slightly dirty	F	0
9434	Ditto +3 lb. FeTi	Ditto [3, 2]	III	Ditto	Trace of IG	0
BASIC—FULLY OXIDIZED, NOT BLOCKED						
9437	2½ lb.Al	Fairly clean	III	Slightly dirty	F	0
9437	Ditto + 3 lb. FeTi	Ditto	III	Ditto	F	0
ACID—FULLY OXIDIZED, NOT BLOCKED						
9432	2½ lb. Al	Fairly clean [1]	III	Fairly clean	F, D	0
9432	Ditto + 3 lb. FeTi	Ditto [3]	II	Slightly dirty	Trace of IG	0
BASIC—PARTIALLY OXIDIZED, BLOCKED, REDUCING SLAG						
9438	2½ lb. Al	Slightly dirty	III	Very clean	10% IG	6
9438	Ditto + 3 lb. FeTi	Dirty	III	Ditto	50% IG	8
ACID—PARTIALLY OXIDIZED AND BLOCKED						
9433	2½ lb. Al	Dirty [3]	III	Fairly clean	Trace of IG	1
9433	Ditto + 3 lb. FeTi	Fairly clean [3]	III	Ditto	5% IG	3

1. Few small Al_2O_3 clusters
2. Many small Al_2O_3 clusters
3. Few large Al_2O_3 clusters

* Key to Type of Fracture in Nick-Bend Test:
F Fibrous
IG Intergranular
D Dendritic

Non-Metallic Inclusions

No Type II sulphide inclusions were observed in the basic steels. Sulphide inclusions of Type II appeared most frequently in the higher sulphur-acid furnace steels despite the fact they were deoxidized with the same quantity of aluminum as were the basic furnace steels. The occurrence of Type II sulphide inclusions appears to account for the generally lower notched-bar impact values of the acid steels as compared to the basic furnace steels. The lower sulphur-acid steels have the highest Charpy values of the acid series when deoxidized with 2½ lb. Al/ton, but when deoxidized with ¾ lb. Al/ton Type II sulphides occurred and significantly lower notched-bar impact values were obtained (See Heats 9431 and 9434 in Table 2).

The addition of ferrotitanium with the aluminum appeared not to lessen the number of Type II sulphide inclusions. In the titanium split of Heat No. 9432 eutectic sulphides were observed and lower impact values were obtained than for the aluminum split which had Type III sulphide. In general, very little difference existed between aluminum-treated and the aluminum plus ferrotitanium-treated steels.

Rating of Fractures

Fractures obtained in Charpy tests and nick-bend fracture bars were rated as to the percentage of intergranular areas appearing on the fractured surfaces. These values are also given in Table 3. This type of fracture has been observed in previous studies,[2] and found to be prevalent in steels containing relatively high nitrogen and aluminum in which an aluminum nitride precipitate may form at the boundaries of large primary grain.

Heat No. 9875 in the "basic reduced" series, Table 2 and Fig. 2, deviates considerably from the rest in its group. Data in Table 3 show that both the aluminum and aluminum plus titanium splits of this heat have the highest percentages of intergranular fracture in the Charpy test bars and fracture bars of all the steels investigated. The split of the partially oxidized basic heat, No. 9438, which was treated with both aluminum and titanium, is another example of relatively low Charpy values attributable to a large percentage of intergranular fracture. The occurrence of intergranular fracture tends to produce erratic Charpy values.

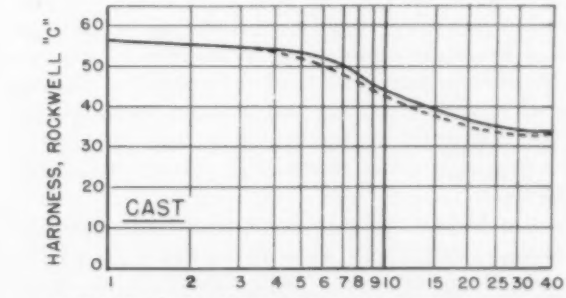
Intergranular areas revealed in the fracture test and in the fracture of Charpy notched-bar impact specimens were far more common for the basic furnace heats, especially those that were finished under a reducing slag, than for the acid furnace heats. If it is considered that the aluminum in higher sulphur-acid steels was tied up as complex sulphides, then it is probable that a greater excess of aluminum was available for forming the aluminum nitride precipitate in the basic steels. A reducing slag tends to permit higher recoveries of aluminum and, also, higher nitrogen pickup.

Room-Temperature Izod Tests

All the Izod test-bar data in Table 2 were obtained from steels which had been water quenched from the tempering temperature. The conclusions which may be drawn from the room-temperature Charpy test-bar data apply to the Izod test-bar data.

Hardenability curves of the cast specimens were plotted in Fig. 3 to 9, inclusive. All steels were of sufficient hardenability to insure full hardening on quenching the notched-bar and fracture specimens used in the investigation. A critical study of these curves shows that many of the differences in the depth of hardening may be attributed to chemical factors. For example, it seems likely that the greater hardenability of Heat No. 9876 over Heat No. 9875 can be attributed to slightly higher contents of carbon, manganese, and molybdenum.

On the other hand, it is obvious that all differences in hardenability are not directly indicated by the chemistry or grain-size determinations and that some other factors are influencing hardenability. It may further



KEY	HEAT NO	DEOXIDATION, LB. PER TON	C	Mn	Si	S	P	Mo	SOL. Al	Ti	QUENCHING TEMP.	GRAIN SIZE
———	9433	2-1/2 Al	.35	1.68	.40	.027	.013	.31	.10	—	1600°F.	7
- - - -	9433	" + 3 FeTi	.35	1.68	.40	.027	.013	.31	.07	.01	"	7

Fig. 9—End-quench hardenability curves for acid cast steel partially oxidized and blocked. Deoxidized with aluminum or aluminum plus ferrotitanium.

be seen that these undetermined factors are not intimately dependent on the melting practice employed. These conclusions are best illustrated by citing several examples.

Hardenability

Inspection of the hardenability curves from six heats which are nearly alike in chemical composition show that not all are of the same hardenability. Heats for comparison are as follows: 9983, 9873, 9877B, 9436, 9435, and 9869. Three melting practices are represented and all but Heats 9873 and 9435 show comparable hardenability in the end-quenched test. The latter two steels show somewhat greater depths of hardening than the others, indicating the probability that there are other factors besides composition and grain size affecting the hardenability.

A further indication that other factors are involved in the hardenability of steel is obtained by comparing Heats 9873 and 9876. By reason of its higher manganese and molybdenum contents, Heat No. 9876 would be expected to have the greater depth of hardening; actually, it has a lower hardenability than Heat No. 9873. Both heats were prepared by the same melting and deoxidation practice.

Summary

The effects of melting practice on notch-bar properties, hardenability, and cleanliness of electric-furnace cast steels melted in a 400-lb. capacity direct-arc furnace were investigated. Eight widely different melting practices were employed initially in preparing steels of the same general composition. A more comprehensive study was later made on the following three methods of melting:

1. Basic furnace practice with the heat fully oxidized, then blocked after the first slag was removed, and finished under a reducing slag,

2. Basic furnace practice with the heat fully oxidized and blocked, and

3. Acid furnace practice with the heat fully oxidized and blocked.

The influence of melting and deoxidizing practice on notched-bar properties was manifested in the type and distribution of nonmetallic inclusions. The somewhat higher sulphur contents of the series of acid steels prepared by fully oxidizing and blocking as compared with the two series of basic steels seemed to account for the generally lower notched-bar properties of the acid steels.

The notched-bar properties of steels with some of the sulphides existing as Type II, and of steels with a high percentage of coarse intergranular areas, were lower than those for steels containing neither Type II sulphides nor the coarse intergranular areas. In the investigation, Type II sulphides occurred more frequently in the acid steels, while intergranular areas were more prevalent in the basic steels, especially those which were finished under a reducing slag.

As a rule, the hardenability of steels made with the same melting practice and with identical melting stock could be attributed to chemical factors. There were a few exceptions, however, where steels with chemical compositions that were nearly alike differed somewhat in hardenability. Variations of this nature were not restricted to one type of melting practice, but prevailed in all types where repeated heats were made.

The results of the investigation seem to bear out the indications that melting practice has but little effect on the notched-bar properties and the hardenability, provided the metal is sound, and the quantity, size, and distribution of the inclusions, the composition of the steel and its microstructure are substantially the same.

Acknowledgment

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APPENDIX

The following section contains heat logs for a representative heat of each melting practice covered in the investigation.

A list of the metallic agents used to make the heats is given below:

In the Charge

1. Low-carbon steel scrap.
2. Low-carbon, high-sulphur (0.11 per cent) steel punchings.
3. Silvery pig—2 per cent C, 10 per cent Si, 1.25 per cent Mn, and 0.090 per cent P.
4. Chateaugay pig—4.50 per cent C, 0.79 per cent Si, 0.20 per cent Mn, and 0.03 per cent P.
5. High-carbon 80 per cent ferromanganese.

Block

1. Chateaugay pig.

Alloy Additions

1. High-carbon 80 per cent ferromanganese.
2. Low-carbon 80 per cent ferromanganese.
3. 75 per cent ferrosilicon.
4. Low-carbon 60 per cent ferromolybdenum.
5. Washed metal, 3.77 per cent C.

Ladle Deoxidizers

1. Aluminum wire.
2. High-carbon 17 per cent ferrotitanium.

Log of Heat No. 9870

Practice: Basic, Fully Oxidized and Blocked.

General Description: Bottom was covered with steel on which pig, FeMn, and limestone were charged and then covered with the remaining steel. Limestone and fluorspar covered the charge. Ore was added under the electrodes after a molten pool was formed.

The bath was boiled down to 0.10 per cent C, enough slag was removed to avoid trapping of alloy additions, and the heat was then blocked. Carbon, manganese, and silicon were adjusted at the block and the heat was held five min. before tapping.

The heat was deoxidized in the ladle. Aluminum was added when the ladle was about one-quarter full.

Material	Wt., Lb.	Time	Remarks
Low-Carbon, high sulphur steel scrap	32.0		Calculated analysis of charge: 0.45 C, 0.50 Mn, 0.25 Si, 0.035 S
Low-carbon steel scrap	288.0		
Silvery pig	6.76		
Chateaugay pig	23.15		
High-carbon FeMn	1.24		
Limestone	6.50		
Burnt lime	9.0		
Fluorspar	3.0		
		11:35	Charged and power on
Ore	1.0	12:03	Under electrodes
		12:05	Low voltage
		12:11	Melted
		12:15	No. 1 Preliminary. 0.31 C
		12:34	Stirred

		12:37	No. 2 Preliminary, 0.27 C, 0.35 Mn
		12:59	No. 3 Preliminary, 0.23 C, 0.31 Mn
		1:20	Stirred
		1:23	No. 4 Preliminary, 0.16 C, 0.29 Mn
		1.32	Stirred
		1:40	No. 5 Preliminary, 0.10 C
		1:55½	Slag off
Chateaugay pig	6.5	1:56	Blocked
Fe Si	2.0	1:56	
FeMo	1.98	"	
High-carbon FeMn	7.12	1:57	
		2:02	Tapped, 3000 F
Aluminum (2½ lb./ton)	0.437		Aluminum added to the ladle
		2:04	Start of pour, 2880 F 2 keel blocks, 1 ingot
FeTi (3 lb./ton)	0.262	2:05	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		2:07	End of pour, 2760 F

Final Analysis: 0.32 C, 1.84 Mn, 0.46 Si, 0.022 S, 0.018 P, 0.28 Mo, and 0.0110 N

Log of Heat No. 9437

Practice: Basic, Fully Oxidized and Not Blocked.

General Description: The same procedure as that in Heat No. 9870 was followed until the carbon has been boiled down to 0.10 per cent, then tapped, alloyed, and deoxidized in the ladle.

Material	Wt., Lb.	Time	Remarks
Low-carbon steel scrap	320.0		
Silvery pig	7.0		
Chateaugay pig	22.95		
High-carbon FeMn	1.29		
Limestone	6.50		
Fluorspar	3.0		
Burnt lime	9.0		
		12:01	Charged and power on
Ore	1.0	12:30	
		12:32	Low voltage
		12:47	Good boil
		1:00	No. 1 Preliminary, 0.32 C, 0.37 Mn
		1:12	No. 2 Preliminary, 0.31 C, 0.29 Mn
		1:35	No. 3 Preliminary, 0.19 C, 0.31 Mn
FeMo	2.04	1:35	
		1:40	Bath stirred
		1:45	Bath stirred
		1:47	No. 4 Preliminary, 0.16 C, 0.29 Mn
		1:50	Bath stirred
		1:52	Taped, 3250 F
FeSi	2.50		Added to the ladle
High-carbon FeMn	7.5		Ditto
Aluminum (2½ lb/ton)	0.437		Ditto
		1:54	Start of pour 2 keel blocks, 1 ingot
FeTi (3 lb/ton)	0.262	1:55	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		1:57	End of pour

Final Analysis: 0.27 C, 1.72 Mn, 0.55 Si, 0.033 S, 0.012 P, 0.39 Mo, and 0.0110 N

Log of Heat No. 9873

Practice: Basic, Fully Oxidized, Blocked, and Finished Under a Reducing Slag.

General Description: The same procedure as that in Heat No. 9870 was followed until the carbon has been boiled down to 0.10 per cent. At this time, the slag was removed and the heat blocked with pig followed by FeMn. A new slag was then built up and reduced with powdered coal.

When the slag was carbidic, manganese, molybdenum, carbon, and silicon were adjusted to final analysis and the heat held for five min. before tapping.

The heat was deoxidized in the ladle as in Heat No. 9870.

Material	Wt., Lb.	Time	Remarks
Low-carbon, high-sulphur steel punchings	32.0		
Low-carbon steel scrap	288.0		
Silvery pig	6.85		
Chateaugay pig	23.5		
High-carbon FeMn	1.24		
		1:47	Charged and power on
Ore	1.0	2:25	
		2:26	Low voltage
		2:29	Melted
		2:31	No. 1 Preliminary, 0.30 C, 0.47 Mn
		3:07	No. 2 Preliminary, 0.19 C
		3:20	Bath stirred
		3:29	No. 3 Preliminary, 0.09 C
		3:30	Slag off
Chateaugay pig	3.10	3:31	Blocked
High-carbon FeMn	5.48	3:31	Blocked
Limestone	6.50	3:32	Slag on
Burnt Lime	9.00	3:32	Slag on
Fluorspar	3.00	3:32	Slag on
		3:35	Started reducing the slag by periodic additions of powdered coal, burnt lime, and fluorspar
		3:46	No. 4 Preliminary, 0.12 C, 0.51 Mn
Chateaugay pig	5.60	3:56	Blocked
		4:03	No. 5 Preliminary, 0.15 C, 0.42 Mn
		4.15	Slag carbidic, brown
		4.19	No. 6 Preliminary, 0.20 C
FeSi	2.0	4:42	
FeMo	1.98		
Aluminum (2½ lb/ton)	0.437	4.47	Tapped, Al added to the ladle
		4:48	Start of pour 2 keel blocks, 1 ingot
FeTi (3 lb/ton)	0.262	4:49	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		4.50	End of pour

Final Analysis: 0.32 C, 1.62 Mn, 0.33 Si, 0.011 S, 0.021 P, 0.32 Mo, and 0.0115 N

Log of Heat No. 9438

Practice: Basic, Partially Oxidized, Blocked, and Finished with a Reducing Slag.

General Description: Bottom was covered with steel on which pig and FeMn were charged, then covered with the remaining steel. The slag-forming ingredients, burnt lime, alumina, and fluorspar covered the charge. No ore or limestone were used in the heat.

When the bath was completely molten, the slag was removed and the heat blocked and finished in the same manner as Heat No. 9873.

Material	Wt., Lb.	Time	Remarks
Low-carbon steel scrap	331.0		Calculated analysis of charge:
Silvery pig	8.0		0.25 C, 0.50 Mn, 0.25
Chateaugay pig	11.75		Si, 0.009 P, 0.025 S
High-carbon FeMn	1.29		
50-50 Lime-Alumina	6.75		
Fluorspar	4.00		
Burnt lime	6.00		
		9:40	Charged and power on
		10:30	Low voltage
		10:51	No. 1 Preliminary, 0.26 C, 0.44 Mn, Bath dead
		10:52	Slag off
Chateaugay	3.37	10:53	
Low-carbon FeMn	3.0	10:53	
Burnt lime	12.0	10:55	
Fluorspar	4.0	10:55	
		11:07	Slag molten, began reducing
		11:12	Brown, slightly carbidic slag
		11:17	No. 2 Preliminary, 0.37 C, 1.16 Mn
		11:25	Strong, carbidic slag
		11:35	Carbidic slag
		11:39	No. 3 Preliminary, 0.33 C, 1.16 Mn
FeSi	2.08	11:44	Bath stirred
FeMo	2.04	11:44	Ditto
FeMn	3.40	11:44	Ditto
Aluminum (2½ lb/ton)	0.437	11:48	Tapped, 3100 F, Al added to the ladle
		11:50	Start of pour 2 keeps blocks, 1 ingot
FeTi (3 lb/ton)	0.262	11:51	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		11:53	End of pour

Final analysis: 0.32 C, 1.77 Mn, 0.45 Si, 0.009 S, 0.011 P, 0.32 Mo, and 0.0210 N

Log of Heat No. 9980

Practice: Acid, Fully Oxidized and Blocked.

General Description: Bottom was covered with steel on which pig and FeMn were charged, then covered with the remaining steel.

Silica sand was used to maintain a slag covering on the melt. The amount and time of the sand addition varied somewhat from heat to heat, depending on the amount of slag obtained from the bottom.

The heat was boiled down to 0.10 per cent carbon and finished in the same way as the corresponding basic heat (Heat No. 9870).

Material	Wt., Lb.	Time	Remarks
Low-carbon, high-sulphur steel punchings	32.0		
Low-carbon, steel scrap	281.0		
Silvery pig	6.5		
Chateaugay pig	30.8		
High-carbon FeMn	1.24		
		10:07	Charged and power on
		10:41	Melted
Ore	5.0	10:46	
Sand	3.0	10:46	
Ore	5.0	10:47	
		11:04	No. 1 Preliminary, 0.39 C, 0.05 Mn, black slag
Ore	2.0	11:07	
		11:30	No. 2 Preliminary, 0.24 C, 0.04 Mn, light-brown slag
Ore	1.0	11:56	
		12:04	No. 3 Preliminary, 0.11 C, 0.04 Mn
Chateaugay pig	2.84	12:30½	Blocked
FeMo	2.04	12:30½	
Low-high carbon, FeMn	11.20	12:32	
Aluminum (2½ lb/ton)	0.437	12:35½	Tapped, 3150 F, Al added to the ladle
		12:37	Start of pour, 3050 F 2 keel blocks, 1 ingot
FeTi (3 lb/ton)	0.262	12:38	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		12:40	End of pour, 2930 F

Final Analysis: 0.34 C, 1.61 Mn, 0.20 Si, 0.032 S, 0.028 P, 0.33 Mo, and 0.0120 N

Log of Heat No. 9432

Practice: Acid, Fully Oxidized, and Not Blocked.

General Description: The same procedure as used on Heat No. 9980 was followed until the carbon was boiled down to 0.10 per cent, then tapped, alloyed, and deoxidized in the ladle as for the corresponding basic heat (Heat No. 9437).

Material	Wt., Lb.	Time	Remarks
Low-carbon steel scrap	313.0		
Silvery pig	6.10		
Chateaugay pig	31.0		
High-carbon FeMn	1.29		
Ore	17.0		
Limestone	1.0		
		11:49	Charged and power on
Sand	1.86		Added during meltdown
		12:52	Low voltage
		12:58	No. 1 Preliminary, 0.35 C, 0.32 Mn, thick, black slag
Ore	10.0	12:59	
Ore	2.0	1:05	
Ore	2.0	1:09	
		1:13	No. 2 Preliminary, 0.30 C, slag O.K., metal cold
Ore	2.0	1:14	
Limestone	1.0	1:17	
		1:24	No. 3 Preliminary, 0.13 C, 0.05 Mn, black, glossy slag
FeMo	2.04	1:26	
Ore	1.0	1:27	
		1:29	Bath stirred
		1:37	No. 4 Preliminary, 0.10 C, 0.06 Mn, slag O.K.
		1:39	Tapped, 3130 F
FeSi	1.37		Added to the ladle
High-carbon FeMn	8.09		Ditto
Carbon	0.18		Ditto
Aluminum (2½ lb/ton)	0.437		Ditto
		1:40	Start of pour, 3020 F 2 keel blocks, 1 ingot
FeTi (3 lb/ton)	0.262	1:41	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		1:43	Finish of pour, 2880 F

Final Analysis: 0.27 C, 1.62 Mn, 0.33 Si, 0.023 S, 0.012 P, 0.37 Mo, and 0.0125 N

Log of Heat No. 9434

Practice: Acid, Fully Oxidized, Blocked, and Silicon Reduced from the Slag.

General Description: The same procedure was used as in Heat No. 9980 until the carbon was boiled down to 0.10 per cent. The heat was blocked with pig and high-carbon FeMn to reduce silicon from the slag. The temperature was raised to aid the silicon reduction and the heat held until sufficient silicon was reduced, then tapped. Carbon and manganese were adjusted in the ladle prior to deoxidation.

Material	Wt., Lb.	Time	Remarks
Low-carbon steel scrap	314.0		
Silvery pig	6.37		
Chateaugay pig	29.75		
High-carbon FeMn	2.18		
		1:07	Charged and power on
Sand	8.0	1:40	
		1:45	Low voltage
		2:04	No. 1 Preliminary, 0.24 Si
Ore	5.0	2:05	
		2:15	No. 2 Preliminary, 0.27 C, 0.09 Mn, 0.03 Si. Good boil
Ore	1.0	2:16	
Ore	1.0	2:22	
		2:24	No. 3 Preliminary, 0.14 C, 0.08 Mn, 0.02 Si. Moderate boil
Ore	1.0	2:30	
		2:33½	No. 4 Preliminary, 0.10 C, 0.10 Mn, 0.02 Si
Chateaugay pig	2.41	2:34	Blocked
High-carbon FeMn	4.00	2:35	
FeMo	2.04	2:35	
		2:37	No. 5 Preliminary, 0.20 C, 0.60 Mn, 0.10 Si
		2:42	No. 6 Preliminary, 0.22 C, 0.57 Mn, 0.13 Si
Sand	2.0	2:42	
Sand	5.0	2:45	
		2:46	Slight boil, thick slag
		2:53	No. 7 Preliminary, 0.42 Mn
			Ditto
High-carbon FeMn	5.2		Ditto
Low-carbon FeMn	0.42	2:55	Tapped, 3220 F
Aluminum (¾ lb/ton)	0.127		Added to the ladle
		2:57	Start of pour, 3090 F 1 keel block, 1 ingot
Aluminum (1¾ lb/ton)	0.200	2:58	Al stirred in the ladle 1 keel block, 1 ingot
FeTi (3 lb/ton)	0.173	2:59	FeTi stirred in the ladle 1 keel block, 1 ingot
		3:00	End of pour, 2900 F

Final Analysis: 0.35 C, 1.52 Mn, 0.36 Si, 0.024 S, 0.016 P, 0.41 Mo, and 0.0090 N

Log of Heat No. 9433

Practice: Acid, Partially Oxidized and Blocked.

General Description: The charge was made up as in Heat No. 9980. Burnt lime was used to replace iron oxide in the slag. After the melt-down, the heat was immediately brought up to tapping temperature and blocked. The heat was finished in the same way as Heat No. 9980.

Material	Wt., Lb.	Time	Remarks
Low-carbon steel scrap	331.0		
Silvery pig	7.9		
Chateaugay pig	7.29		
High-carbon FeMn	1.29		
		2:28	Charged and power on
Burnt lime	3.75	2:31	
Crushed burnt lime	2.50	(2:53 to 3:02	Small amounts added periodically
		3:05	Low voltage
		3:08	No. 1 Preliminary, 0.23 C, 0.39 Mn, black, glossy, foaming slag
		3:17	No. 2 Preliminary, 0.20 C, 0.32 Mn, green, glossy slag. No boil
Crushed burnt lime	1.24	3:18	
		3:30	No. 3 Preliminary, 0.21 C
Chateaugay pig	2.92	3:33	
Washed metal	10.15	3:33	
FeSi	1.37	3:35	
FeMo	2.04	3:35	
Low-carbon FeMn	8.30	3:36	
Aluminum (2½ lb/ton)	0.437	3:38	Tapped, 3030 F. Al added to the ladle
		3:40	Start of pour, 2880 F. 2 keel blocks, 1 ingot
FeTi (3 lb/ton)	0.262	3:41	FeTi stirred in the ladle 2 keel blocks, 1 ingot
		3:43	End of pour, 2780 F.

Final analysis: 0.35 C, 1.62 Mn, 0.40 Si, 0.027 S, 0.013 P, 0.31 Mo, and 0.0180 N

DISCUSSION

Chairman: C. W. BRIGGS, Steel Founders' Society of America, Cleveland.

Co-Chairman: L. H. HAHN, Sivyer Steel Castings Co., Chicago.

CHAIRMAN BRIGGS: If the hardenability obtained cannot be accounted for by the chemical analysis, and if the steel does not harden sufficiently and all the factors we normally look for are reasonably present, then what do you do?

MR. KURA: In most cases, hardenability calculated from the composition does not match the observed hardenability very closely. The end-quench test is a simple one, and if the exact hardenability is desired, the end-quench test should be made. If any doubt exists when comparing the calculated hardenability with the observed hardenability, a second end-quench test should be made to confirm the results of the first one.

CO-CHAIRMAN HAHN: I am wondering if some of the discrepancies that result are due to incomplete aluminum recovery. What we are concerned with is the efficiency of aluminum recovery.

MR. KURA: The cause of intergranular areas in some of our Charpy bars is explained in detail by Dr. Lorig and Mr. Elsea in a paper entitled "Occurrence of Intergranular Fracture in Cast Steels." See p. 160.

We could not detect any effect of aluminum nitride inclusion on hardenability. Repeated heats for a given basic furnace melting practice, and a comparison between basic furnace melting practices, did not show a definite trend with respect to hardenability.

CO-CHAIRMAN HAHN: What about the acid steels?

MR. KURA: For a given charge and melting practice, acid furnace steels usually result in higher sulphur content than basic furnace steels. We have information in the paper that leads us to believe that acid furnace steels, deoxidized with insufficient aluminum or aluminum plus titanium, are apt to contain more of the detrimental Type II sulphide inclusions because of this higher sulphur content. If an acid furnace heat can be made with residual element and sulphur content comparable to a basic furnace heat, there is no reason to believe that the acid steel would not possess properties similar to a basic steel.

An indication of the effect sulphur content exerts on Charpy bar values is evident in Fig. 2. Regardless of the melting practice, the preliminary heats, which were made with a low sulphur charge, ranked highest in Charpy bar value.

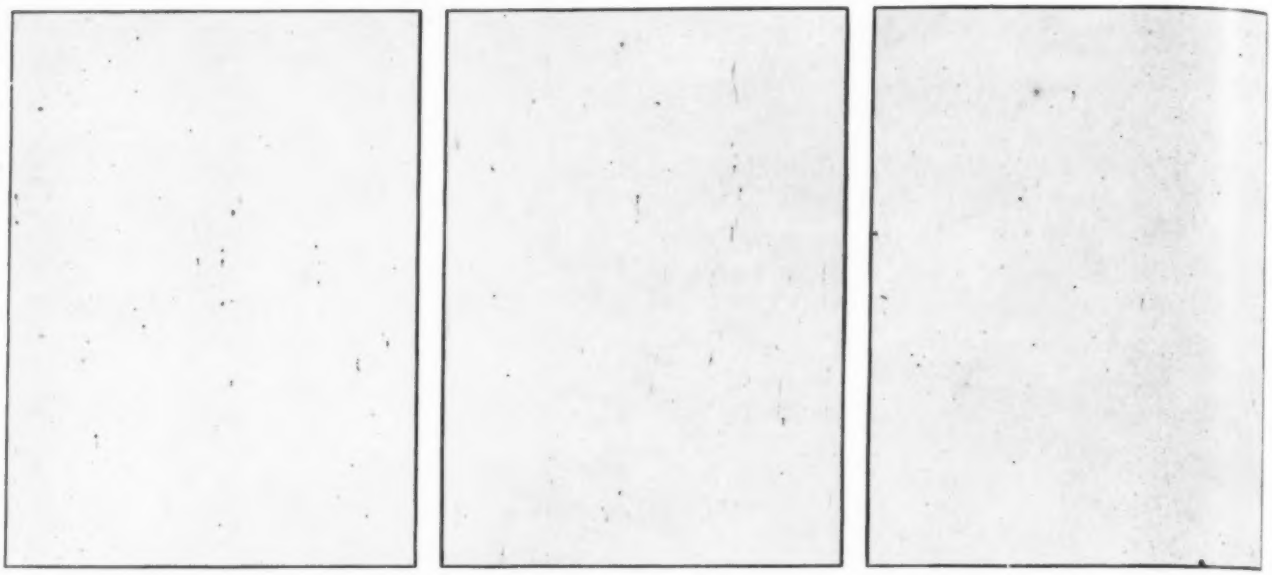


Fig. 10—Photomicrographs illustrating cleanliness rating of steels with respect to inclusions: A, dirty with respect to oxides; B, dirty with respect to sulphides; C, clean with respect to both oxides and sulphides. Unetched. x100.

As in the basic steels, hardenability of the acid steels did not show any trend attributable directly to the melting practice employed.

G. A. LILLIEQVIST:[1] I want to congratulate Mr. Kura for the excellent paper. I would like to ask in regard to hardenability whether you analyzed for chromium and nickel? You may have 20 points of chromium and 15 points of nickel and that could make a difference. You have given us no reason why you have this difference in this hardenability. Could it be due to these different elements? Also, nitrogen could add to hardenability.

MR. KURA: Which way would you say nitrogen would affect hardenability?

MR. LILLIEQVIST: It would impart higher hardenability.

MR. KURA: Heat 9434 had a nitrogen content of 0.0090 per cent while heat 9433 had 0.0180 per cent nitrogen. The chemical analysis of both heats is otherwise very similar. However, the hardenability curves for these two heats, deoxidized with 2½ lb. of aluminum per ton, are practically identical as shown in Fig. 7 and 9. If nitrogen were at all potent in enhancing hardenability, it should have been evident in this comparison. Digges and Reinhart* state that nitrogen has no effect on hardenability of boron-free steels. The heats reported in this paper were all boron-free.

MR. LILLIEQVIST: Did you analyze for chromium and nickel?

MR. KURA: Analysis for chromium and nickel was not made because the residual amount of these elements in the charge was of the order of 0.04 per cent chromium and 0.02 per cent nickel.

CHAIRMAN BRIGGS: In Table 3 it may be observed that under the type of sulphides and the amount, there are statements that the steels are dirty, slightly dirty, and so on, and then in the text material it is explained that even the steels rated as dirty in Table 3 would generally be classed as clean in commercial practice. Such statements appearing in the text are often overlooked when studying or reproducing tables and I would like to see some numbering system of relative cleanliness applied in the paper as appearing in the TRANSACTIONS instead of the statement that a steel is listed as dirty when commercially speaking it is not a dirty steel.

MR. KURA: Since the steels rated as dirty in Table 3 were quite clean compared to commercial steels, an arbitrary rating had to be established to indicate the differences found in these experimental steels. Perhaps the photomicrographs, Fig. 10, will establish the cleanliness rating in Table 3 more lucidly than the suggested numbering system.

F. A. MELMOTH:[2] Can you formulate definite conclusions as to what would happen in normal practice by making very small heats, and then even splitting those down further? I am not sure that you are even going to get regularity in repeats, and the author did indeed point out variations on repeats. Those variations might not be of a large order, but you are looking for influence of practice and I am not sure that you can get a true picture of influence of variable practice or variable method of production when you deal with such small quantities and then still further fractionate them to arrive at a new set of figures. I would like to know whether Mr. Kura believes that producing data in that fashion would leave it able to be translated into expected behavior in larger heats of the commercial type.

MR. KURA: The conclusions we arrived at were based on the small experimental heats only. In the paper a statement was made to the effect that we realized the difficulties of translating such information over to production heats. Obviously, it would have been economically prohibitive to carry out such an experimental program, as reported in this paper, with large commercial furnace heats. Therefore, small experimental heats were made with the added assurance that the many variables encountered could be controlled to our satisfaction.

C. O. BURGESS:[3] The authors have concluded that the notched impact properties appear to be principally influenced by the sulphur content, i.e., they decrease as sulphur content increases, and the properties are relatively independent of melting practice except as it influences the amount of this element. Their basic practice naturally gave the greatest opportunity for effective sulphur elimination. It should be pointed out that the conclusions of the present authors are confirmed by data in a paper "Influence of Melting Conditions on the Physical Properties of Steel Castings" by H. T. Protheroe, *Journal* of the Iron and Steel Institute (British), vol. CL, No. 2, p 157P (1914). He investigated some 28 steels made by the acid electric, basic electric, and Tropenas Converter processes and deoxidized with either 6 oz. aluminum, 24 oz. aluminum, or 8 oz. of calcium-manganese-silicon per ton. If an analysis of his data is made, it appears that the only variable definitely affecting Izod impact properties is sulphur content. At a 0.01 per cent sulphur level, the average impact value of his steels was 60 ft.-lb. and at 0.050 per cent sulphur the average impact dropped to 20 ft.-lb. If the sulphur was held to 0.025 per cent maximum, no impact values of under 35 ft.-lb. were found.

J. B. CAINE:[3] There is considerable discussion about the effect of sulphur on the Izod values. It is assumed that if we have

[1] American Steel Foundries, Indiana Harbor Works, East Chicago, Ind.
* T. G. Digges and F. M. Reinhart, "Influence of Nitrogen on Some Properties of Experimental Steels Without and With Boron," O.S.R.D. No. 3378 (M-23), March 15, 1944.

[2] Glen Lake, Cedar, Mich.
[3] Sawbrook Steel Castings Co., Cincinnati.

60 Izod ft.-lb. we have a wonderful steel, but if we have 20 Izod ft.-lb. we have, supposedly, a poor steel. We have no proof that this is true. We do know that if the Izod values decrease to about 10 ft.-lb. or less and we get a coarse crystalline fracture the serviceability of that steel is questionable, for this has been proven in the correlation between test results and actual use. As far as I know, no one has ever proven that a steel with 60 ft.-lb. is a better steel than one with 20 or 25 ft.-lb. Can anybody tell me the difference between 60 ft.-lb. and 25 ft.-lb. in the actual serviceability of the part?

C. E. SIMS:[4] There are some who think that Izod or Charpy results should be classified merely as low, medium, or high, and that the actual figures mean very little. Slowly we are gathering information to show that notch-bar impact values give a measure of toughness and that high values improve the serviceability. However, as far as this paper is concerned, I would say that the authors were merely making a comparison between the steels to see whether various practices did have some effect. The bulk of the evidence developed in the paper is that the practices used had no direct effect at all.

If in these experimental heats differences had been found as a result of variation in practice, then one would be justified in saying that practice does make a difference and it would be necessary to investigate on a commercial scale to find out what the real differences are. However, in view of the fact that no differences were found, it is quite unlikely that one would find them in commercial practice. I think the outcome of the paper is very logical. We have to rule out occult effects on the properties of steel, and practices can only affect either the composition or structure of the steel. It is only the composition or structure of the steel that controls the property.

R. E. KERR:[5] It is my understanding that the main function of your aluminum is deoxidation, and you mentioned that in your basic steel with the fully reducing slag you thought you might be getting some aluminum nitrides which affected your impact properties. On our basic steels, finished under reducing slag, we did not seem to find it necessary to use aluminum. I was wondering whether any tests had been made on basic steel with the reducing slag without aluminum.

[4] Battelle Memorial Institute, Columbus, Ohio.
[5] Pettibone Mulliken Corp., Chicago.

MR. KURA: The addition of the same amount of deoxidizer was made to all the heats so that deoxidation would not be an uncontrolled factor when the heats were compared.

If the basic, reduced heats had not been deoxidized with aluminum, no intergranular type of fracture caused by aluminum nitride could have occurred in the steel. If no aluminum or any other strong deoxidizing agent had been added, the sulphide inclusions would have been Type I, that is the sulphides would have been present as globules and would probably have exerted less effect on Charpy bar values.

MR. SIMS: I wanted to mention one instance where an acid foundry had very low sulphur steel. They got all their scrap from a steel mill and their maximum sulphur ran about 0.022 per cent. Regardless of how they deoxidized, they had no trouble with ductility, which is one example of commercial steel where low sulphur was a solution to the ductility problem. problem.

MR. LILLIEQVIST: I think that you can use impact in service.

MR. CAINE: What I am trying to put across is that we are saying that we must have more than zero ductility no matter how you measure it, but we have no evidence to distinguish between 60 Izod ft.-lb. and 25 ft.-lb. All the evidence that is on the books at the present time says that a steel of 25 ft.-lb. impact strength will perform in service as well as one with 60 ft.-lb.

MEMBER: In what kind of service?

MR. CAINE: In any kind of service from aircraft down, but if the impact strength is less than 10 ft.-lb. with a coarse crystalline fracture, there is evidence that such a steel will fail in service. Again the temperature at which the actual part is to be used determines the testing temperature, and the impact strengths must be determined at this temperature. If the actual part is to be used at temperatures below room temperature, room temperature impact properties mean nothing.

E. C TROY:[6] Are the titanium figures spoken of in per cent of titanium added?

MR. KURA: Titanium was added as 75 per cent FeTi in amounts of 3 lb. of FeTi per ton of steel. The 0.01 per cent titanium is the amount recovered in the casting.

[6] Dodge Steel Co., Philadelphia.
[7] Union Carbide and Carbon Research Labs., Niagara Falls, N.Y.

GRAPHITE PHASE IN GRAY CAST IRON

By Robert W. Lindsay

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Pennsylvania State College
State College, Pennsylvania

ABSTRACT

The paper reviews the factors affecting the development of the graphite structure in gray cast iron and the relationship of this graphite structure to the properties of this important group of alloys.

The main factors influencing the formation of graphite—cooling rate of the casting, and carbon and silicon contents—are discussed extensively. After briefly mentioning that the structural features influencing the properties of gray cast iron are the graphite flakes and the matrix, the discussion turns to a consideration of the graphite flakes.

It is pointed out that certain features of the flakes, their amount, size shape and distribution, are important with respect to properties. The factors affecting these features of the flakes are reviewed. Emphasis is placed on the factors contributing to the formation of graphite distributions illustrated in the A.F.A. chart, and their desirability or undesirability. The treatment of the graphite distributions is prefaced by a review of the ideas regarding graphite formation in general, and the factors affecting the same.

The paper concludes with a summary, which suggests the connection of the properties of cast iron to the graphite structure. Also, it emphasizes again that those concerned with producing gray iron castings must regulate certain features of the graphite and that this regulation is obtained by control of melting conditions and temperature, pouring temperature, alloying and inoculation practices.

IT IS A GENERALLY ACCEPTED FACT among metallurgists that the properties of metals and their alloys are related to their internal structure, and cast iron is no exception. It is recognized that the properties of gray cast iron are related to its structure—the important features of the structure in this case being graphite, and the metal or steel-like matrix.

Much has been written concerning the relationship between structure and properties, and the factors affecting the development of cast iron structure. It is the aim of this paper to review these factors and the relationship between properties and structure with the view of presenting them simply, yet without sacrificing correctness. In accomplishing this aim, various sources were consulted and these are acknowledged in the bibliography.[1-13] These references are recommended to those readers who are interested in inquiring into the questions of structure and properties in more detail.

Factors in Graphite Formation

The main factors which influence the structural development of cast iron are:

a. The rate of cooling, and
b. The composition of the iron principally in terms of carbon and silicon.

Most foundrymen are acquainted with the procedure of chilling (the rapid cooling of cast iron by the use of metal plates in the mold) to produce a hard, wear-resistant surface on cast iron, or are accustomed to seeing chilling tests used as a method of control in their foundries. Examination of such a chilled section (Fig. 1) shows clearly the effect of rate of cooling on the structure of cast iron of the same composition.

If samples are taken from the white zone, the mottled zone, and the gray zone, prepared by polishing and examined with a microscope in the as polished condition, then the comparison shown in Fig. 2 will be observed. There is practically no graphite apparent in the white zone where cooling was rapid, some is evident in the mottled zone where cooling is somewhat slower, and much more is found in the gray zone. The chemical analyses for carbon as graphite gave 0.20 per cent in the white zone, 1.50 per cent in the mottled, and 2.70 per cent in the gray region.

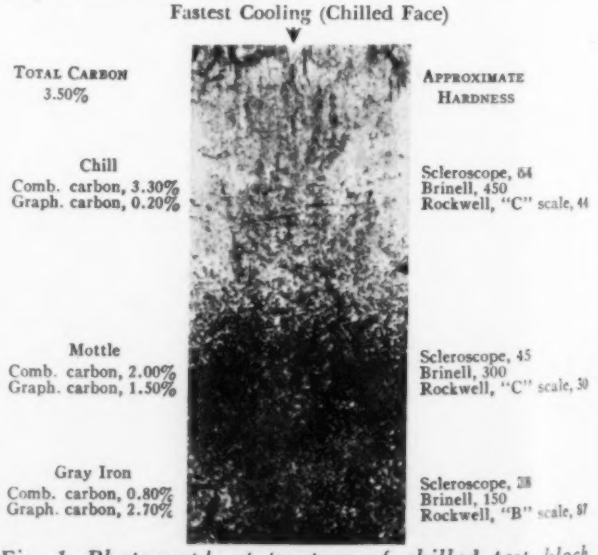


Fig. 1—Photograph of fracture of chilled test block. Note hardnesses and carbon analyses of white, mottled and gray iron zones. (After Massari[14]).

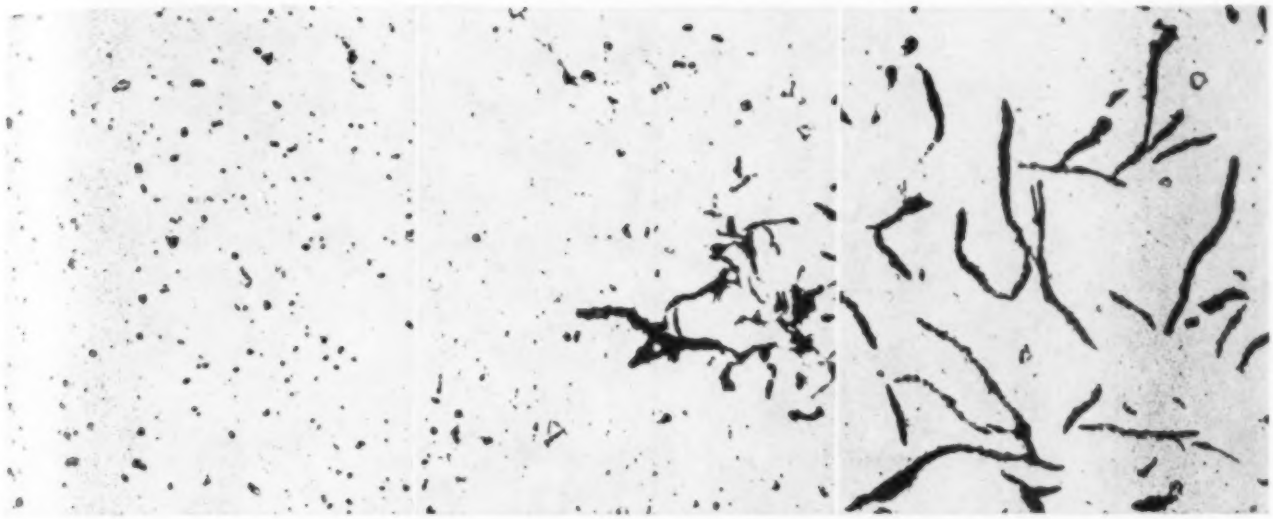


Fig. 2—Photomicrographs of white iron zone (left), mottled iron zone (center), and gray iron zone (right) of same chilled casting showing relative quantities of graphite. Unetched, 100X.

It can be said then that graphite has a rate of formation (whether it be its direct separation from the melt or the decomposition of pre-existing iron carbide) and that the amount of graphite which will form is governed by the relationship between this rate of formation and the rate of cooling of the casting. A fast rate of cooling may prevent the formation of graphite entirely while progressively slower rates of cooling favor the formation of increasing amounts of graphite.

Effects of Carbon and Silicon

In similar fashion, the effect of the elements, carbon and silicon, on the amount of graphite formed can be shown. This is probably best shown by pouring a set of chill tests under similar conditions holding silicon constant and varying carbon in one instance, and holding carbon constant and varying silicon in the other. The results of such comparisons are shown in Figs. 3 and 4. It will be noted that as the silicon content is decreased with carbon held constant, the depth of chill (depth of white iron) increases, and the same is true if the total carbon content is decreased with the silicon held constant. This means that formation of graphite is retarded by decrease of silicon content or decrease of total carbon content even though cooling rate is held constant. Some of the factors promoting and retarding graphite formation are listed in Table 1.

In most cases it is desirable to keep either excess, hard carbides or excess, soft and weak ferrite out of the structure. Therefore, if the section thickness is such that the casting cools rapidly, then to maintain a gray iron structure it is necessary that the total carbon and silicon contents must be high and the alloying elements must be in balance so that there will be no excess, hard carbide areas in the structure. On the other hand, if the casting size imposes a slow rate of cooling, then the graphitizing potential due to analysis has to be low, otherwise overgraphitization as shown in Fig. 5 may occur. This results in the presence of excess ferrite in the structure.

Considering the use of alloying elements, it is apparent that if any graphitizing element is to be used alone in a given iron then the silicon content should be lowered simultaneously to avoid overgraphitization. In using combinations of carbide stabilizing elements and graphitizing elements, it is necessary to maintain balance to keep graphitization under control.

The preceding statements are best summarized by emphasizing that cooling rate and chemical composition must be balanced to obtain a desirable gray iron structure. No mention has been made as yet of the effect of such factors as superheating, pouring temperature and inoculation upon the graphitizing tendency of cast iron. These will be introduced into the discussion later. It is also worthy of mention that still other factors such as type of melting furnace and melting practice as well as atmosphere and slag composition can influence the graphitizing tendency of gray cast iron. However, a detailed discussion of such influences is felt to be beyond the scope of this paper. The factors affecting the properties of gray cast irons have been summarized elsewhere.[11]

Consideration of the Graphite Phase

Structure desired in good grade of gray cast iron is shown in Fig. 6. Two features of this structure should be emphasized, namely: the graphite flakes, and the matrix.

TABLE 1.—SOME FACTORS PROMOTING OR RETARDING GRAPHITE FORMATION

Factors Promoting Formation	Factors Retarding Formation
1. Slower cooling rate	1. Faster cooling rate
2. Increased total carbon	2. Decreased total carbon
3. Increased silicon	3. Decreased silicon
4. Presence of graphitizing elements such as nickel, aluminum, titanium, zirconium, and copper	4. Presence of carbide stabilizing elements such as chromium, vanadium and in lesser degree manganese and molybdenum

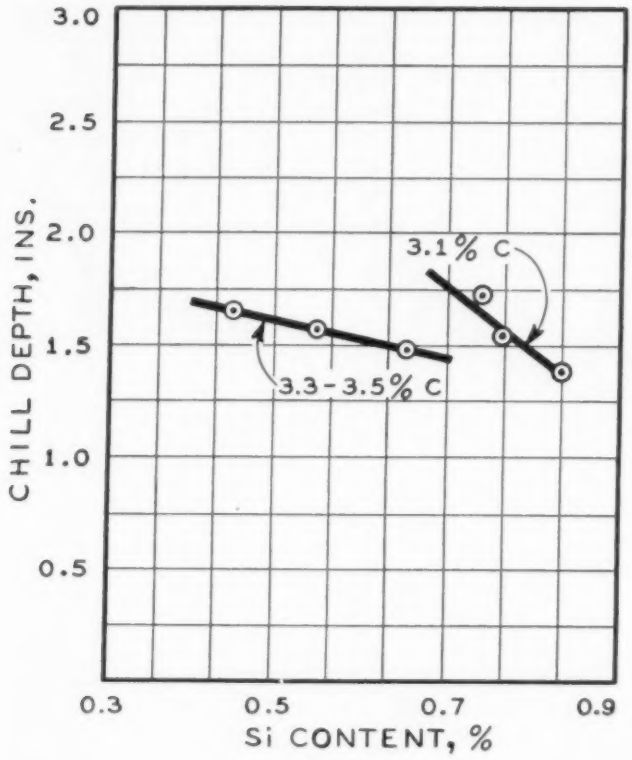


Fig. 3—Increase of chill depth with decrease of silicon content for constant carbon contents. (After Schuz and Pohl[15]).

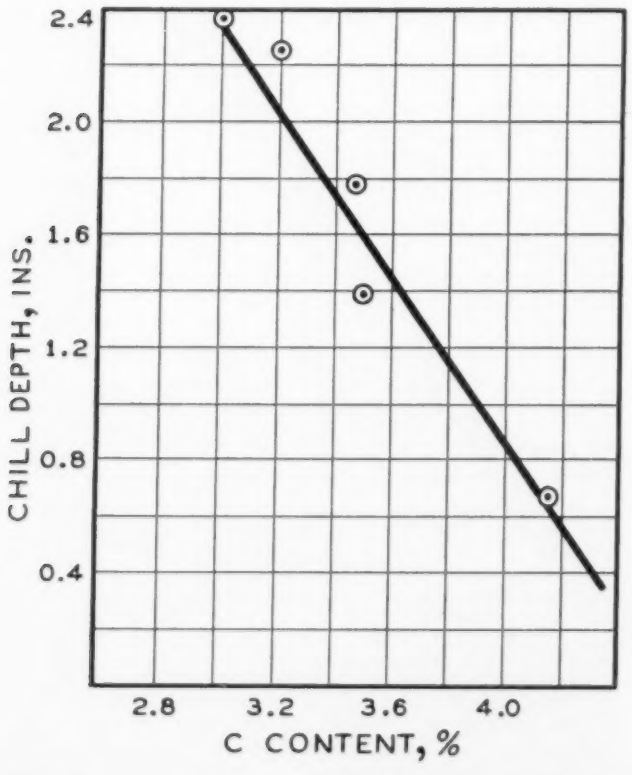


Fig. 4—Increase of chill depth with decrease of total carbon content. Silicon range 0.81 to 0.89 per cent. (After Schuz and Pohl[15]).

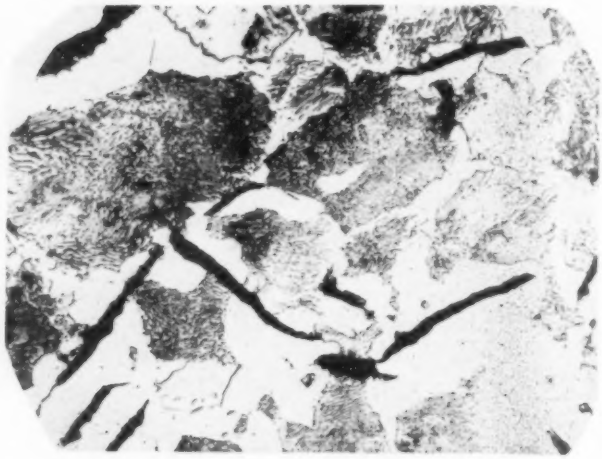


Fig. 5—Photomicrograph of overgraphitized gray cast iron. Note large white areas of ferrite, pearlite areas of "thumb print" structure, and black flakes of graphite. Nital etch, 500X.



Fig. 6 — Photomicrograph of pearlitic gray cast iron showing pearlite and graphite flakes. Nital etch, 500X.

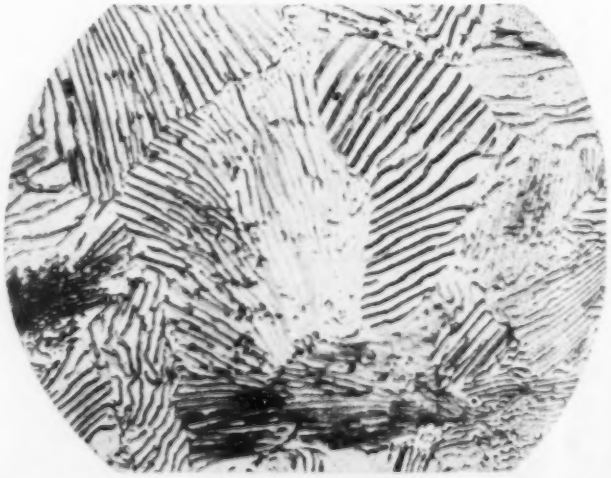


Fig. 7—Photomicrograph of 0.8 per cent carbon (eutectoid) steel showing a completely pearlitic "thumb print" structure. (After Allen[17]).

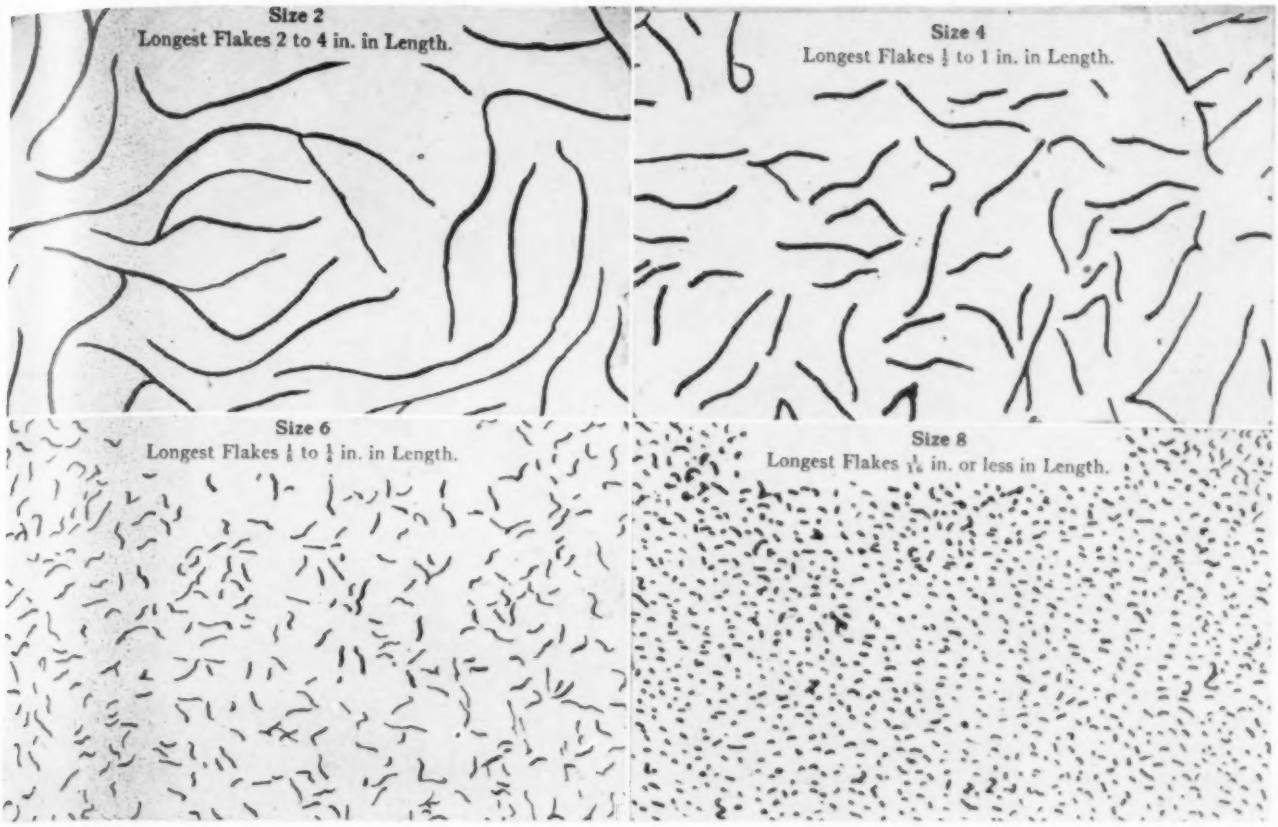


Fig. 8 (above)—Four of the various flake sizes of graphite included in the A.F.A. Chart. Entire chart is contained in the Cast Metals Handbook[16]. Unetched, 100X.

Fig. 10 (Below)—Graphite flake sizes in two irons of different carbon contents. Upper photomicrographs, 3.46 per cent carbon iron; lower photomicrographs, 3.05 per cent carbon iron. At left, 1.2-in. diameter bars; at right, 2-in. diameter bars. (From Mahin and Lownie[18])

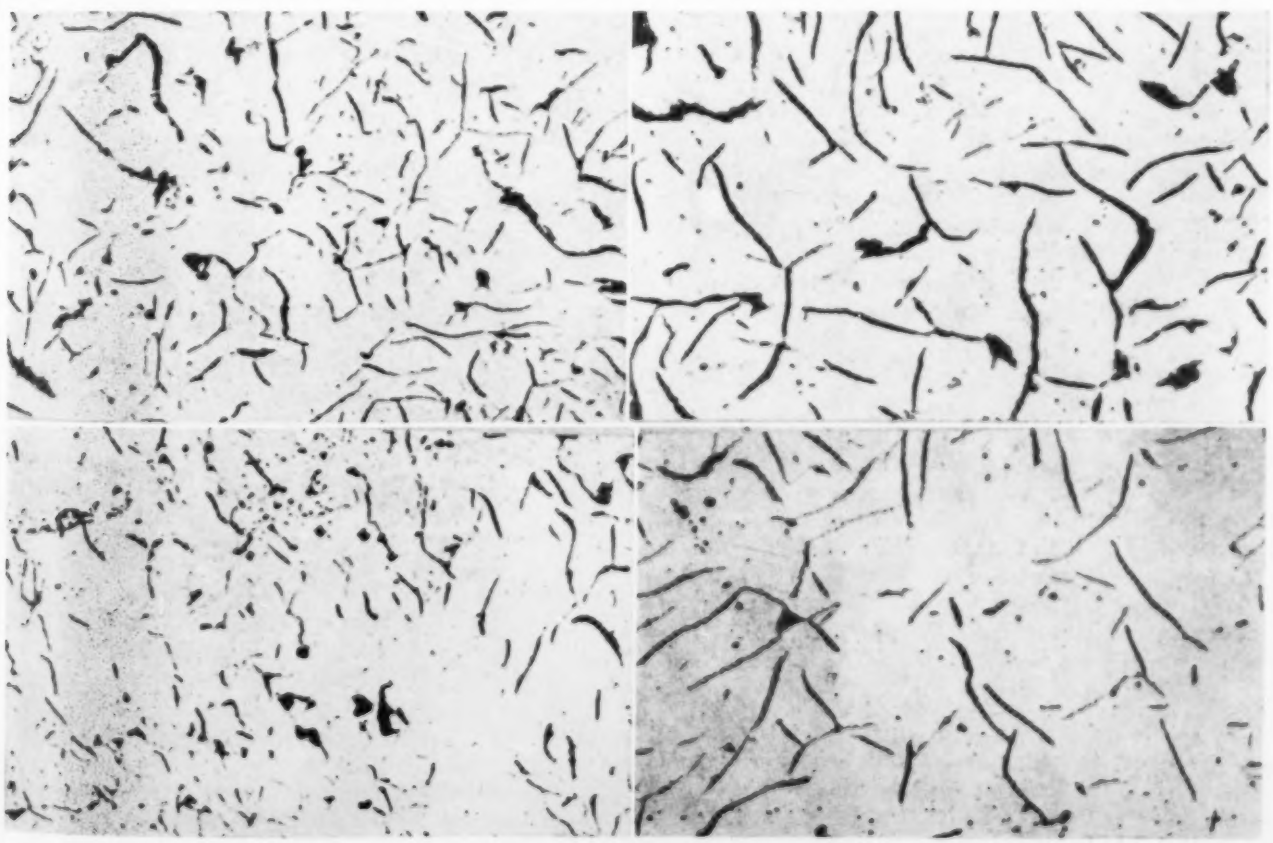




Fig. 9—Graphite flake sizes in 1.2-in. diameter bar (left), 2-in. diameter bar (center), and 6-in. diameter bar (right). Chemical analysis of iron: T. C. 3.46 per cent, Si 2.36 per cent. Unetched, 100X. (From Mahin and Lownie[18]).

The matrix plays an important part in governing the properties of gray cast iron. Gray cast iron has been referred to as steel plus graphite[8] and it is the matrix, or metallic portion of the structure, which is similar to steel in nature. In this paper, it is not intended to deal with the matrix in detail. A thorough discussion of matrix will be found in a previous A.F.A. paper.[13] It is sufficient to say that the matrix structure should resemble as much as possible the structure of a steel with 0.8 per cent carbon, that is, it should be mainly pearlitic (Fig. 7). It should be understood that modifications of this particular matrix are sometimes produced in the interest of improving certain mechanical properties of gray cast iron.

Turning to a consideration of the graphite flakes, it is evident that there are several features connected with the graphite formation. These are flake size, flake shape, the amount of graphite, and the distribution of the graphite. Each of these features and the factors operating to affect them will be considered.

Features of the Graphite

Size of graphite flakes are classified on the basis of structural examination in the unetched condition at 100 magnifications. The American Foundrymen's Association in conjunction with the American Society for Testing Materials has established a chart depicting the range of flake sizes encountered in commercial irons.[16] By comparison of the structure in question with this chart it is possible to rate the flakes according to the numbering system of the chart. The finest flakes are rated at size 8 (longest flakes 1/16 to 1/8 in. in length at x 100) and the coarsest flakes are rated as size 1 (longest flakes 4 in. or more in length at x 100). A reproduction of parts of the A.F.A. graphite flakes size chart is shown in Fig. 8.

One factor which is of importance in influencing the size of the graphite flakes is the rate of cooling of the casting. The flake size will be finer as the casting cools at a faster rate—that is, as the casting section size is reduced. Another factor influencing the flake size is the total carbon content of the iron. In this case the size of the flakes is smaller as the total carbon content of the iron is lower. Figures 9 and 10 illustrate the influence of cooling rate and total carbon content on the graphite flake size.

The presence of certain alloying elements would also affect the graphite size. It is probable that graphite size will be of influence on the mechanical properties of gray cast iron, but it can not be demonstrated positively in many cases. Frequently, the variations in flake size are accompanied by variations in the matrix and hence the true effects of flake size variations are somewhat obscured.

Shape of the flakes is a somewhat uncontrollable feature of the graphite structure. It is true that fine graphite flakes are compact in form and that the graphite flakes in some alloy cast irons also exhibit this

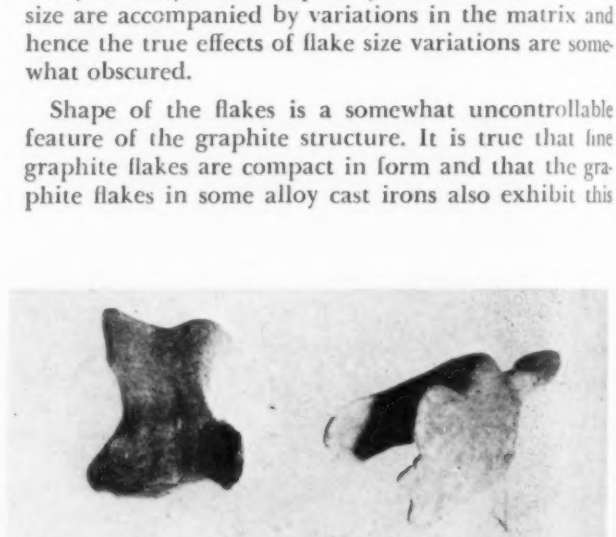


Fig. 11—Graphite flakes and formation models as constructed by MacKenzie[8].

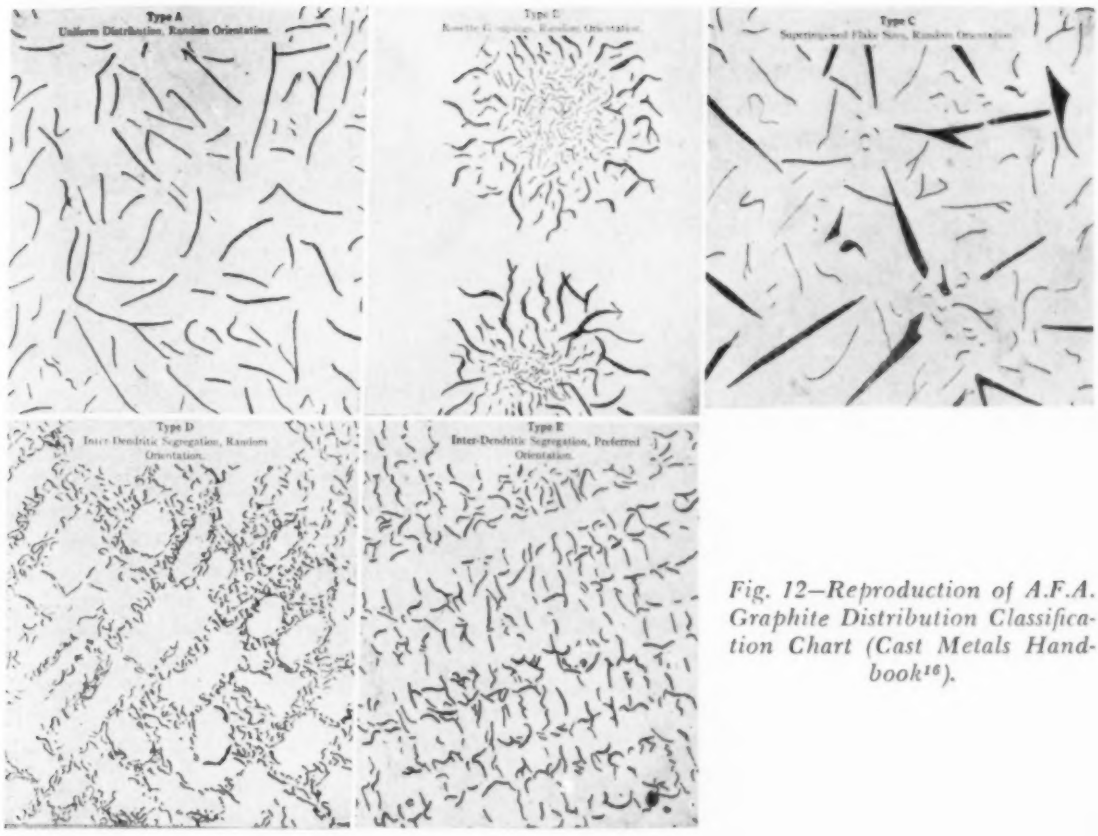


Fig. 12—Reproduction of A.F.A. Graphite Distribution Classification Chart (Cast Metals Handbook[16]).

compactness. In most cases, however, the flakes seem to be definitely stringy in character. Graphite more nearly in the form of temper carbon—the compact type of graphite found in malleable iron—would seem to be desirable.

In connection with the shape of graphite in gray iron, it is interesting to note that the graphite flake is actually not the two dimensional (length and thickness) particle that we might be led to expect from our microexamination. It has been shown[8] that the graphite has a third dimension and its real appearance in space can be modeled as shown in Fig. 11.

The amount of graphite is related to the total carbon content of the iron, and to other factors. For example, if the total carbon content of gray cast iron is lowered and the remainder of the analysis is adjusted—mainly by raising the silicon content—so as to maintain a pearlite matrix, then a lesser amount of graphite will form. This is based on the assumption that the size of the casting, and hence the cooling rate, will be kept constant.

Some analyses values might clarify this. If the total carbon content is to be considered 3.50 per cent and the combined carbon content—that part of the carbon in the pearlite—is 0.70 per cent, then the carbon as graphite will be 2.80 per cent by difference. Now, if the total carbon in another iron is 3.0 per cent and the combined carbon is again 0.70 per cent, the carbon as graphite will be 2.30 per cent.

The other important factors affecting the amount of graphite are the presence of other elements, and the cooling rate. Certain elements such as silicon and nickel are known as graphitizers and certain others such as chromium are known as carbide formers. If a certain size of casting is being made and the remainder of the analysis is held constant, an increase in the silicon will result in an increase in the amount of graphite to the point of causing the occurrence of the aforementioned overgraphitization. On the other hand, a carbide former such as chromium would work in the other direction and the amount of graphite here would be decreased. In fact, excess, hard carbide areas would appear in the structure with increasing amounts of chromium.

The importance of cooling rate was brought out earlier and attention is called again to Figs. 1 and 5. Graphite formation is suppressed above a certain (fast) rate of cooling in any given cast iron. The rate of cooling required to suppress graphite formation will be related to the analysis of the iron. An iron with high carbon and/or silicon will contain graphite in its structure at higher rates of cooling than an iron with lower carbon and/or lower silicon contents. This means that for a given analysis larger sections, which cool slowly, may experience much graphite formation and large quantites of ferrite may form in the matrix (overgraphitization in Fig. 5). This condition is not desirable in the majority of cases and must be guarded against by analysis control.

The distribution of graphite, or the arrangement of the flakes, in gray cast iron is of definite importance. This feature of the graphite has also been the subject of classification by the A.F.A.—A.S.T.M. A reproduction of the distribution classification chart is given in Fig. 12. As it can be observed, this classification is on the basis of the letters A to E. If fine graphite is to have

a beneficial or strengthening effect on gray cast iron, it is important that this graphite be in random arrangement and not interdendritic arrangement.

An examination of the manner in which these graphite structures form is in order. However, it seems advisable to consider graphite formation in general before taking up the details of these structures.

Formation of Graphite. There has always been much debate as to whether graphite separates directly from the molten iron, or whether iron carbide deposits first and graphite formation results from its decomposition.[19] Fairly recent experiments by Boyles,[4] Boyles and Lorig,[2] and Eash[3] may be quoted as bearing on this question.

Boyles showed that solidification of gray cast iron started with the formation of primary austenite—that is, when the line BC′ (BC) was reached on the iron-carbon diagram (Fig. 13). Primary austenite continued to form within the remaining liquid iron as the temperature dropped. Then, in the vicinity of the line E′C′F′ (ECF) on the iron-carbon diagram, the remaining liquid iron froze. This final freezing was accomplished by the formation of more austenite, and graphite in the spaces between the so-called dendrites of primary austenite. This growth started from several centers and in each case grew out radially like the spokes of a wheel. The graphite flakes continued their growth by lengthening, but not by thickening very much. This leads to a rosette-like or whorl development of the graphite. A stage in such radial growth between the primary austenite dendrites is shown in Fig. 14 taken from Boyles' paper.

Boyles and Lorig presented further data in reporting on the undercooling of gray cast iron. They were endeavoring to show that under similar conditions of cooling, the radial development of graphite did not always occur at the same temperature range. At times this formation of graphite will undercool, that is take place at a somewhat lower temperature. Evidence of this undercooling and the structural result as far as graphite is concerned, is shown in Figs. 15 and 16 from their paper. Note in the cooling curves of Fig. 15 that the breaks between 2050 and 2100 F occur at a somewhat higher temperature in the normal irons as compared to the modified irons, which exhibit undercooling.

The structural differences in the graphite as shown in Fig. 16 should also be noted. The normal iron on the left contains graphite similar to Type A or Type B on the chart (Fig. 12). The modified, undercooled iron on the right, with the radial growth developing from only one center (at D) in this particular region shows considerable graphite resembling Type D (Fig. 12). The coarser flakes at the edges of this growth formed at a somewhat higher temperature where the rate of solidification was slower. The arrows on Figure 16 show directions of flake growth. The letters on the figure indicate centers of growth and are *not* indicative of the type of graphite.

Eash studied the behavior of gray cast iron in the course of an investigation of inoculation. He came to the conclusion that in certain cases graphite separated directly from the melt in the vicinity of line E′C′F′ on the iron-carbon diagram in which cases it would be of Type A, B or E distribution. He concluded further that in other cases iron carbide separated from the melt in the vicinity of line ECF on the diagram (Fig. 13) as a part of the eutectic and this carbide decomposed to give graphite. The result of the latter behavior was Type D graphite.

This is based on the reasoning that there are two diagrams (Fig. 13), one representing relationships between iron and graphite, and the other relationships between iron and iron carbide. If undercooling of the iron-graphite line occurs (line E′C′F′), then solidification may partly occur in the vicinity of the iron-iron carbide line, ECF, and graphite will result only from decomposition of the iron carbide. Photomicrographic evidence is presented by Eash to substantiate his results. Discussion of these photomicrographs might become too voluminous for this paper and it is suggested that the interested reader consult the original reference.[3]

Additional Factors of Influence. Further evidence of the factors influencing graphite formation and distribution have been brought out by cooling rate studies and various superheating investigations. Schneidewind and D'Amico in the course of studies of structural development[5,6] clearly showed the influence of severe undercooling in promoting fine graphite formation of type D or E distribution.

By pouring wedge shaped castings of cast iron, these investigators were able to study the relationship between cooling rate and structure. Furthermore they found that structure could be correlated with the temperature at which solidification could be made to occur.

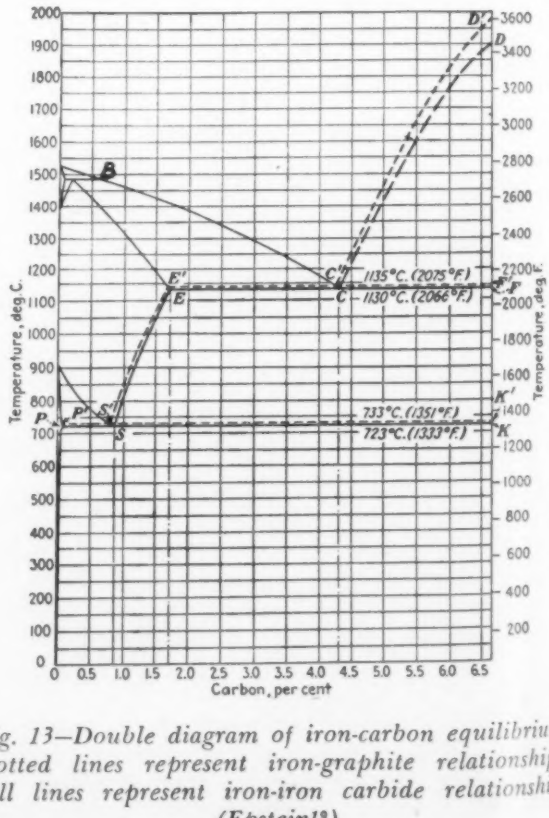


Fig. 13—Double diagram of iron-carbon equilibrium. Dotted lines represent iron-graphite relationships; full lines represent iron-iron carbide relationships (Epstein[19]).

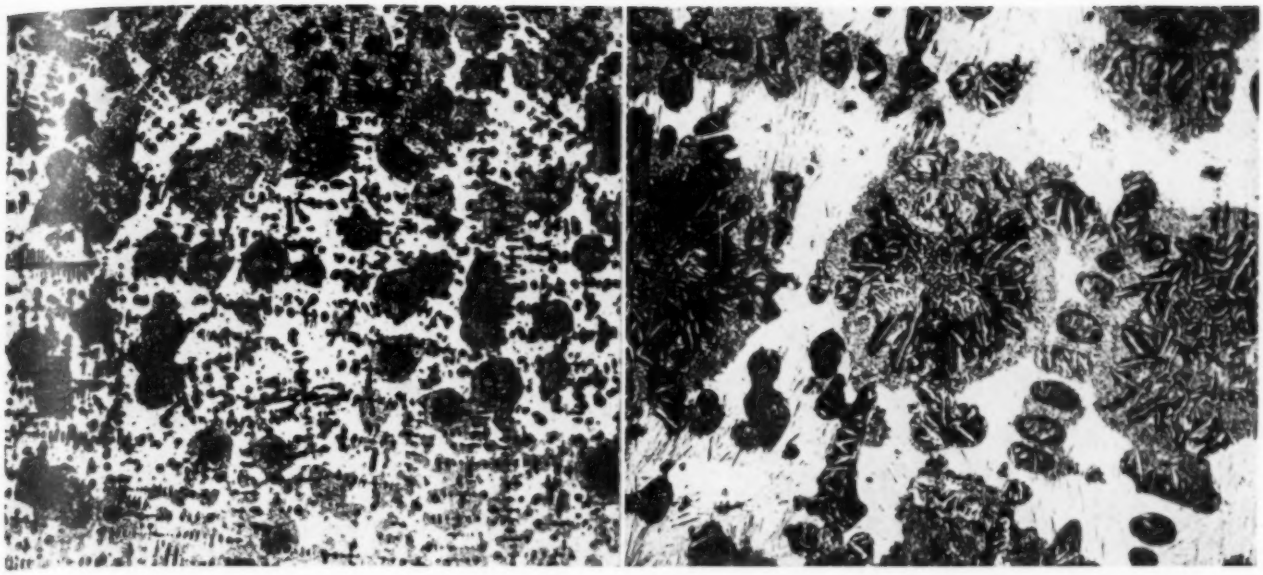


Fig. 14—Radial or cell growth in gray cast iron at eutectic temperature. Note cells growing between primary austenite dendrites (left) and structure of cell growth (right). (After Boyles[4].)

Table 2 taken from their papers and modified somewhat shows the correlation between solidification temperature and structure.

It is evident that if the cooling rate is such as to cause the final stages of solidification to take place in the vicinity of 1970 to 1992 F, a condition which is satisfied near the tip of such aforementioned wedge castings, then Type D to E distributions of the graphite will result.

Numerous investigations on superheating have indicated the far reaching effect of this factor on graphite formation. Although the explanation of the results of superheating are still a matter of controversy,[10,20] it is generally agreed that superheating may result in undercooling. This means that superheating leads to a finer graphite flake size and a tendency toward Type D graphite distribution, the latter particularly in the case of superheating of lower carbon irons.

Finally, mention should be made of the influence of pouring temperature upon graphite formation and the general structure of gray cast iron. Timmons and Crosby[7] showed that a decrease in pouring temperature acted in the same direction as an increase in cooling rate and hence favored undercooling. In their higher carbon irons, the graphite which was of definite Type A distribution with a pouring temperature of 2800 F became just as definitely Type B with pouring temperature of 2350 F and a good deal of ferrite appeared in the matrix. Again, one of their lower carbon irons, which showed only a faint Type D graphite distribution when poured at 2800 F, showed pronounced Type D graphite and more free ferrite when poured at 2350 F. The tensile and impact strengths in these lower carbon irons poured at 2350 F were definitely low.

Formation of Distribution Types. Having discussed the manner of graphite formation and some of the factors influencing the same, consideration will now be given to the formation of the various distribution types as shown in Fig. 12.

Type A—This type of graphite distribution will result when all factors tending to promote severe undercooling are held to a minimum. This type of graphite occurs quite readily in higher carbon irons. However, in such irons an increase of cooling rate will result in a tendency toward Type B distribution. The graphite flakes in the Type A distribution can be produced in somewhat finer size by superheating of these higher carbon irons, that is heating the molten iron to temperatures well above those necessary to melt the iron. Type A graphite does not occur so readily in the lower carbon irons, namely those irons with carbon in the vicinity of 3 per cent more or less. However, by processing or inoculating of such irons, it is possible to favor the formation of Type A distribution. This type of graphite distribution is the most desirable one, since it promotes superior strength and toughness.

Type B—From examination of various photomicrographs in the literature, it would appear that these pronounced rosette patterns appear most frequently in higher carbon irons. Such pronounced rosettes result when these irons are cooled at faster rates. For example, Type B graphite distribution occurs near the surfaces of higher carbon castings while the graphite in the main body of the casting is Type A. Although this type of graphite is not so desirable as Type A, it is questionable whether it is seriously detrimental to the major properties of gray cast iron. However, the wear resistance of gray irons containing pronounced Type B graphite may be adversely affected because of the association of large quantities of soft ferrite with this type of graphite.

Type C—This type of graphite is uncommon in gray cast iron. In fact, the writer believes recalling that the particular photomicrograph shown in the A.F.A. chart was taken from a sample of pig iron. It has been suggested that this distribution would occur in irons whose analysis was such as to make them hyper-eutectic, that is to place them to the right of point C′ (C) in the iron-

carbon diagram (Fig. 13). Thus, any iron might show this distribution if the sum of its carbon content plus one-third of its silicon content was 4.3 or greater.

Type D and Type E—These types of graphite are classified as interdendritic graphite. This classification would seem to be somewhat of a misnomer in view of Boyles' experiments which indicate that all graphite is interdendritic, that is, it forms between the primary austenite dendrites. Furthermore, the term preferred orientation is somewhat misleading. Type E, which is classified in this fashion, is certainly not to be preferred in the usual sense of this word. In many instances it is preferable not to have Type E or Type D graphite.

The word preferred in this case is used to indicate the pronounced directional nature of the graphite. These types of graphite are found rather frequently in lower carbon irons as stated previously. In many instances such lower carbon irons, those with carbon content in the vicinity of 3 per cent, are chosen with the expectancy that they will exhibit high strength values, yet when actual tests are made, such as tensile strength, transverse strength, transverse deflection and impact strength determinations, the results may be disappointingly low. This situation may be and is being controlled in present day foundry practice by inoculation, or late ladle treatment for the purpose of regulating the structure of the iron with respect to graphite and chilling tendencies. The theory of inoculation has been presented in a recent paper by Lownie.[21]

Eash[3] has presented some values to indicate the detrimental effect of these graphite distribution types and the benefit to be derived from inoculation treatment. McElwee and Barlow[22] have also dealt extensively with this subject.

Formation of these types of graphite is favored by faster cooling rates, so that at least there is a tendency for them to appear even in higher carbon irons if the cooling rate is fast enough. In other cases, the graphite distribution in the main body of the casting may be fairly good, while at the casting surfaces Types D and E graphite will occur. Since large amounts of ferrite are associated with these latter distributions, this is another undesirable feature of this type of graphite structure as it detracts from wear resistance. Inoculation is also helpful in this respect either by eliminating the condition altogether, or displacing it to such a position in the casting that it can be removed in subsequent machine shop operations.

TABLE 2.—INFLUENCE OF TEMPERATURE OF SOLIDIFICATION UPON THE GRAPHITE PATTERN

(Adapted from Schneidewind & D'Amico)
Analysis of iron: Carbon 2.78%, Silicon 1.5%
(other elements not given)

Solidification Temperature, ° F.	Type of Structure
2060	Gray iron. Normal flake structure, Type A.
2040	Gray iron. Normal flake structure, Type A.
1992	Gray iron. Some short flake graphite. Some eutectiform graphite in dendritic dispersion. Type A and Type D.
1970	Gray iron. Eutectiform graphite in very dendritic dispersion, Types D to E.
1955	Mottled iron. Carbides plus eutectiform graphite.
1818	White iron. Traces of graphite and some temper carbon formed during cooling.

Summary

The graphite in gray cast iron has certain features that are of importance to the metallurgist and to the casting manufacturer. Considerable attention has been paid to these features, which include the amount, size, shape and distribution of graphite, because the graphite phase has a very definite bearing on the properties of cast iron. The presence of graphite in gray cast iron and the structural features of this graphite is reflected in such commonly considered properties of gray cast iron as tensile and compressive strengths, transverse (bending) strength, deflection in the transverse test, impact strength, wear, and heat resistance. Of course, other details in tensile and compressive testing are affected by the graphite phase. Furthermore, two desirable properties specific to cast iron—namely, its ability to absorb vibrations set up in it (damping capacity) and its low sensitivity to notches in vibratory applications are linked to the presence of the graphite. Each specific use for cast iron requires a more or less specific total carbon content. This total carbon content regulates to a large degree the amount of graphite. It is the job of the foundry supervision to see to it that this amount of graphite is maintained in a wholly pearlitic matrix in most cases, or at least a close approach to the same, and to guide the formation of this graphite as much as possible toward Type A distribution. The size and section relationships of the casting being fixed, the desired regulation is obtained by control of melting conditions and temperature, pouring temperature, alloying and inoculation practices.

Acknowledgment

The author wishes to express his appreciation to Mr. John Hoke, Graduate Assistant, Pennsylvania State College, for his assistance in preparing this paper. Furthermore, he wishes to thank Mr. H. W. Lownie, Jr. of the Battelle Memorial Institute staff for reading the manuscript and offering many valuable suggestions.

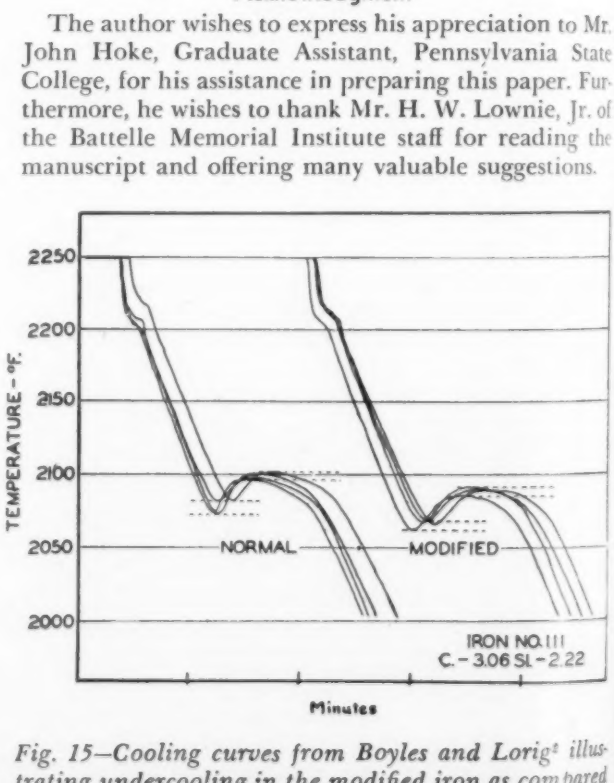


Fig. 15—Cooling curves from Boyles and Lorig[4] illustrating undercooling in the modified iron as compared to the normal iron. Note lower "break" between 2050 and 2100° F. in curve of modified iron.

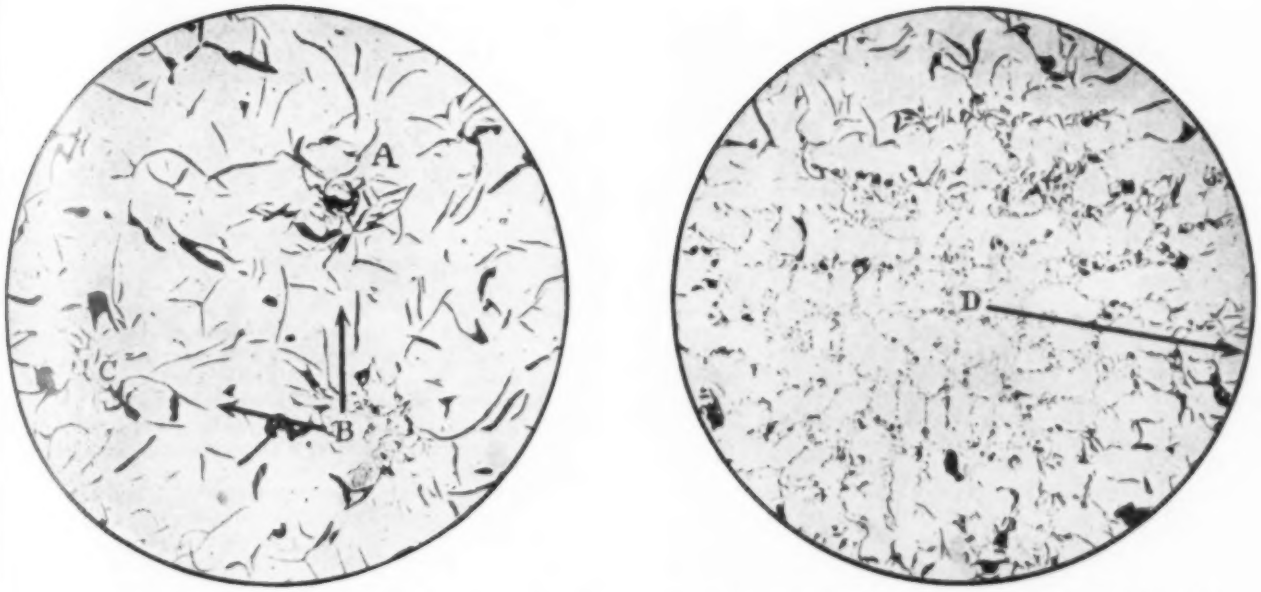


Fig. 16—Comparison of graphite structures in normal iron (left), and modified iron (right) from Fig. 15. (After Boyles and Lorig[2]).

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DISCUSSION

Chairman—V. A. CROSBY, Climax Molybdenum Co., Detroit.

Co-Chairman—H. N. MYERS, Perfect Circle Co., Hagerstown, Ind.

ALFRED BOYLES[1] (written discussion): The author has done a good job of describing in a clear and simple manner some of the major factors influencing the structure of cast iron. There are several points which might be mentioned, not by way of adverse criticism, but simply to broaden the scope of discussion somewhat.

It has always seemed to me that a double diagram, such as that shown in Fig. 13, may be very misleading to some people. A foundryman, unversed in the mysteries of physical equilibria, looks at such a diagram and says: "The gray iron eutectic freezes at 2075 F. The white iron eutectic freezes at 2066 F. If my castings undercool to 2066 F or below, I am sunk."

Then he may put a thermocouple in a mold and find that the eutectic in his iron is actually freezing at 2025 F. The fracture is gray and the microscope shows Type A graphite flakes in a matrix of pearlite. This foundryman, who does not realize that the diagram represents "the theoretical stable and metastable temperatures of equilibrium in iron-carbon alloys" naturally concludes that the diagram is no good and discounts all theories based thereon. A sharp distinction between "normal structures" and "undercooled structures" established this way may be very misleading. It is safer to say that all castings undercool to a certain extent and that the difference is one of degree and not of kind.

The degree of undercooling needed to produce a white iron is

[1] U.S. Cast Iron Pipe Co., Burlington, N.J.

a function of composition with respect to carbon and silicon, as the author points out. What is not made clear is that Fig. 13 applies to *iron-carbon alloys* and that the presence of silicon alters the diagram very profoundly. I realize that the author could not go into the complexities of a ternary system in a paper of this type. This criticism is offered simply in the hope of avoiding confusion in the minds of some readers by over-simplification of the subject.

A second point of discussion is concerned with the question: "What is the ideal structure?" There is a group of foundrymen who must produce castings of high strength and high resistance to wear. These men are practically unanimous in voting for Type A graphite in a fully pearlitic matrix. As a group they have devoted the most attention to structure and have written the most papers. No one will dispute with them on the structural requirements they must meet in order to produce a satisfactory product.

There is a second group of foundrymen who turn out enormous quantities of gray iron castings without any particular regard to structure control other than to avoid chill. Suppose one of these men reads the papers written by the first group and begins to strive for the type of structure recommended by them. Whether he gains or loses depends entirely on the conditions of service of the castings in question. A mixture of Types A, B and D graphite in a matrix of ferrite and pearlite may be perfectly satisfactory for the class of work this man is producing and his efforts at quality might have been better devoted to improving the soundness of his castings rather than to changing their type of structure.

There is a third type of gray iron casting poured in permanent molds and then annealed to produce a fully ferritic matrix. Some of these castings are very thin and have a very pronounced Type D graphite structure throughout. Their strength, because of the fineness of the dendritic pattern, may be quite high and their resistance to shock may be excellent. Nevertheless, I have heard some of the men producing these castings apologize for their structure in a technical meeting. Actually, a thin casting of the type under discussion, if made in a manner to give Type A graphite of medium size in a fully pearlitic matrix, would probably have been lower in strength and would certainly have been very much lower in impact resistance than the permanent mold casting. The men who apologized had been reading about the ideal structure of cast iron.

This question of structure is something like setting up an "ideal structure" for steel without regard to the conditions of service under which the steel must perform. It would be foolish to use a high quality, structurally controlled steel for concrete bars. Such bars are made from "off heats" unsuitable for other purposes and yet the finished product serves its purpose and is entirely satisfactory from an engineering standpoint. It seems to me that the question of structure control in cast iron should be subject to the same principles. What is good in a bath tub may be worthless in a cylinder liner, but this does not mean that the bath tub would be improved by giving it the same structure as the cylinder liner.

R. C. MCELWEE[1]: Dr. Lindsay raised the question whether or not type B graphite was seriously detrimental to the major properties of gray cast iron. It is my experience that it does affect machining of castings—machinability—for the following reason: If castings are being machined on an old machine tool, or one that is not too rigid in set up, I think it can be appreciated that the tool is moving from moderately hard areas to relatively soft areas in a structure containing type B graphite and associated ferrite. This is apt to cause the phenomenon of tool chatter and result in a number of complaints of poor machinability attributed to hardness. Actually this behavior is due to the relative hardness of pearlite, which is moderately hard, and ferrite, which is quite soft. In such cases the overall hardness of the structure as measured by a hardness test is fairly normal for gray cast iron.

W. R. JENNINGS[2]: Terms such as super-cooling and under-cooling are commonly used around the laboratory. These terms are not too familiar to the man in the shop. It seems to me desirable to define and elaborate upon much of this terminology.

MR. LINDSAY: I am in agreement with what Mr. McElwee said about the effect of type B graphite on machinability. I was doubtful mainly as to whether any conclusive data had been presented regarding the effect of type B graphite on such mechanical properties as the tensile behavior, the toughness, and the transverse strength and deflection. Of course, the effect of type B graphite on machinability would be equally important in view of the machining performed upon gray cast iron.

C. O. BURGESS[3]: When you examine the cross section of a transverse bar of gray cast iron, quite frequently the fine, type D graphite wil be found close to the surface which is explainable on the basis of super-cooling. However, areas of this type of graphite are found deeper in the body of the bar where cooling is slower than at the surface, and often some areas are found in the very center of the bar. This would appear difficult to explain on the basis of super-cooling alone.

R. W. LINDSAY: It is a behavior which I have noticed from time to time. The presence of this type D graphite in the interior of a bar would seem to be due to some factor or factors other than cooling rate. Recently we worked on some bars which showed this very condition. We were investigating the decomposition of combined carbon in pearlite and we noted that the rapidity with which the combined carbon was decomposed depended on the type of graphite present, being more rapid in the presence of type D. Consequently, we were interested in having present only one type of graphite, preferably type A. In order to obtain such samples, we had to turn off the surface of the bar, slice discs from the bar, and then cut our samples from these discs in such a way that the central portion of the bar was rejected. I can offer no valid explanation as to why these areas of type D graphite form in the body of the casting where cooling is slower.

J. T. MACKENZIE[4]: I think you will find it is due to pressure. If you take a chilled specimen in casting, a chilled mold in a round bar, and take it out just after the outside has frozen and quench it, you nearly always get another chill area in the center and that is because the graphite has to expand to form and you have no room for it to expand—it cannot form. That is probably the reason for the difference in the center structure. By the time you get down to that small volume, there is so much pressure that normal graphite cannot form.

MR. LINDSAY (Author's Closure): The author wishes to thank those who took part in discussing this paper. The question of terminology raised by Mr. Jennings is a troublesome one. The original idea was to prepare a paper to present a simplified explanation of the formation and influence of graphite in gray cast iron. However, it was not found possible to do this entirely and technical terminology began to creep into the presentation. At the time the paper was reviewed the opinion was expressed that simplification of terminology was becoming necessary but it was agreed that such simplification would probably not work in too well here. The possibility of preparing some sort of a text to do this job was mentioned.

Since the word "under-cooling" was used in the paper quite a bit, an attempt will be made to clarify its meaning at this time. Figure 17 will be used for this discussion. Any eutectic type alloy will show a curve such as (a) or (b) when it is in the process of cooling and solidifying. There are two such eutectics possible in the iron-carbon system—one, the eutectic in the iron-graphite system, and the other the eutectic in the iron-iron carbide system. When type A or type B graphite forms, it separates directly from the melt as part of the austenite-graphite eutectic. On the other hand, when type D graphite forms it does so as a result of the formation of the austenite–iron carbide eutectic and subsequent decomposition of the carbide in this eutectic at some temperature following solidification to give fine graphite.

In the case where type A or B graphite form, solidification starts with the formation of austenite dendrites and this austenite formation continues in region I down to the austenite-graphite arrest temperature. Assuming the case of no under-cooling, a eutectic mixture forms at this temperature which is maintained constant until solidification is complete. This mixture consists of more austenite plus graphite of type A or B distribution. This solidification pattern for a cast iron which is hypo-eutectic—that is, for a cast iron in which the sum of its carbon content plus one-third of the silicon content is less than 4.3 per cent—is shown schematically in Fig. 18. In lower carbon irons where an appreciable number of austenite dendrites form prior to eutectic solidification, the graphite in the eutectic is forced to form in very confined spaces between these dendrites. As a result the graphite outlines these dendrites very clearly and possesses a type E distribution.

[1] Vanadium Corporation of America, Detroit
[2] John Deere Tractor Company, Waterloo, Iowa
[3] Electro-Metallurgical Corporation, Niagara Falls, N. Y.
[4] American Cast Iron Pipe Company, Birmingham

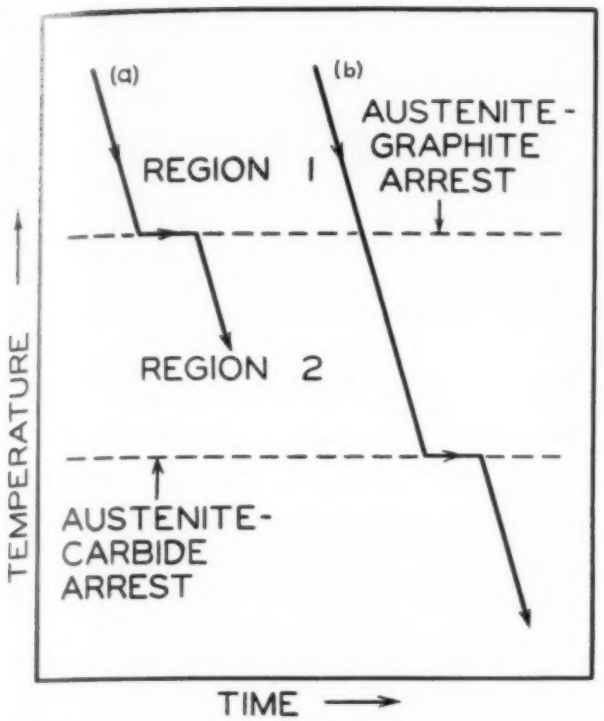


Fig. 17—Schematic representation of undercooling of the austenite-graphite arrest. Curve (a) is the case of no undercooling while curve (b) is the case of complete undercooling.



Fig. 18—Progress of solidification according to the stable system in hypo-eutectic iron-carbon-silicon alloys. (Adapted from Morrogh and Williams.) At A, austenite dendrites in liquid, above the eutectic arrrest the primary crystals forms. At B, austenite dendrites and growing eutectic cells of austenite and graphite; at the eutectic arrest the eutectic of austenite and graphite solidifies on a spheroidal crystallization front. At C, graphite flakes in a matrix of austenite; on completion of solidification the austenite of dendrites and the eutectic become continuous.

However, the solidification of the austenite-graphite eutectic very often does not start when the iron has cooled to the temperature of the austenite-graphite arrest in Fig. 17. It cools below this, or undercools, to a certain extent into region 2. The cooling curve for a mild case of undercooling is shown in the left hand curve—the one for the normal iron—in Fig. 15 in the paper. The degree of undercooling within region 2 will depend upon the cooling rate, the graphitizing power of the iron in terms of its carbon and silicon contents, and the presence of nuclei to initiate graphitization. The austenite-graphite eutectic arrest can be undercooled more severely, when the faster the cooling rate, the lower the carbon and/or silicon contents, and the less nuclei are present.

If undercooling is very severe, the iron may cool according to curve (b). This will result in final solidification taking place with formation of austenite-iron carbide eutectic at the austenite-carbide arrest temperature. If such an iron has sufficient graphitizing power, the carbide in the eutectic will decompose shortly after solidification to fine, type D graphite. However, white or mottled iron would form if the graphitizing power were low—say due to insufficient carbon or silicon, or if the cooling rate subsequent to solidification was rapid enough to bring the iron into a temperature range where decomposition of carbide is relatively slow.

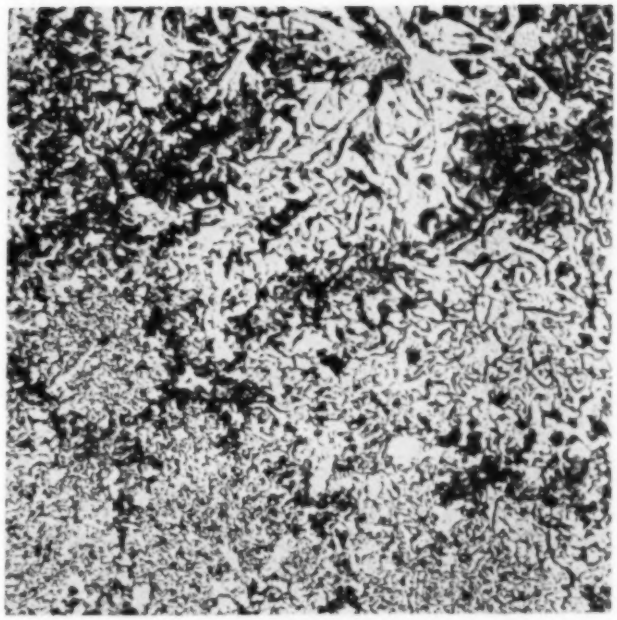


Fig. 19—Graphite structures in annealed permanent mold casting. Type D graphite, bottom left; Type A-B graphite, upper right.

Frequently the structure in gray cast iron is similar to that shown in Fig. 16 (right) in the paper. This means probably that the first regions of eutectic to form in the centers of the cells between the austenite dendrites are austenite-iron carbide eutectic, and the carbides in these areas subsquently decomposed into fine, type D graphite. The heat of formation of these eutectic areas raises the temperature a bit and the remaining liquid toward the edges of the cells finishes solidification in the upper part of region 2 where a mildly undercooled austenite-graphite eutectic forms. This leads to a coarser flake formation at the edges of the cells.

The other possibility, which is much less likely in view of recent experimental evidence, is that greater undercooling into region 2 from region 1 would lead to fine graphite at the cell centers and coarse graphite at the cell edges. The thought is that the graphite that would form in the lower part of region 2 would be fine in line with the solidification behavior of other undercooled solutions. The heat developed by this initial solidification would bring the temperature back up into the upper part of region 2 where coarser graphite would form.

The author considers the discussion written by Dr. Boyles to be quite pertinent to the paper. However, there is one point brought out by Dr. Boyles that might be in error. This involves the statement that freezing of the eutectic at 2025° F. would result in type A graphite. The author has re-examined previous literature and has found that appreciable type D graphite results if solidification takes place in part at this temperature. In fact, type D graphite resulted in many cases where a temperature break in the cooling curve was found at even higher temperatures. Such evidence is clear in papers by Eash,[3] Boyles and Lorig,[2] and Schneidewind and D'Amico.[5,6] Also, the presence of silicon raises the eutectic temperature and this means that a temperature of 2025° F. would be associated with more pronounced undercooling.

It is true that very frequently the type of graphite present in a casting might be a matter of no particular concern. The paper will serve a purpose, however, if it assists in an understanding of the graphite formation problem in those instances where the size and distribution of the graphite is important.

It is true that in permanent mold castings type D structure is quite prevalent and that good service results are obtained. This is a definite case where type D graphite is the result of the very rapid cooling of certain parts or all of these castings and the presence of this type of graphite must be accepted as a fact. It is doubtful if anything could be done to materially lessen amount of type D graphite in such castings (Fig. 19).

IMPREGNATION OF ALUMINUM AND MAGNESIUM CASTINGS

By E. V. Blackmun *
Cleveland, Ohio

FOUNDRIES producing aluminum and magnesium alloy castings and the users of the castings may be confronted with the problem of eliminating leakage in castings which must be pressure tight. Limitations imposed on the designer may be such that the casting contains certain areas from which fine leaks cannot be eliminated. In such cases impregnation is a necessary step in the manufacture of pressure tight castings which will meet the service required of them.

Impregnation should not be considered as a means of salvaging structurally faulty castings because the process is effective only when the leaks are small. Leak-proofing procedures are not expedients to increase production or to maintain a high rate of manufacture in the face of the reduced quality that is sometimes associated with increased production. When used correctly, impregnation methods avoid the loss of serviceable castings and are necessary to compensate for unfavorable design and the limitations of the foundry art.

The selection of sealing materials and the methods of impregnation have been the subject of considerable experimentation over a period of years. In general, the tests have been carried out on production castings. Leak-proofing procedures were developed gradually from the production tests and standard processes were evolved for leak testing and impregnation with sodium silicate solution, tung oil, phenolic resin varnishes, styrene-resin solutions, and drying oil-resin solutions.

Leak Testing

The perfect leak test should exactly duplicate the service conditions. In actual practice, however, the many types of solutions which are held in, or flow through castings under pressure and the innumerable variations in pressure and temperature make actual duplication of service conditions impractical. A few simple test methods have been devised which will indicate whether or not a casting will be pressure tight under a wide variety of service conditions. The selection of the conditions of the test will depend on the service requirements and the size and shape of the castings.

Several methods of leak testing are commonly used. Internal pressure may be applied with compressed air and the leakage revealed by covering the casting with a film of soapy water or by immersing the casting in water. Pressure may also be applied by means of liquids such as glycol, mineral spirits or mineral oil, the selection of the material depending on the service conditions. Tests of this kind are used infrequently and will not be described in detail. The combination of internal air pressure and soap solution or water immersion is most widely used and a detailed description follows.

After all openings in the casting have been covered or plugged, a predetermined air pressure is applied. The usual pressures employed for testing range from 10 to 40 psi. Occasionally pressures as high as 90 psi have been applied, but experience has shown that the lower pressures are just as effective as the higher pressures, provided a suitable means is employed for detecting leaks in the particular casting type.

Special Fixture Types

Figure 1 shows a typical casting clamped in a pneumatic machine ready for pressure testing. Figure 7 shows a bank of pressure testing machines in which small motor parts are being tested.

Other types of fixtures are employed for special cases. Figure 8 shows an automobile cylinder head set up ready to be leak tested, while Fig. 9 shows a large casting plugged and capped ready for leak testing.

Whether or not the leak test will detect all leaks which might show up in service depends largely on the method of detecting the leaks. The most critical test consists of pouring or spraying a film of warm (120 F) soap solution on the casting which has been heated to 130 to 150 F. The heating of the casting may be accomplished by immersion in hot water either before or after clamping in the fixture. The initial temperature should be high enough that the final temperature after testing is not below 130 F. The soap solution is usually made by dissolving 8 oz of neutral soap powder or flakes in 5 gal of water. A typical application of soap solution is shown in Fig. 2.

The sensitivity of the soap solution is decreased by testing castings at room temperature. Very fine leaks which are detected when the casting is heated may not show up when the solution is applied to a cold casting.

Immersion of the casting and fixture in hot or cold water may also be employed to detect leaks. This method is less sensitive than the soap solution method because of the difficulty in observing the escaping air bubbles through the water. Although fine leaks may not be detected by immersion, this method has proved satisfactory in many applications.

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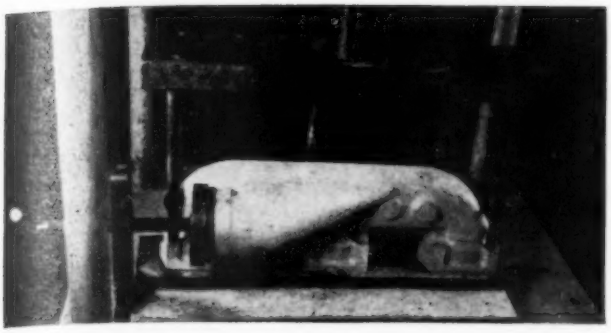


Fig. 1 – Small casting in place in a pressure testing machine. Openings in bottom and at one end of casting are closed and air pressure applied through passages in the base of fixture.

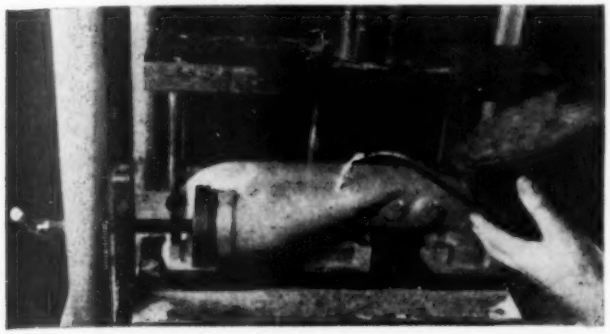


Fig. 2 – Warm soap solution poured over outside of casting reveals leaks as small bubbles when air pressure is applied internally.

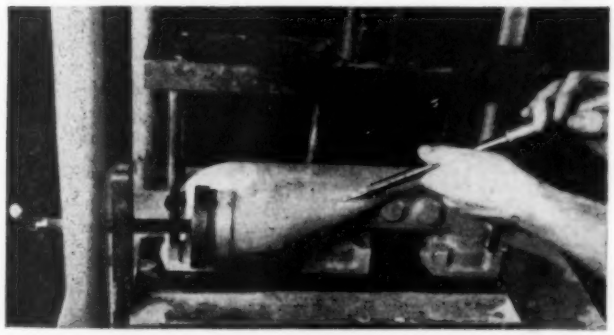


Fig. 3 – Small leaks revealed by the soap bubbles are being closed by peening with blunt chisel in an air tool.

Several methods of eliminating leakage may be used. None of these, however, should be considered as a cure-all for leakage resulting from poor foundry practice. Every effort should be made to eliminate the leakage by changes in foundry practice before the peening or sealing treatments are tried.

After the leakage has been reduced to a minimum, one of several methods may be used to eliminate the fine leaks which remain. Mechanical peening of surface may be sufficient if the leakage is very fine. Somewhat more severe leakage may be sealed with liquids which will set during air drying or baking. The commonest and most economical of these solutions is sodium silicate. Drying oils such as tung oil, synthetic varnishes of the glycerol phthalate or phenol formaldehyde types, styrene resin solutions, and drying oil-resin solutions are also used. The oil or varnish treatments may also give a surface film which provides protection from corrosive attack as well as eliminating leakage.

In some cases fine leaks can be closed permanently by peening the surface metal toward the leak with a blunt tool in a short stroke air hammer as shown in Fig. 3. The blows of the tool should be from the circumference of a small circle around the leak toward the center. The blows of the tool should not be applied directly at the point of leakage. Although the leak may be closed temporarily in this way, later sand blasting, polishing, or machining may reopen the leak. In effect, correct peening should pinch the leak shut.

Properties of Impregnating Materials

It is generally agreed that the ideal impregnating material would possess the following properties:

1. Thermosetting (infusible on reheating).
2. Capable of plasticizing to avoid formation of brittle solids.
3. Low viscosity before setting.
4. Good penetrating properties.
5. Minimum contraction on setting.
6. Insoluble in water, coolants, solvents, gasoline, and oil after setting.
7. Chemically inert to aluminum and magnesium alloys.
8. Economical and readily available.

A preliminary evaluation of impregnating materials is usually carried out in the laboratory by first determining the chemical and physical properties of the material as follows:

1. Examination before setting or polymerization.

   a. Viscosity—The viscosity of the material is obtained at various temperatures. It is generally accepted that the relative penetrating efficiency of an impregnating material is increased with decrease in viscosity.

   b. Chemical Effect—Immerse magnesium or aluminum alloy specimens at impregnation temperature for 24 hr and examine specimens for etching or corrosive attack.

   c. Life of Impregnant—Maintain a sample of the material at room temperature and another sample at operating temperature. Examine samples daily and check viscosities.

2. Examination of setting or polymerization characteristics.

   a. Time and Temperature of Setting—Place a small sample of impregnant in test tubes or capillary tubes and determine optimum setting time and temperature.

3. Examination after setting or polymerization.

   a. Shrinkage—Obtain shrinkage characteristics by measuring the volume change in a small quantity of impregnant during setting.

   b. Solubility—Measure weight change after immersion of set or polymerized impregnant in water, coolant mixtures, solvents, gasoline and oil at room temperature and at elevated temperatures for 48 hr. Simple immersion in high boiling solutions is

usually satisfactory. Solubility tests in solvents, gasoline and oils require refluxing.

c. Mechanical Properties—Examine set or polymerized impregnant for mechanical properties by simple fracture tests. Hard and brittle materials should be avoided.

Laboratory tests have been used to make preliminary evaluation of various new impregnating materials which are proposed from time to time. A small pressure test cup casting is used for these tests. After casting under conditions to control the degree and type of leakage the pressure cups are pressure tested, impregnated, and retested. If a particular application is involved, the cups may be given further tests under service conditions to determine permanence of sealing.

The test casting is a small cup 3¾ in. in diameter at the top, 2⅝ in. in diameter at the bottom, and 4 in. high. Wall thicknesses of ⅜, ¼, and ½ in. are used to correspond to typical production casting thicknesses. The cup casting with standard gating is shown in Fig. 10.

Pressure-Test Cup

In casting pressure cups for studies of impregnating materials, several sets are made with varying degrees of leakage. With a low pouring temperature (1250 F), the gating shown will produce little or no leakage. The amount of leakage is increased by making the following changes in the order given:

1. Reduce 1½ in. top riser to ¾ in.
2. Eliminate 1½ in. top riser.
3. Raise pouring temperature 100 F.
4. Raise pouring temperature 200 F.
5. Raise pouring temperature 300 F.

Using these variations in foundry practice, at least two sets of cups are cast with different degrees of leakage. It is usually sufficient to cast one set with a small amount of fine leaks and one set with severe leaks.

The selection of alloy for the cups will have an effect on the type and extent of leakage. Alloys which may have fine internal shrinkage show a fine leakage. The aluminum alloys 220 (10.0 Mg, remainder Al) or 214 (3.8 Mg, remainder Al), and most of the magnesium alloys are of this type. When impregnating materials are being evaluated, one aluminum-magnesium alloy or one magnesium alloy is included in the tests. This is important because the fine leakage encountered is not common with other types of aluminum alloys.

Pressure-Tight Alloys

The aluminum-silicon alloys, such as 43 (5.0 Si, remainder Al) and the aluminum-copper-silicon alloys, such as 355 (1.3 Cu, 5.0 Si, 0.5 Mg, remainder Al) are used for aluminum castings requiring pressure tightness. Because of their good casting characteristics, they are not susceptible to leakage except in local areas where feeding is inadequate. Test cups cast in these alloys, with the feeding restricted by elimination of the 1½ in. top riser and with high pouring temperature, 1450 to 1550 F, will show small areas of leaks. These leaks are comparable to those obtained in commercial castings with large differences in section thickness. The results of cup tests on these alloys are generally applicable to commercial castings in alloys except the aluminum-magnesium alloys.

The pressure cups require no preparation for impregnation tests after the gates and risers have been removed. Although the number of leaks may be increased by machining the cups, no change in the type of leaks will result. The as-cast surface is usually satisfactory.

Impregnating Compounds

Laboratory tests such as those described, as well as commercial use over long period of years, have established a number of materials as satisfactory impregnating materials for leak-proofing aluminum and magnesium alloy castings. The following general classes of

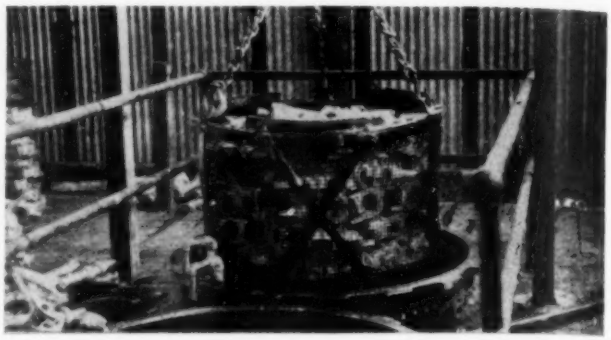


Fig. 4—Basket packed with castings to be lowered into tank of silicate solution. Solution is heated by steam coils in bottom of tank. Tank in foreground is a wash tank, likewise supplied with steam coils.

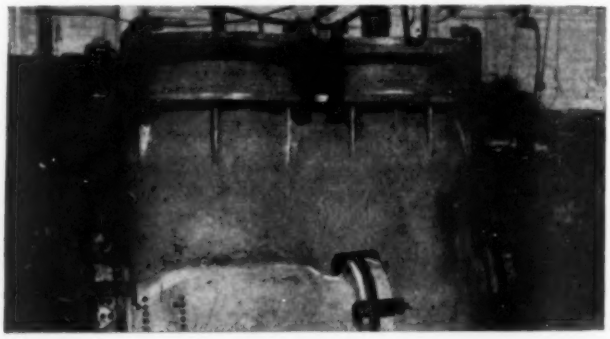
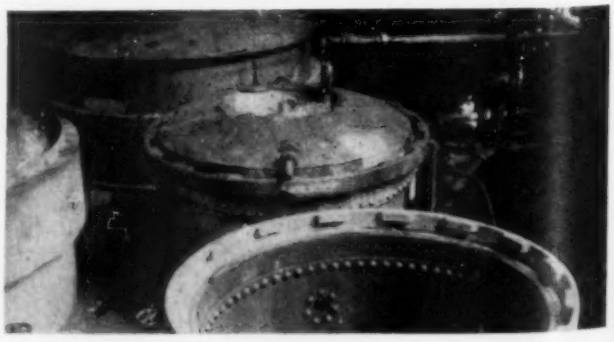


Fig. 5—Large casting being treated by pressure method. Note rubber pads and covers clamped in place.

Fig. 6—Two silicating tanks used for batch pressure silicating. Tank in background has self-sealing cover already in place while tank in foreground is open. Steam coils in bottom of tanks supply necessary heat for maintaining silicate at desired temperature.



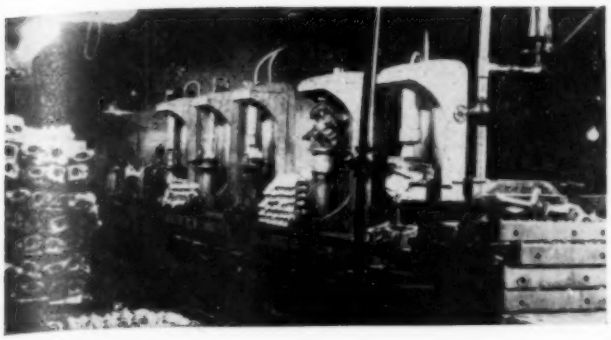


Fig. 7—Battery of pressure testing machines. Castings are held in rubber faced jigs by hydraulic pistons while pressure is applied internally.

compounds are considered satisfactory, depending upon the type of castings, type of leakage, and impregnating method used.

Sodium silicate[2]
Drying oils (tung oil, linseed oil)[1,2]
Phenolic resins
Glycerol phthalate resins
Styrene-drying oil mixtures[2]
Styrene-monomeric resin mixtures[3,4,5]

The materials are listed in order of their commercial use as impregnants rather than in any order of preference. Many of the oil and resin impregnants are described elsewhere (see references) and will not be detailed here. The references describe several of the materials as indicated in the table of materials. Several general references are also given.

Sodium Silicate Impregnation

Considering all of the factors in processing equipment required, procedures, cost and performance of impregnated castings, sodium silicate impregnation is probably the most generally accepted method for aluminum castings and is widely used for magnesium castings. The fine leakage which results from microshrinkage in magnesium castings and fine leakage in some aluminum castings may be impregnated more successfully with monomeric resin-styrene solutions.

A careful selection of the sodium silicate must be made, since certain grades may contain harmful alkalies. A ready mixed solution of the following composition having an SiO_2 to Na_2O ratio of approximately 3.5 to 1 is recommended.

Silica	(SiO_2)	27.0—29.0 Per Cent
Sodium Oxide	(Na_2O)	8.0— 9.0 Per Cent
	(Fe_2O_3)	
	(CaO)	
Impurities	(MgO)	2.0 Maximum
	(SO_3)	
	(NaCl)	
Water	(H_2O)	Balance
Baume' Gravity at 65 F		39°—41°

For the treatment of castings with fine porosity it is necessary to dilute with water to 30° Baume' (at 65 F) for best results.

Castings are immersed in a solution of sodium silicate at 150 to 200 F for a period of four or more hours. At the end of this time they are removed, washed in water, and then either air dried at room temperature or baked at a temperature of 215 to 300 F. After the air drying or baking operation, castings are ready for testing.

One steel tank is required for the sodium silicate solution and another for the washing water. Both tanks should be provided with a means of heating, such as a steam coil at the bottom, to keep the contents at the required temperature. An open end air line in to the silicate should also be provided as a means of agitating the solution when it becomes too sluggish and to keep a sludge from forming on the bottom. A mono-rail hoist above the tanks will facilitate the handling of the castings from the silicate solution to the wash.

The silicate solution should be heated to 150 to 200 F before the castings are placed in the tank. Small castings may be packed in steel wire baskets, such as shown in Fig. 4, in such a manner that the solution will drain out freely from each casting. The basket should be totally submerged in the silicate and left for a period of four or more hours, depending on the type and extent of the leakage. Large castings may be suspended in the solution without being placed in a basket.

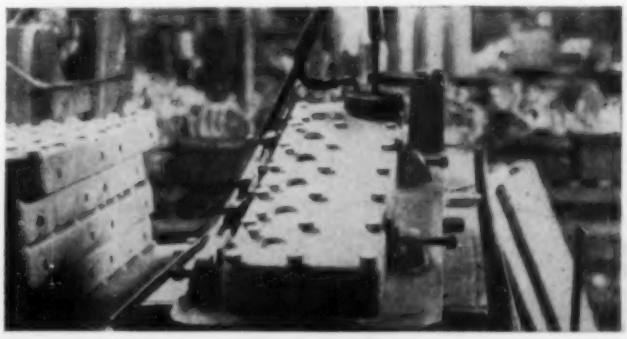
Immediately after removal from the silicate tank, the castings should be drained and thoroughly washed in water, preferably at a temperature of 150 to 180 F. Removal of excess silicate from the casting surfaces is desirable from the standpoint of appearance.

The silicate may be dried at room temperature or baked at elevated temperatures. If air dried, the time required will be somewhat longer than when an elevated temperature is employed. To facilitate production, a short baking treatment is preferred after the castings have been air dried for approximately an hour. Heat treated castings requiring a precipitation treatment at 300 to 350 F can often be baked at the same temperature as used for the precipitation treatment. Heat treated castings in which artificial aging is not desired may be baked satisfactorily at 215 F for one hour. For nonheat-treated castings a temperature of 300 F for one hour is satisfactory.

Pressure Silicating

Large castings that cannot be conveniently immersed in a tank, and castings with wall thickness greater than ½ in. can be satisfactorily treated by the pressure method. The openings in the castings are closed and

Fig. 8—Leak testing an automobile cylinder head casting. Note jig providing means for clamping rubber pads over various openings. When assembled entire unit is immersed in water to detect leaks.



hot silicate introduced on the inside under pressure. After sufficient time, silicate is drained off, surface silicate removed by washing, and the casting allowed to dry.

Special equipment is required for pressure silicating, including suitable jigs, clamps, plugs, and covers for sealing the various openings during silicating. For circular openings up to 3 in. in diameter, ordinary iron pipe plugs may be used. Soft rubber pads held in place by cover plates and clamps will serve to close circular and irregular openings larger than 3 in. in diameter. A source of air pressure, regulator valves and pressure gauges are necessary for controlling internal pressure.

After all but one of the necessary plugs have been inserted, sodium silicate solution at a temperature of 150 to 200 F is poured in until the chamber is full. The last plug, which also serves as an air connection, is put in place and a predetermined pressure applied. Too great a pressure will, of course, burst the casting so it is necessary to maintain the pressure at a safe figure. As a rule a pressure of 30 psi is sufficient.

Treatment Period Varies

The time of treatment depends upon the condition of the casting; as little as 5 min is sufficient in some cases and as much as 4 hr in others. During this time it is necessary to keep the silicate solution at a temperature of 150 to 200 F by means of some external heating method. Where all metal plugs have been used, the whole unit may be heated in a furnace. If rubber pads are used this method of heating is not satisfactory and it is necessary to direct jets of steam or gas torches on the casting surface. When applying heat, care must be taken that the internal pressure due to expanding silicate does not become too great. In some cases the external heating alone develops sufficient pressure without any air pressure being applied. Figure 5 shows a large casting set up for pressure silicating.

Immediately after draining the silicate solution and removing the plugs and covers the casting should be washed thoroughly in water at 180 to 212 F and baked or air dried as outlined under the immersion method.

Presence of air in the minute pores of a casting prevents satisfactory filling of these pores by the silicate solution, particularly when immersed at atmospheric pressure. Likewise, it is not always economical to treat small castings individually by the internal pressure methods. The batch pressure method combines the advantages of both these methods and forms a very efficient process. With this latter method, a batch of castings is immersed in the sodium silicate solution, and pressure applied to the surface of the solution.

Batch Pressure Silicating (Immersion)

A suitable tank for batch pressure silicating is shown in Fig. 6. These tanks are commercially supplied for rubber curing, artificial aging, impregnating, and processing. The flanged covers are sealed by a self-closing rubber gasket after being locked in place. The silicate solution in the tank may be heated by a steam coil, an external steam jacket, or electrical immersion heaters. A high pressure air line to supply the necessary pressure leads into the tank below the level of the solution, serving as the source of pressure and also as a means of agitating the solution. A pressure regulator in the air line and a pressure gauge in the head of the tank

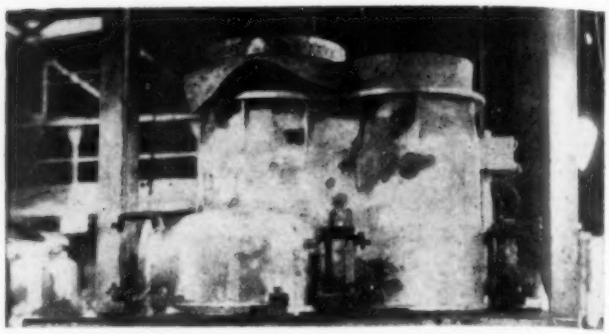


Fig. 9—Leak testing a large silicate treated casting. Soap solution is painted on surface to aid in detecting leaks.

provide a means of controlling the internal pressure applied. An open steel washing tank equipped with means for heating is also required for the purpose of washing the castings after silicating.

The castings for silicating may be packed in steel wire baskets and immersed in the silicate solution at 150 to 200 F. The tank cover is immediately placed in position and internal air pressure applied. The castings should be held in this solution for 2 to 6 hr under 50 to 100 psi pressure. The time and pressure elements are governed wholly by the condition of the castings with respect to leaks. The usual run of foundry castings may be satisfactorily impregnated over a 6-hr run at 50 lb pressure. It has been found that the use of a dilute sodium silicate solution (30 Baume' at 65 F), previously described, is required with this method of silicating. The finer porosity requires more dilute solutions.

After treatment the castings are removed and thoroughly washed in water, preferably at 150 to 180 F. The castings may then be air dried or baked at 215 to 350 F in the same manner as described under "Immersion Silicating."

Maintenance of Silicating Equipment

Sodium silicate solutions are extremely hard when air dried or baked and if allowed to accumulate in any quantities, either inside or outside of the silicating room, may be extremely difficult to remove. For this reason it is advisable to keep everything washed well with hot water and avoid any necessity for cleaning up when the solution has hardened.

Unless watched carefully and agitated each day, a sludge deposit will collect on the bottom of the silicate tank and in time solidify about the steam coils or other heating elements. To remove this deposit all loose material should first be removed and the solid material removed with hammers and light crow-bars.

The washing tank requires a change of water frequently. Failure to do this will result in an undesirable film of silicate remaining on the surface of the castings after washing.

Styrene-Resin Impregnation

Resins used in styrene-resin impregnants are usually made from glycerine, phthalic anhydride, and fatty acids, although the exact composition is not revealed by the manufacturers. Although monomeric styrene is reactive, it is stabilized with guaiacol by the manufac-

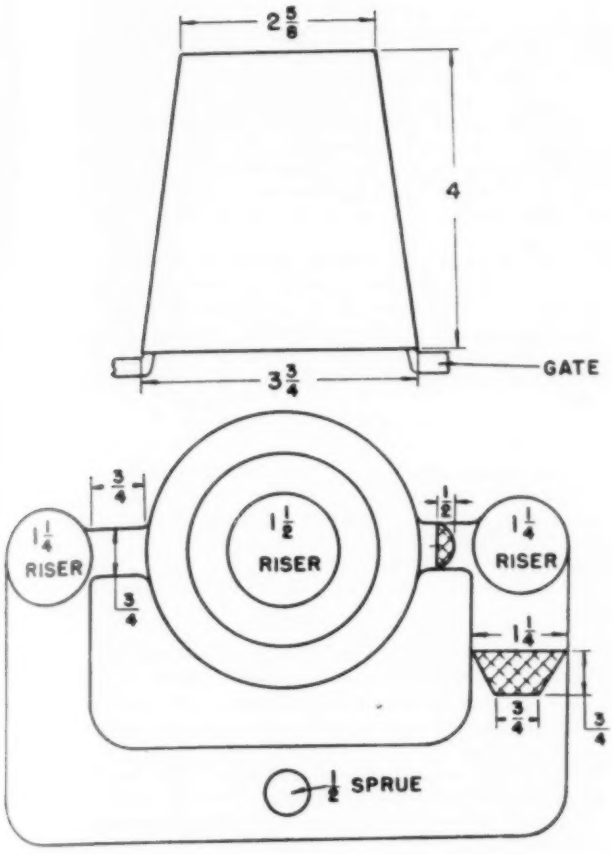


Fig. 10—Pressure test cup showing gating.

turer for long periods of time. The mixture of the two is reactive but stable enough for normal cycles, provided reserve supplies are not stored for long periods of time. A temperature below 70 F is recommended to avoid reaction during use.

Usually 25 to 40 per cent base resin in styrene is recommended for aluminum and magnesium castings. A viscosity of 10 to 25 centipoises should be maintained during use.

The direct pressure method or the batch pressure method may be used to impregnate with styrene-resin mixture. The impregnation is carried out in the same way as when silicate is used except that no heating is permitted. After impregnation the castings are cleaned with kerosene or a stabilized alkaline cleaner. The impregnant is then cured or polymerized by baking for 2 hr at 275 F. An additional 2 hr at 325 F may be employed when maximum resistance to solvents, such as gasoline, is required. Surface exudation may occur during baking at atmospheric pressure. If this is objectionable, baking under 90 lb carbon dioxide, nitrogen, or air pressure will minimize exudation.

Vacuum-Pressure Method

If the batch pressure method is not completely effective in sealing leakage and if the direct pressure method is not feasible, the vacuum-pressure method is recommended for styrene-resin impregnation. The parts are placed in a large sealed tank and vacuum of 28 in. is obtained and held for 15 min. The vacuum is then allowed to draw the impregnant into the tank, covering the parts. The vacuum is then released and a pressure of 50 to 90 psi is then applied for 15 to 30 min. The parts are then removed, washed, and baked.

Impregnation and Other Treatments

Cleaning, anodizing, painting, heat treating procedures may have some effect on impregnation or vice versa. Since both aluminum and magnesium castings are usually impregnated by the foundry before finishing, the effect of impregnation on protective coating treatments for aluminum and AMC chemical treatments on magnesium alloys should be considered.

Films of impregnant remaining on the surfaces of castings will interfere with the formation of uniform, continuous protective coatings. For this reason it is important that the castings be cleaned thoroughly after impregnation. If this is done, no interference should occur. Castings which are properly cleaned should show no visual evidence of impregnation. The protective coating treatments may tend to open up additional leakage which was not apparent before the treatment because most such treatments break the surface skin on the casting.

In some cases, it may be necessary to re-impregnate after applying the protective coating treatment. This may be done when Alumilite* treatments are applied to aluminum alloy castings or when AMC treatment A is applied to magnesium alloy castings. Impregnation with sodium silicate should not be applied to aluminum alloy castings after application of Alrok* coating because the impregnating solution destroys the coating.

If a large amount of additional leakage results from the application of one of these treatments, it may be avoided by giving the castings a light etch prior to impregnation. A slight amount of etching will open the leakage which is closed by a small amount of surface metal and permit penetration of the impregnant. Aluminum alloy castings may be etched in dilute sodium hydroxide solutions. Magnesium castings may be etched lightly in a solution containing eight parts of concentrated nitric acid, two parts of concentrated sulfuric acid, and 90 parts water at room temperature. Thorough washing is necessary after these etching treatments.

Effect of Baking Process

The baking or setting step during the process of impregnation may have an effect on the mechanical properties of both magnesium and aluminum alloy castings. Since the effect of heating cycles may be cumulative, care must be exercised in the selection of the baking process. In general, solution heat treated castings are most sensitive to the effects of reheating.

In the case of aluminum alloy castings, a maximum baking temperature of two hours at 250 F will not substantially affect the properties of solution-heat-treated and quenched castings. Although the effect of the baking of impregnants will have a smaller effect on the properties of artificially-aged aluminum alloy castings, the additional aging during the baking should be taken into account.

It is often possible to combine the artificial aging and sodium silicate baking of aluminum alloy castings.

*Patented process owned by Aluminum Company of America.

This procedure avoids any change in mechanical properties and provides a more economical treatment. The mechanical properties of solution-heat-treated magnesium alloy castings will not be affected by baking within a range of 6 hr at 275 F to one hour at 350 F. In general, as-cast and solution-heat-treated and artificially-aged magnesium alloy castings may be reheated to a maximum of 400 F without affecting the properties.

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DISCUSSION

Chairman: R. E. WARD, Eclipse-Pioneer Div., Bendix Aviation Corp., Teterboro, N.J.

Co-Chairman: W. MADER, Oberdorfer Foundries, Syracuse, N.Y.

CHAIRMAN MADER: Impregnation of aluminum and magnesium castings is a relatively common procedure due to their tendency towards micro-shrinkage and micro-porosity because of their low specific gravity. It is a recognized procedure to reduce leakage of castings in these light alloys. Impregnation should not be considered a salvage operation. Many people have gotten into trouble trying to salvage castings with shrinks and other defects in them.

Impregnation is a costly operation involving much material handling and investment in expensive autoclaves. In Mr. Blackmun's paper he says, "The usual pressures are employed for testing range from 10 to 40 psi. Occasionally pressures as high as 90 psi have been applied but experience has shown the lower pressures are as effective as the higher pressures." We make aluminum castings that must withstand pressures of 700 to 800 psi in service. These castings should be pressure tested at the pressure they are supposed to withstand in service and not at some lower pressure.

The author states that sodium silicate is a commonly accepted impregnant. There are other possible impregnants but sodium silicate performs satisfactorily. It is economical and could be used to good advantage.

Another use for impregnants is as a coating against corrosion. Sometimes it is hard to aluminize or anodize a casting, particularly with internal cavities, and such plastic impregnants as styrene, could be used to withstand corrosion and cavitation and certain other conditions.

MEMBER: I would like to ask if the sodium silicate referred to is commercially pure sodium silicate or some mixture commonly sold on the market? There are two or three different brands of mixtures that contain sodium silicate.

MR. BLACKMUN: The solution we used is a ready-mixed solution having a silica to sodium oxide ratio of approximately 3½ to 1. The reasons for the high silica ratio are fairly obvious, as you do not want a highly alkaline sodium silicate solution. You may get into difficulties with some actual etching or chemical attack on aluminum casting if a low silica ratio solution is used. The actual composition of this ready-mixed solution is 27 to 29 per cent silica, 8 to 9 per cent sodium oxide, and small amounts of other oxides of iron and calcium, magnesium sulphate, and sodium chloride. The total of these impurities probably should not exceed about 2 per cent.

Water makes up the balance. This solution has a gravity of about 39 to 41° Baumé. We have found that this solution is quite easy to obtain from a number of the chemical companies that make silicates. They seem to be willing to prepare this composition if they do not have the commercial product available.

MEMBER: What is the nature of the leaks you have in the castings?

MR. BLACKMUN: Generally leakage in aluminum or magnesium alloy castings is due to shrinkage. Shrinkage porosity, which is interconnected sufficiently to permit the passage of air or solution through the wall of the casting is the primary cause of leakages. I do not believe basically there is any other defect that you can directly connect with leakage, except possibly dross or oxide films.

MEMBER: How effective is impregnation when you have dross or oxide?

MR. BLACKMUN: Impregnation methods are not too reliable when leakage occurs through oxide films. That is, there seems to be a great deal of variation in the behavior of leakage through oxide films or leakage associated with oxide films. We are inclined to avoid impregnation of that type leakage. Before you even get to an impregnation step you find quite erratic behavior from the standpoint of pressure testing when the leakage is associated with dross or oxide films.

CHAIRMAN MADER: Oxide skins or dross inclusions cause fissures in the casting. Heat may cause expansion or contraction and will open up these fissures and you will have leakage even after impregnation. I think it should be a standard practice where you have x-ray facilities to see what the defect is before you impregnate the castings. Often it is possible that you may impregnate the castings and still lose them because the defect is dross. You will have trouble later on, especially if you machine the casting in the area where the leakage occurs.

M. H. GOULD[1]: Styrene resin is often baked 2 hr. at 325° F. What would happen to this material in the case of service that involved temperatures of around 300°-F.? Would a styrene resin stand up under these conditions or would it eventually break down?

MR. BLACKMUN: You reach complete polymerization of that type of material in about 2 hr. at 350° F. From its nature I would expect that continuous exposure at temperatures of that order would probably tend to make it become somewhat brittle. I do not know that that involves any complications in service, however unless the leak through the casting is so large that perhaps it gets so brittle that it will break up. Leakage after impregnation might develop later on.

CHAIRMAN MADER: Styrene impregnants can polymerize at lower temperatures and in fact with added catalyst may polymerize even at room temperature. That is one of the things you must watch out for. If you let the impregnant stand around it may thicken up and eventually you will have a mass of solid impregnant.

J. J. BROWN[2]: Is there any pressure range beyond which impregnation is effective or is it effective at all pressures?

MR. BLACKMUN: I do not believe that the pressure range is the important factor. The nature of the leak and type of defect which causes the leak are more important from that standpoint. I see no reason to believe that an impregnation of a casting would have any greater tendency to break down at high pressure than it would at low pressure. If the walls of the casting are such that you get some deformation under high pressure, however, you might break open some leakage in service or in pressure testing. I do not believe that pressure considerations are necessarily tied in with the impregnating material.

MR. BROWN: In other words, if you had a casting which had to maintain 800 pounds and you get a leakage, the impregnation would be just as effective as though it only had to maintain 40 pounds, is that right?

MR. BLACKMUN: I think that is true. At the same time, it is also true that it is more difficult to impregnate severe leakage in a casting that is going to be used at that higher pressure than to impregnate a casting that is going to be used at a lower pressure.

[1] Aluminum Co. of America, Fairfield, Conn.
[2] Edw. S. Christiansen Co., Chicago.

RADIOGRAPHY OF GUN METAL CASTINGS

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IN RECENT YEARS much attention has been devoted to the radiography of steel and aluminum castings while little progress has been reported on the radiography of bronze castings. The radiographic standards for steel and aluminum castings have not only contributed to increased safety, higher quality and fewer rejects, but have also caused the respective foundries to learn more about the foundry practice by which high quality may be secured.

The radiography of bronze castings has been neglected, probably, because a large proportion of bronze castings require a pressure test. The pressure test, however, may be very deceiving as to the true quality of the metal. It yields no information concerning the metal below the thin skin of the casting which may be removed by abrasion, corrosion, machining or drilling.

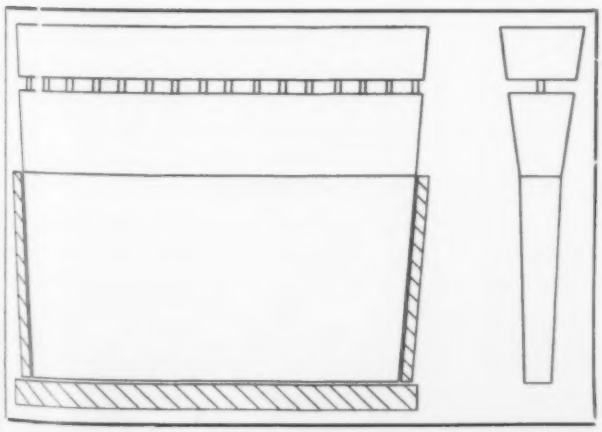
The pressure test is limited in pressure and time of application. There is no provision for sudden overloads caused by water-hammer or the slow, continued penetration of corroding solutions in service. It is not surprising that many castings which pass rigid inspection tests still fail by leakage after only two or three years in service.

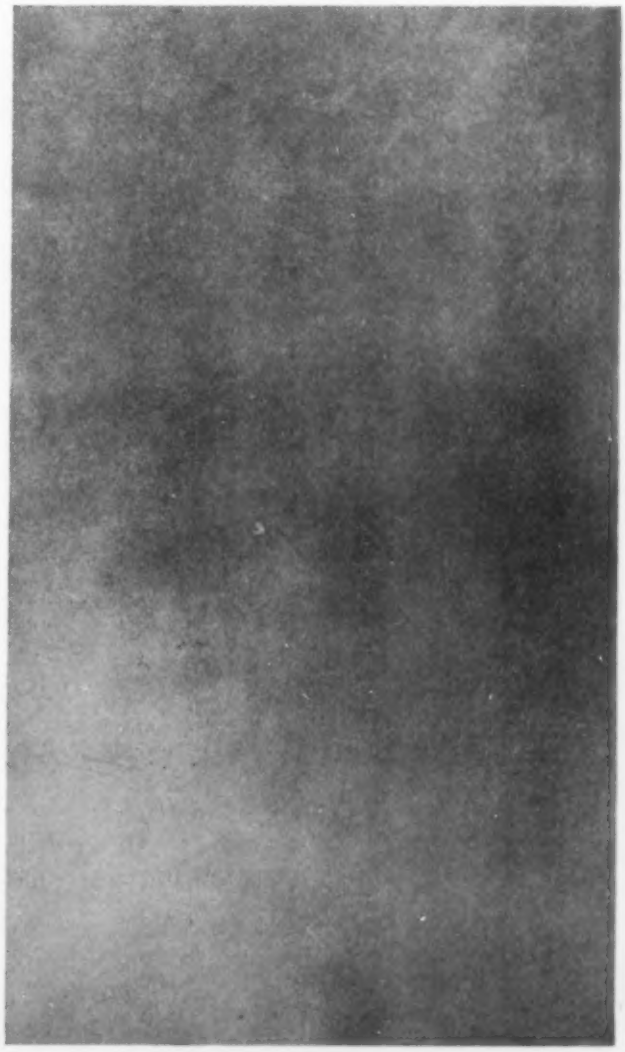
For these reasons there is a definite need to supplement the pressure test with a more discriminating method of testing. The fact that a casting leaks should be supplemented by knowledge of the cause of the leakage. Knowing this, the foundry engineer may make appropriate changes in melting, molding or casting practice in the particular foundry.

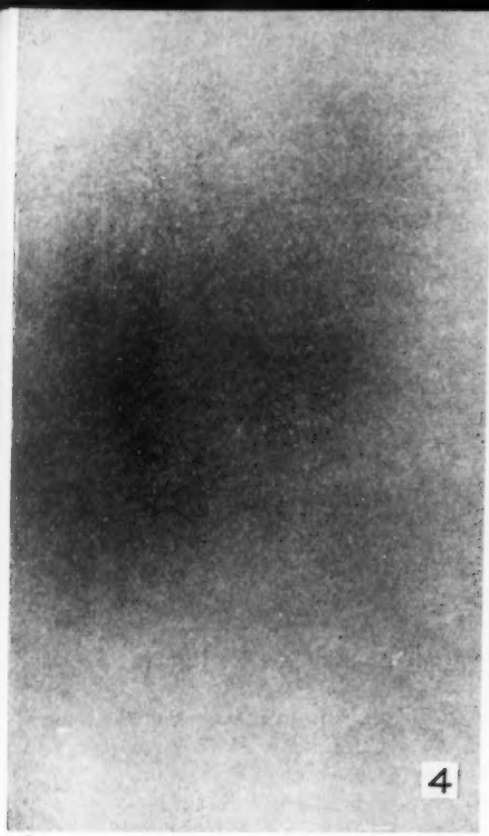
The Navy recognized the importance of radiography of steel castings in 1942 when the "Radiographic Standards for Steel Castings" were released by the Navy Department.[1] These standards have been of great wartime value to industry in general as well as to the companies working specifically on naval construction. Once the inspector has a clear mental picture of the standards, it is necessary only occasionally for him to refresh his memory.

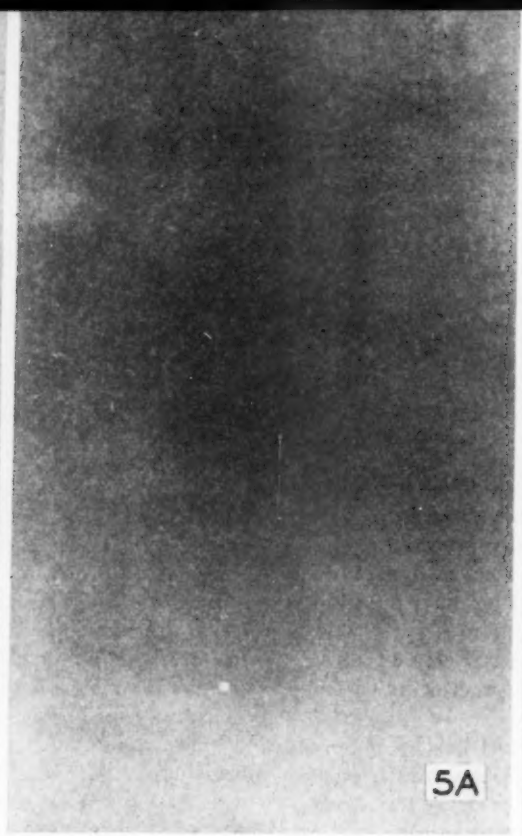
Radiographic standards for aluminum casting were developed shortly after the steel standards. This work was done largely by the Army Air Corps.[2] These standards have proven beneficial to users as well as manufacturers of aluminum castings. The user is assured of

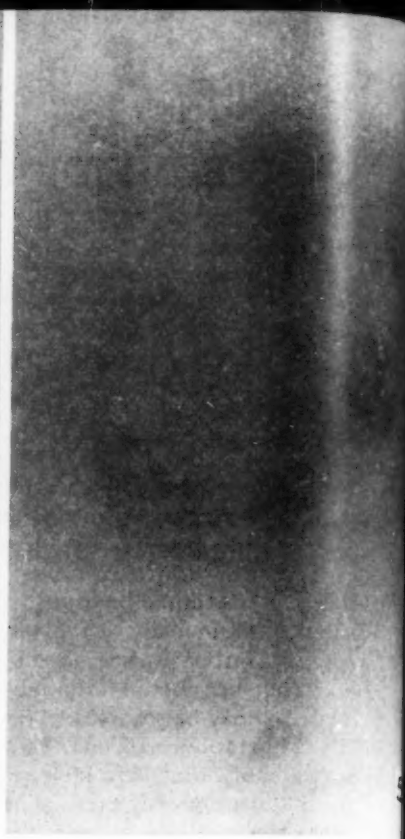
Fig. 1 (below) —Casting plan of plate. Fig. 2 (right) — Radiograph section (center portion) taken from radiograph of entire plate. Casting made as shown in Fig. 1.











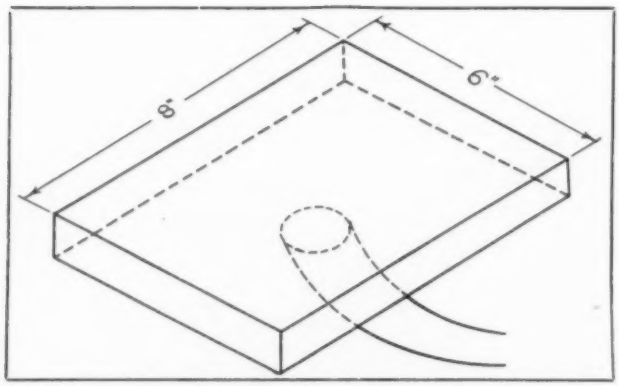


Fig. 3—Sketch showing dimensions of plate with reversed horn gate. Fig. 4—Spongy shrinkage; no riser. Unless otherwise stated, all plates cast horizontally. Fig. 5A—Spongy shrinkage. Riser 4 in. diameter and 2 in. high in center of plate. Fig. 5B—Spongy shrinkage. Riser 3 in. diameter and 4 in. high in center of plate.

castings of good mechanical properties and the manufacturer, when he has pointed out to him specific types of defects, may more readily correct that part of his foundry practice shown to be at fault. The net effect of radiographic standards has been an increase in production of castings of good quality.

It is now time for the bronze foundries to recognize the advantage of radiographic inspection, if not for all castings, at least for selected castings and pilot castings. Several large manufacturing companies producing bronze castings have set up company standards for their own use. In general these standards consist of typical radiographs of castings, more or less arbitrarily designated as to quality on the basis of the radiographs. The standards usually have three very broad classifications. These classifications are: satisfactory, borderline and unsatisfactory.

The various company standards suffer from the common fault of being based on such a variety of sizes and shapes of specific castings that the radiographic standards find limited use outside the particular fields of operation of the respective companies. At the present time variations exist in the definitions of the terms "satisfactory" and "unsatisfactory" in the various specifications of quality of bronze castings.

The purpose of the present work is to identify various typical defects in gun metal by means of radiographs. More than one hundred gun metal plates 8 x 6 x 1 in. of nominal composition 88 per cent copper, 8 per cent tin and 4 per cent zinc were cast under carefully controlled conditions so as to produce the defects. Complete records of the melting and molding practice were kept to facilitate the reproduction of the defects when required. The defects included distributed shrinkage, hot tears, sand and slag inclusions and gas porosity.

No attempt was made in the present investigation to evaluate the seriousness of the defects or to place them in a scale of merit. The investigation was confined to the correlation of defects with foundry practice leaving for future work pressure testing of plates, micro-radiographs, fracturing and other destructive tests.

Experimental Procedure

The bronze was melted either in a lift coil induction furnace or in a gas fired furnace. The materials used consisted of Grade A copper, Straits tin and Horsehead zinc. Clay-graphite crucibles were used for all heats. The copper was melted down under a light charcoal cover and when molten, the tin was added followed by the zinc. When the temperature of the melt was 1150 C

(2100 F), two ounces of phosphor-copper per 100 lb. were added. The crucible was then removed from the furnace, the charcoal skimmed from the melt and the castings poured at 1150 C (2100 F).

The sand used for making most of the molds was No. 00 Albany, faced with a dry graphite wash. Some of the molds, especially those made to produce tears, were made with a synthetic sand because of its greater strength.

The radiographs were taken with 220 kv and ten milliamperes with an exposure time of five minutes and a 36 in. target-to-film distance. A lead filter 0.03 in. in thickness was used at the source and lead intensifying screens 0.005 in. in thickness were placed on both sides of the film. Eastman type F x-ray film was selected on account of its widespread use and fine grain size. The density of the radiograph was kept as close to 1.25 as possible; a stepped penetrameter of gun metal of 2, 4 and 6 per cent sensitivity was used.

Radiographically Sound Bronze—Preliminary Investigation—Although flat plates of gun metal may be difficult to feed, there are several methods by which plates of good quality may be cast either horizontally or vertically. If good directional solidification is provided by the use of chills, padding or insulation, good results will be obtained. A method of casting a plate in a vertical position in the mold is shown in Fig. 1. The size of the pencil gates is such that little splashing and a minimum of turbulence takes place. At the same time the pencil gates are of sufficient size that feeding is permitted at the proper time.

The chills along the edges of a plate of this size are essential in promoting directional solidification. Although the dimensions of this plate (8 x 14 x 1 in.) are considerably larger than the dimensions of the plate (8 x 6 x 1 in.) used in the major portion of the investigation, it is seen in Fig. 2 that larger size has not proved detrimental to the production of a sound casting because the principles of good directional solidification could be followed.

Defects in Bronze Castings. In common with other alloys having wide solidification ranges, tin bronze is subject to spongy shrinkage when it is not adequately fed. In order to produce conditions favorable for this defect a bronze plate was cast as shown in Fig. 3 with the reversed horn gate entering at the center of the plate. The severity of the defect was controlled by placing risers of various sizes just above the gate.

Riser Factor in Shrinkage

Figures 4 and 5a show that the riser often may be an important factor in determining the extent of the shrinkage. In fact, with no riser whatever this plate has a uniformly distributed shrinkage. As shown in Fig. 5b, the riser may cause the shrinkage to become more concentrated.

The weak skin strength of the bronze gave some difficulty in artificially producing the concentrated type of spongy shrinkage. In castings made in a horizontal position in the mold, the surface of the casting had a tendency to be caved in by atmospheric pressure over the positions in the plate where the shrinkage was intended to occur.

This difficulty was not so serious when the plate was cast vertically as shown in Fig. 6. With no risers and chills one-eighth inch in thickness placed along the edges, the plate was made to solidify without the benefit of proper directional solidification. The skin of the casting did not cave inward under atmospheric pressure in spite of the concentrated type of spongy shrinkage at the center of the plate, Fig. 7, the last portion of the casting to solidify. The chills had a strong influence in causing the shrinkage to be concentrated.

The casting, made exactly the same as in Fig. 6 except for the omission of the chills, had "lacy" and interconnecting shrinkage which appeared to follow the

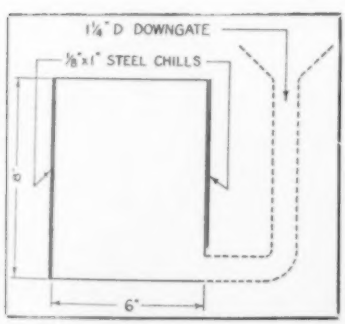


Fig. 6—Sketch showing dimensions and location of chills in the vertically-cast test plate.

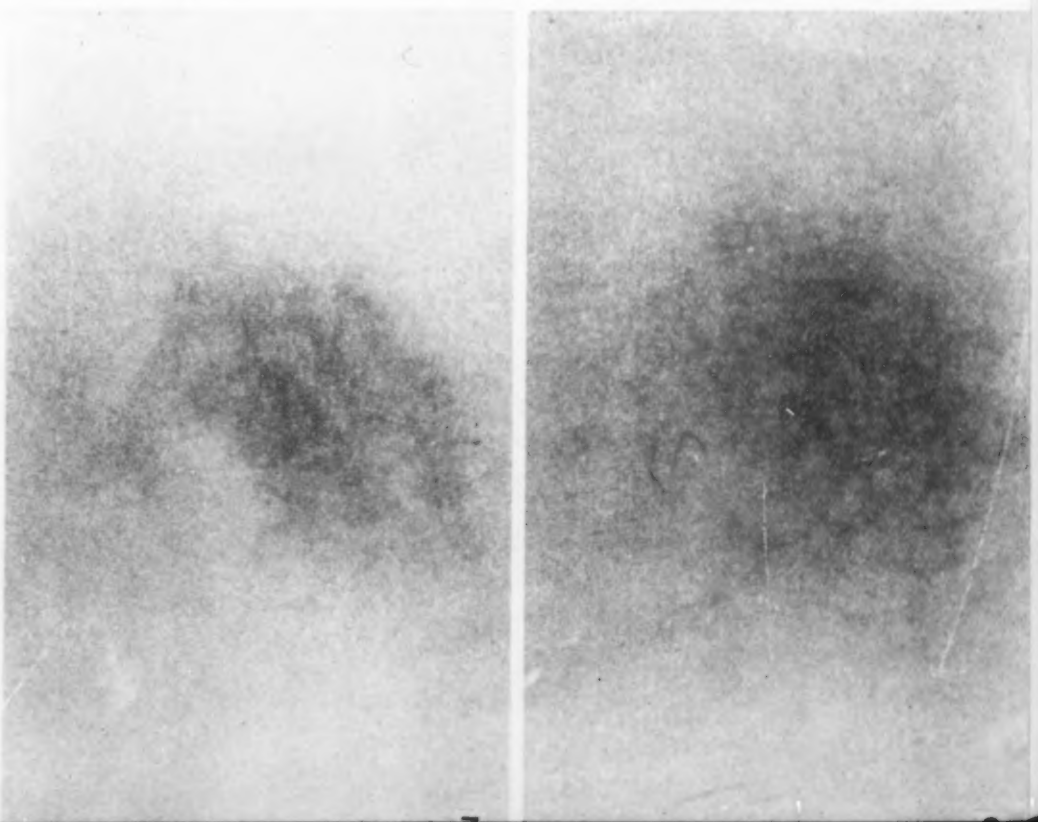


Fig. 7—Radiograph showing spongy shrinkage concentrated in center of plate, due to chills.

Fig. 8—Interconnecting "lacy" shrinkage; the plate was cast vertically; no riser employed.

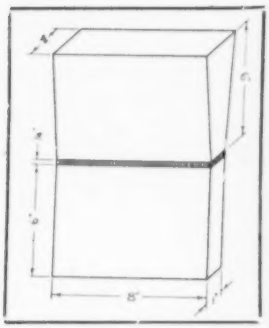


Fig. 9—Sketch showing plate dimensions, riser and core sand strainer.

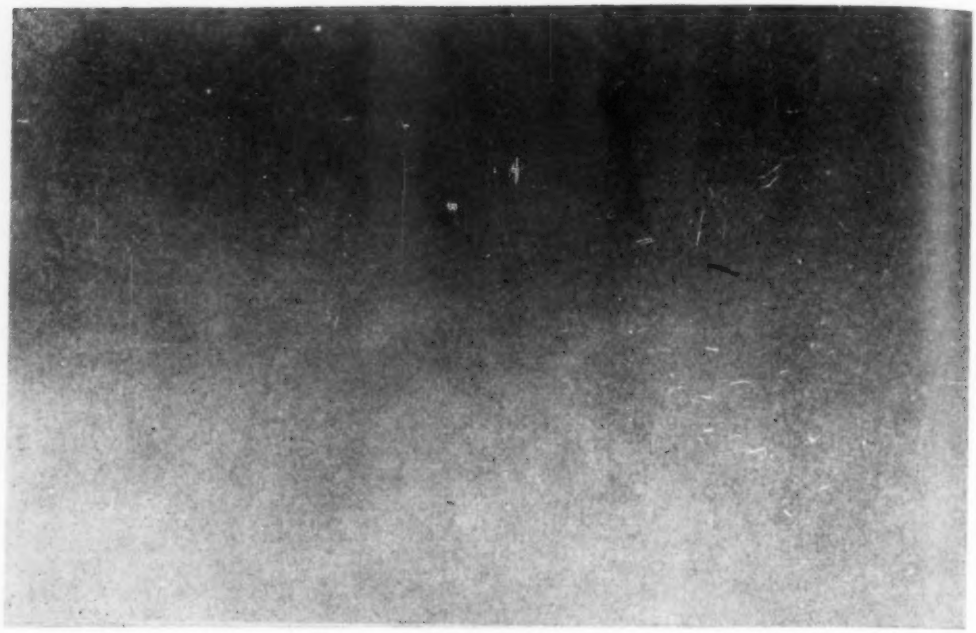


Fig. 10—Radiograph showing piping shrink; vertically-cast plate.

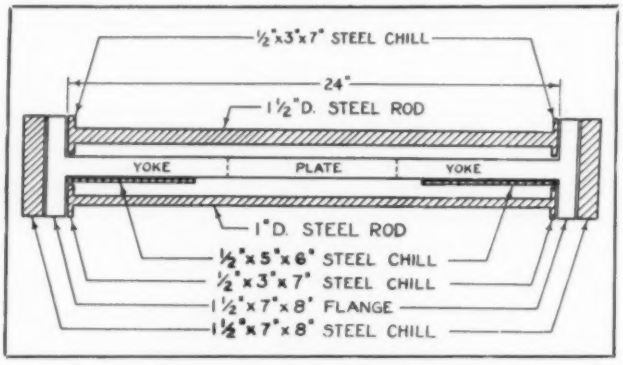


Fig. 11—Cross-sectional sketch of the hot-tear test casting showing chills, steel rods and dimensions.

Fig. 12—Photograph showing the hot-tear test casting with the risers and steel reinforcing rods in position.

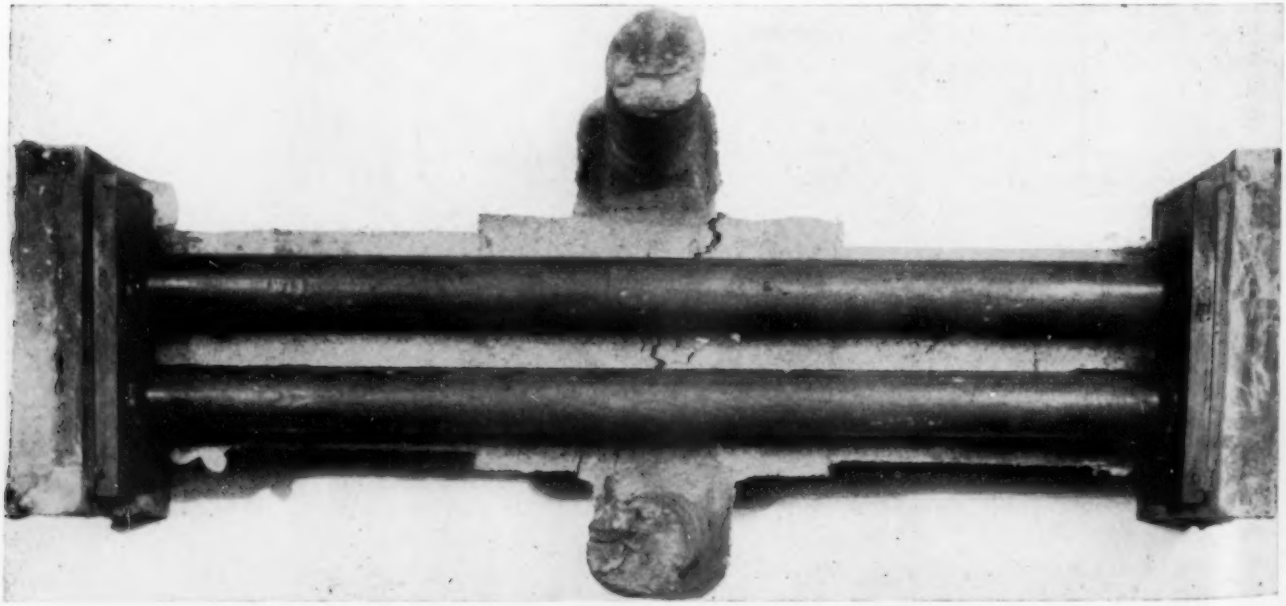
outline of the grain boundaries as shown in Fig. 8. This is one of the worst types of shrinkage from the standpoint of pressure tightness.

A special form of concentrated shrinkage, often associated with ingates of improper dimensions, is called "piping" shrinkage. As shown in Fig. 10, the channels between the dendrites of the casting have a characteristic tuberous appearance.

Bronze Gates

This failure is by no means restricted to the pencil gates which were selected to illustrate this type of failure. Whenever bronze is gated directly into the casting, such as in Fig. 9, rather than into the riser, there is danger from "piping" shrinkage.

The form taken by various defects is greatly influenced, not only by the positions of the gates and risers, but by the contraction of the solidified cast-



ing and the resistance offered by the mold to this contraction. When the mold hot strength is too high, the casting will be pulled apart by its own internal stresses during solidification.

A mold reinforced with steel rods was made as shown in Fig. 11 in order to produce hot tears in the gun metal plates. The crack was made to occur in the center of the plate by keeping this portion of the casting at elevated temperature (in the hot short range) while the remainder of the casting was rapidly cooling to a lower temperature. The gates at the center of the casting also served as risers, thus promoting the desired gradient of temperature. The severity of the hot tear was strongly influenced by the length of the yokes between the flanges. Considerable difficulty was encountered in preventing distributed shrinkage occurring adjacent to the hot tear. The hot tears shown in Figs. 12, 13 and 14 are transverse to applied stress.

Gas in Bronze

Most of the preceding heats were melted under identical conditions. The defects, therefore, may be related directly to gating, risering and molding practice. Quite a different situation exists when the metal is supersaturated with gases which have been dissolved during the melting process. The radiograph in Fig. 15 presents the mottled appearance which is characteristic of bronzes which have distributed gas porosity. This particular metal was produced in a gas fired furnace with a reducing atmosphere (no cover or flux) and was not deoxidized with phosphor copper before the pouring of the casting.

To produce gas porosity artificially, it is necessary to add a suitable combination of reactive compounds to the metal or mold. Such compounds include the following: copper oxide, hydrides of low temperature of decomposition, hydrocarbons such as linseed oil and excessive moisture. The finely distributed gas porosity shown in Fig. 16 was caused by five grams of calcium hydride sprinkled on the bottom of the mold. Best results were obtained when a steel plate $\frac{1}{16}$x6x8 in. was placed on the bottom of the mold to prevent the hydrogen from escaping.

Quite different from the various types of shrinkage and gas porosity which are somewhat dependent on the characteristics of the metal, are the inclusions produced by sand being washed from the mold. These inclusions present a similar appearance in all metals, being of identical material and of common origin. Figure 17 is an example of sand inclusions. A closely associated type of inclusion is seen in Fig. 18 which illustrates dross inclusions. In both instances about equal quantities of sand or dross were admitted to the mold by addition of the materials to the stream of molten metal. Castings with inadequate gating systems and few risers are likely to have these sand and dross inclusion defects.

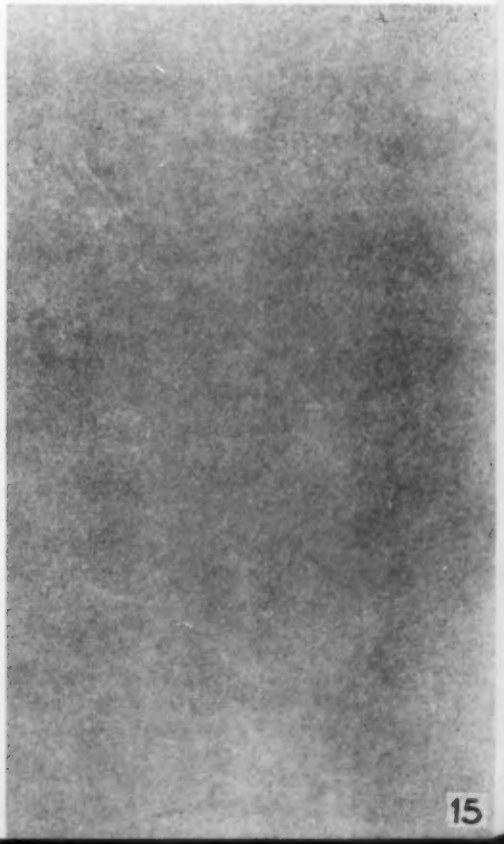
Shrinkage Factors

The results of the shrinkage study show that the extent and type of the shrinkage depends on the gating, risering and chilling of the casting. Optimum height and diameter of risers are necessary for sound plates. Concentrated shrinkage in bronze is produced when the risers are too small in diameter and too high which causes them to freeze first and withdraw metal from the casting. Concentrated shrinkage also occurs when chills are placed along the narrow edges of the plate. Without either chills or risers the shrinkage tends to be dispersed.

Fig. 13—Dispersed spongy shrinkage with severe hot tear. Fig. 14—Hot tear with shrinkage at a minimum. Fig. 15—Gas porosity combined with spongy shrinkage.







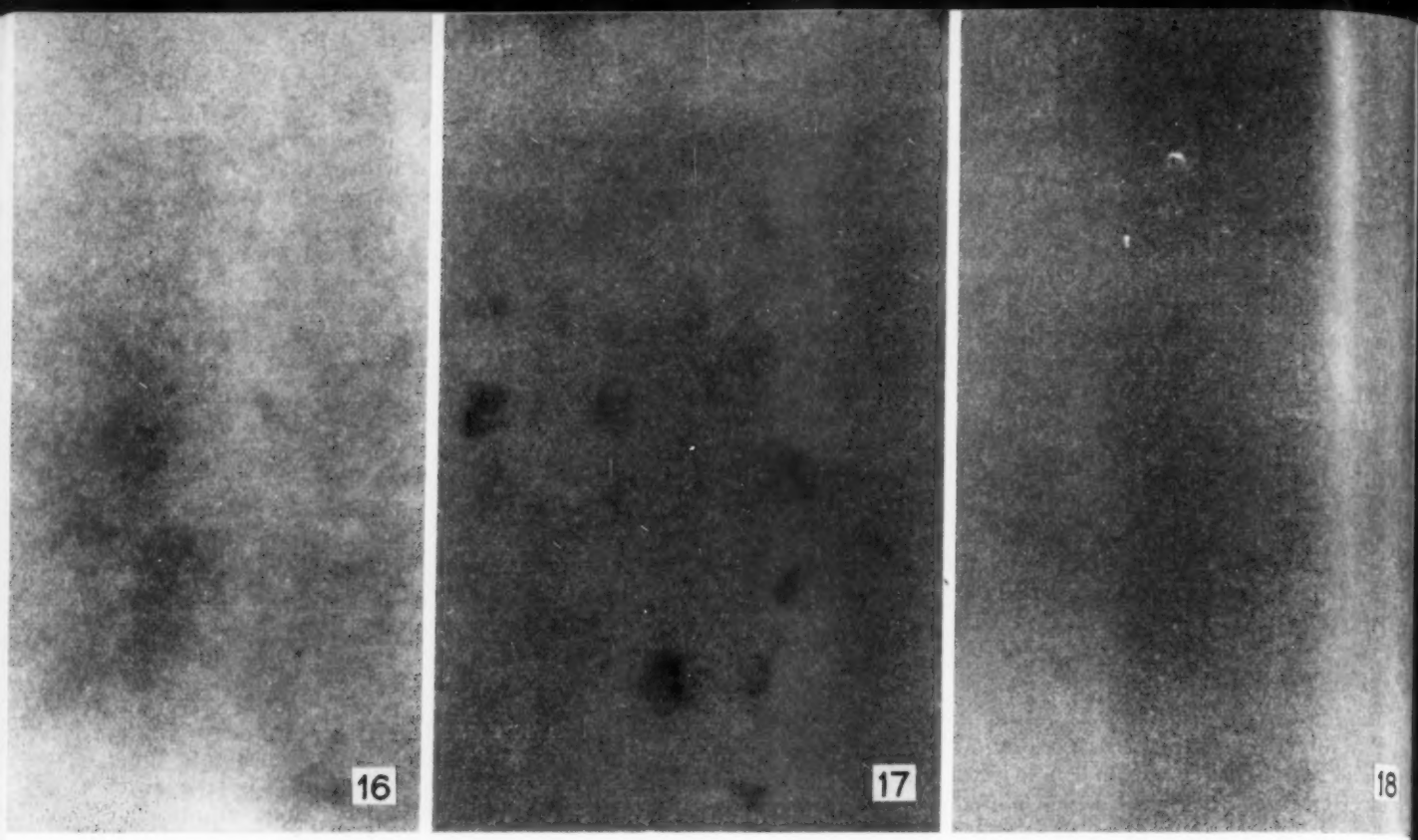


Fig. 16—Gas porosity with some shrinkage. Fig. 17—Sand inclusions shown in this radiograph. Fig. 18—Dross inclusions in vertically-cast plate; no riser used.

It is believed that the control of the shrinkage is also closely related to melting practice. Optimum dimensions of risers, even for the same plate could conceivably be much different if the plate were melted or poured with other procedures.

Hot tears occur less frequently in bronze castings of small size and considerable difficulty was encountered before the plate finally was torn apart. An idea of the stresses in the casting may be gained from the fact that the steel bars shown in Fig. 11 were frequently bent with deflections of ¼ in. at the center, especially when the casting did not tear. This makes it easy to understand why the collapsibility of cores is so important in the production of sound castings.

Gas porosity was produced (1) by poor melting practice, (2) by additions to the melt which produce the same dissolved gases as bad melting practice and (3) by additions of materials to the mold cavity which give off the same gases as many molding materials. The gas porosity was shown to increase when a $\frac{1}{16}$ in. thick steel plate was placed on the bottom of the mold cavity. Low permeability of sand evidently increases the danger from gassy metal.

It is very evident that these experiments, while serving the purpose of producing radiographs, also point out many reasons why the effects of gas contamination may be so difficult to correct in the foundry: the melting, molding or the use of mold washes under some conditions may be the responsible factor.

Summary and Conclusions

Defects in bronze castings may be reproduced artificially in controlled amounts provided the foundry practice is carefully and thoroughly controlled.

The defects observed in radiographs of bronze castings are directly related to the foundry practice. This has been shown by correlations of the observed defects with melting and molding practice on more than 100 bronze plates. Only a few of the common, representative defects have been included in this discussion. Included among these common defects are distributed shrinkage, hot tears, sand and slag inclusions and gas porosity. Complete records of melting and molding practice were kept to facilitate correlation of defects with foundry practice.

Characteristic differences have been observed between gas porosity and distributed shrinkage. The gas porosity as exemplified by Fig. 15 tends to be evenly distributed and causes a hazy, indefinite appearance as if the film had been partially fogged before it had been used. This is considerably different from distributed shrinkage which, although spread over the central region of the plate, still has limits beyond which it does not occur to any great extent.

Defects such as gas porosity, shrinkage, blowholes, tears and dross often are difficult to recognize because they may not occur alone or in great quantity.

Acknowledgments

The author would like to acknowledge the assistance of Dr. B. M. Loring and Mr. Herman Kaiser in giving much helpful advice. The radiographic work done by Mr. Stevens is also greatly appreciated.

References

1. Preparation of Radiographic Standards, Naval Research Laboratory, Report No. M-1909 (1942).
2. Army Air Forces Technical Report No. 4796 (1942).

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DISCUSSION

Chairman: H. M. St. JOHN, Crane Co., Chicago.

Co-Chairman: A. K. HIGGINS, Allis-Chalmers Mfg. Co., Milwaukee.

J. W. BOLTON:[1] The author is to be congratulated on presenting a clear description of the radiographic method. We might call attention to the limitations of the method. Some years ago, we were working with radiographic methods. This general dispersed porosity, or incipient shrinkage is microscopic. You can compare two cast plates where the cross-sections are uniform and if your photography and everything is timed just so, you can make comparisons of one radiographic film with another and get a good idea of the relative degree of porosity. You can see, as is shown in the radiographs in the paper, a dark place near the center in your shadowgraph. On the other hand, for that type of porosity, the moment you get into variant sections, you have light film areas due to the sections themselves. That puts a decided limit on the practical application of radiography to the examination of castings.

In the case of gross discontinuities, such as localized shrinkages or good sized cracks or large inclusions, there is a good possibility of using radiographic methods successfully.

As opposed to the radiographic approach for many castings, and especially small castings weighing only a few pounds apiece, the older method of attack by sectioning and deep etching, we believe is much more sensitive. Any porosity or porous area is much more rapidly attacked by acid and you get, in effect, a magnification of the effect; that is, it is made much more apparent.

I believe the author will agree with these statements of limitation, because the method as a whole has very much to commend it.

C. L. FREAR:[2] Commenting on Mr. Bolton's remarks, we cannot expect radiography to show all of the defects in bronze castings. We have to remember in making a radiograph that the best sensitivity obtainable under normal X-ray work is approximately 1½ per cent, which means that with a 1-in. wall thickness, any defect we find would have to be at least 0.015 in. in size. Anything smaller than that, which most of our interdendritic shrinkage is, would not show as a separate defect. Therefore, most of our interdendritic shrinkage will show only as a shadow rather than as separate defects.

In the use of radiography for bronze castings, we would like to use it, but we do not know exactly how to interpret the results, simply because the X-ray does not show all of the defects. I agree with Mr. Bolton that the sectioning often is a better method of inspection for non-ferrous castings. I prefer fracturing rather than cutting to identify the internal defects. We hope some day to tie our radiographic work with the actual physical properties of castings. One of the biggest opportunities for the use of radiography in my opinion is in the matter of impregnation. When we started permitting impregnation of castings, I know of two cases, at least, where as soon as we authorized this impregnation the foundry started making castings to be impregnated. We can impregnate a casting but we do not know from looking at the surface of that casting whether we have a rather massive type of shrinkage or very fine interdendritic shrinkage. I have seen cases of very fine interdendritic shrinkage which was not hurting the casting at all but that would not be permitted to be impregnated because of excessive leakage. On the other hand, I have seen castings that would stand very high pressures but when you broke them open, looked like a piece of rotten wood on the inside. I believe that radiographic examination is one method of finding whether the casting itself has the necessary strength to withstand the loads. A casting which is tested hydrostatically, perhaps will stand the hydrostatic load, but if you put the pressure on it often enough, it may break through. The skin is the only thing holding it.

MR. BOLTON: Those points are certainly well taken and I am entirely in agreement with them. I was merely trying to point out that this has to be supplemented with other things in order to get the whole story.

In the case of test bars, there is a chance for evaluation of strength in test bars, and with castings, with carefully standardized radiography, the density of your film can be tied in with that. We have not done it but have seen the possibilities of it.

MR. BAER: This is only about one-fifth of the actual work that we have done on this particular subject. As Mr. Frear suggested and as he knows, I have sectioned the castings, fractured them, deep etched them and have gone so far as to pressure test all of these castings. In the future I hope to be able to publish more information which will settle many of these questions. We have done all this work in order to get the complete story. The present paper is really just the beginning of a series of publications on interpretation of radiographs of bronze castings.

N. A. KAHN:[3] I would like to ask Mr. Baer if he has made radium exposures of his plates in conjunction with X-ray exposures. Also, whether the changes in density on the film as a result of differences in section thickness of the casting might be confused with changes in density of the film as a result of radiographic defects.

MR. BAER: We did not make radium exposures of the plates.

J. F. EDNIE:[4] I would like to ask Mr. Baer if there was any lead present in the gun metal he was using. Gun metal may contain lead up to one per cent and possibly Mr. Baer could tell us if it interferes with the accuracy of his work in radiography.

MR. BAER: In practically all cases, there was no lead in the gun metal. It was lead-free by wet chemical methods of analysis.

MR. EDNIE: Do you have any idea what the upper limit is?

MR. BAER: There is no lead in gun metal. The Navy specifications show about 0.3 per cent max. lead.

MR. EDNIE: Would you have 0.3 per cent in this alloy?

MR. BAER: We did not have 0.3 per cent of lead.

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[1] Lunkenheimer Corp., Cincinnati.
[2] Bureau of Ships, Washington, D. C.
[3] New York Naval Shipyard, Brooklyn, N. Y.
[4] Duquesne Smelting Corp., Pittsburgh, Pa.

OCCURRENCE OF

INTERGRANULAR FRACTURE IN CAST STEELS

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ABSTRACT

The causes of a peculiar smooth undulating fracture in test bars of some cast steel specimens were investigated. The surfaces observed on the fracture surface were found to be the primary austenite grain boundaries established at the time of casting. The following causes of this peculiar intergranular fracture are advanced as the result of this investigation:

1. Aluminum nitride precipitation at the primary grain boundaries.

2. Ferrite precipitation as a network at the primary grain boundaries.

3. Massive carbides in the primary grain boundaries.

4. Extreme cases of Type II sulphide inclusions.

5. Internal hot tears.

Steels in which a very fine precipitate of aluminum nitride was responsible for intergranular fracture became increasingly susceptible to the injurious effects of the precipitate as (1) the aluminum content was increased, (2) the nitrogen content was increased, and (3) the rate of cooling after casting was decreased. With a cooling rate of roughly 100° F per hr., precipitation was found to occur at approximately 1500° F, and solution of the precipitate was effected by reheating to temperatures of 2100° F and higher.

FROM TIME TO TIME examples of intergranular fractures, characterized by smooth curved areas on the fractured surface, have been observed by manufacturers of cast steel. Sometimes this peculiar fracture has been noted on specimens fractured after having been heat treated. Examination of specimens exhibiting this peculiar fracture has indicated that the smooth curved areas represent primary austenite grain boundaries established as the casting cooled from solidification. For that reason, such fractures have been termed intergranular.

The smooth undulating surfaces of such fractures may vary from very bright and shiny to very dull gray in appearance. The significance of these variations in reflectivity is not understood, but it has been observed that, in general, the appearance of the fracture becomes more dull as the severity of the prior austenitizing treatment is increased.

Specimens broken as cast, generally, have the brightest intergranular fractures; those broken after being quenched from 1600° F and drawn have intergranular fractures somewhat less bright; and those broken after being quenched from a higher temperature, or after having received both a normalizing treatment and a quench have intergranular fractures that are still less bright.

NOTE: This paper is based on work done for the Office of Scientific Research and Development under Contracts No. OEMsr-449, and No. OEMsr-450 with Battelle Memorial Institute.

Intergranular Fracture Defined

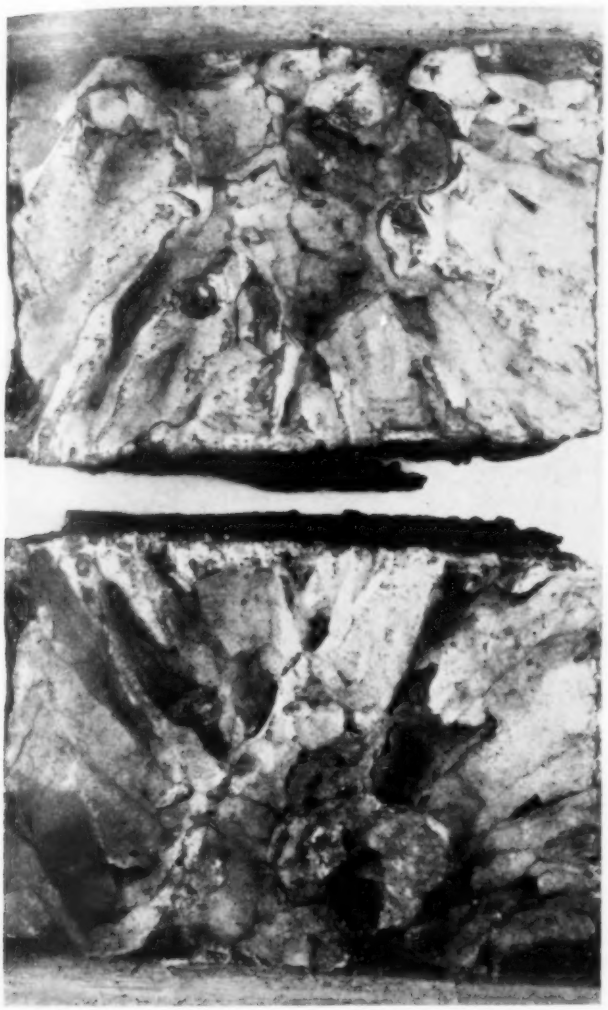
It should be stressed that the term "intergranular fracture" refers to failure along what was once the primary austenite grain boundaries, regardless of whether the specimen was recrystallized before breaking. Failure can likewise occur on secondary austenite grain boundaries when the precipitate causing the fracture is taken into solution by a high-temperature treatment and is then permitted to reprecipitate on cooling. In most cases, specimens are heat treated before breaking and metallographic evidence of the primary grain boundaries thus obliterated; nevertheless, failure occurs not along the small secondary grain boundaries but along the relatively large primary grain boundaries.

Recently Jolivet [1] observed the persistance of high-temperature structural features in the fracture of cast steels, and suggested that the origin of the resultant fragility is related to the deposition of metallographic constituents, such as cementite or some nonmetallic phase of an indefinite nature. He further observed that the brittleness was commonly met in steels containing alloying elements or those treated with aluminum. It disappeared upon recrystallization when associated with structural characteristics, but persisted when caused by nonmetallic inclusions, the solubility and diffusibility of which are low at the temperature of treatment.

Jolivet's Observations

He found that the latter could be eliminated sometimes by diffusion treatments at high temperatures. The brittleness and the characteristics of the fracture were closely similar to those observed in overheating and burning of steel.

It appears to be almost axiomatic that any condition which causes the primary grain boundary areas to possess mechanical properties that are different from the center portion of each grain may result in an intergranular type of fracture. Different examples



of intergranular fracture have been observed where the cause has been, in one case, the presence of a brittle constituent at the primary grain boundaries, and in another case, the presence of a ductile but low-strength material at the primary grain boundaries.

It also appears axiomatic that if an intergranular fracture is caused by the presence of a brittle constituent at the primary grain boundaries, the intergranular character of the fracture may be either partly or wholly removed by introducing some material into the melt which causes embrittlement in the body of the austenite grains. Here, however, though the intergranular appearance of the fracture may be eliminated, there is no actual improvement in the ductility of the material.

The subject of intergranular fracture has previously been reviewed by one of the authors.[2]

Investigations to Reproduce Intergranular Fracture

Examination of Steel Exhibiting Intergranular Fracture. The first steel examined which exhibited this crystalline fracture had been heat treated, and the primary austenite grains had thus been obscured to the usual metallographic procedure; nevertheless, the contour of the fracture suggested that the failure occurred through regions that were originally the primary austenite grain boundaries. The appearance of the fracture of this steel is illustrated in Fig. 1.

The same steel was deep etched in a hot 38 per cent HCl, 12 per cent H_2SO_4 solution. Figure 2 shows a network of hairline cracks that developed on the etched surface. The general appearance of the network also strongly suggests a close association between the hairline cracks and the original austenitic grain boundaries. It was later shown that a relationship exists between the hairline cracks apparent after deep etching and the average amount of intergranular fracture found in the steel. It appears that the fracture must, however, be roughly 50 per cent intergranular before any evidence of hairline cracks becomes apparent on deep-etched specimens.

Careful examination under the microscope of steels showing intergranular fracture reveals the presence of a chain of nonmetallic particles. These are illustrated in Fig. 3. The particles are extremely small in size and become apparent only at a magnification of 1000 diameters or more. The chain of particles was not observed to form so complete a network as the network formed by deep etching, probably because the particles become submicroscopic in size at intervals, but the portions that could be observed seemed to take the same pattern as the hairline cracks developed on deep etching.

The first steel examined contained more acid-soluble aluminum than normally found in steels where aluminum is used simply for the purpose of

Fig. 1 (left)—Intergranular-type fracture exhibited by cast steel specimen. ×3. Fig. 2 (below)—Macroetched specimen from the same cast steel specimen photographed in Fig. 1 showing hair-line cracks at primary austenite grain boundaries.

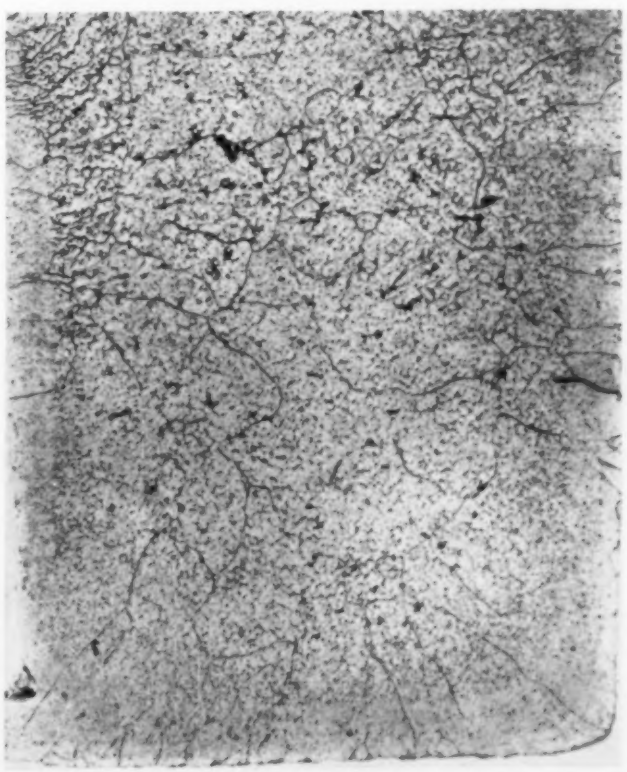




Fig. 3—Three typical examples of the chain of non-metallic particles found in the cast steel specimen. Unetched. ×1500.

deoxidation. The steel was also processed under conditions which lead one to believe that the steel might contain a higher than normal nitrogen content. These two elements were, therefore, suspected of playing a part in the formation of the intergranular fracture. Hydrogen was also suspected, principally because of hairline cracks developed by deep etching.

With the foregoing information serving as a guide, experiments were undertaken to reproduce the intergranular fracture and to analyze its cause.

Preliminary Laboratory Experiments. The first steels made in the laboratory were prepared to explore the effect which variations in nitrogen and aluminum contents had on the formation of intergranular fracture. The steels were made in an induction furnace, and all had the same base composition of 0.30 per cent carbon, 1.60 per cent manganese, 0.50 per cent silicon, 0.50 per cent chromium, 0.35 per cent molybdenum, and 0.03 per cent maximum sulphur and phosphorus. Nitrogen additions were made by using high-nitrogen ferrochromium and calcium cyanamide. Prior to pouring each casting, an addition of aluminum was made to the melt.

Several castings were poured from each heat, and each casting received a different deoxidation treatment. Three or four splits were made for each heat, the metal being cast into two types of double-keel blocks, one having legs measuring approximately 1¼ x 1¼ x 15 in,; the other with legs measuring approximately ¾ x 1 x 9 in. After they were removed from the block, the legs were heat treated by holding for 4 hr. at 1850° F, then air cooling, and reheating to 1600° F for 3 hr. and water quenching. They were finally tempered for 1 hr. at 1100° F. The bars so heat treated were notched, broken, and the fractures rated according to the per cent that appeared inter-

Table 1.

PER CENT OF INTERGRANULAR FRACTURE OBTAINED IN STEELS OF VARYING NITROGEN CONTENT WITH INCREASED AMOUNTS OF ALUMINUM ADDED

Heat No.	9356	9357	9358	9359
Per Cent N (Intended)	0.002	0.010	0.015	0.020
Deoxidation: Al added—lb./ton				
0	0	0	0	0
1	0	0	0	0
2	0	0	0	0
3	0	0	0	12
4	5	Trace	5	80
5	5	20	10	90
6	25	55	15	90
8	60	95	60	90
20	35	50	50	65
28	0	15	40	Trace

Table 2

IZOD VALUE FOR HEAT-TREATED SPECIMENS FROM STEELS OF INCREASING ALUMINUM CONTENT*

Heat No.	9356	9357	9358	9359
Per Cent N (Intended)	0.002	0.008	0.015	0.020
Al added—lb./ton				
0	36.7	48.5	52.0	46.5
1	34.2	41.7	31.7	36.0
2	35.2	41.5	45.7	34.7
3	37.0	40.0	41.7	35.7
4	38.5	47.7	45.7	33.0
5	34.0	40.5	41.5	24.2
6	32.0	35.5	36.7	19.0
8	20.7	10.5	8.7	9.5
20	7.2	7.5	6.2	8.2
28	24.5	20.0	6.2	35.0

* Izod values shown are the average of six tests.

Table 3.

INTERGRANULAR FRACTURE RATINGS FOR HIGH AND LOW NITROGEN AND HYDROGEN-CONTENT STEELS

Heat No.	Casting No.	Al In Lb. Per Ton	N	H	Intergranular Fracture, %: A — As Cast (No Heat Treatment)	B — Not Homogenized	C — Homogenized 24 Hr. 2300° F.-Furnace Cooled*	D — Homogenized 2 Hr. 2400° F.-Furnace Cooled*	E — Homogenized 2 Hr. 2500° F.-Furnace Cooled	F — Homogenized 2 Hr. 2100° F.-Oil Quenched*	G — Homogenized 2 Hr. 2500° F.-Oil Quenched*
9616	2	0	Low	Low	0	0	0				
9616	4	1½	Low	Low	0	0	Trace				
9616	6	3	Low	Low	0	0	20				
9617	2	0	Low	High	0	0	0				
9617	4	1½	Low	High	0	0	0				
9617	6	3	Low	High	0	Trace	60				
9615	2	0	High	Low	0	0	0				
9615	4	1½	High	Low	0	0	5	15	0	0	0
9615	6	3	High	Low	0	5	85	20	15	20	15
9614	2	0	High	High	0	0	0				
9614	4	1½	High	High	0	0	35	8	10	0	0
9614	6	3	High	High	0	10	95	55	90	55	40
9616	1**	0	Low	Low	0	0	0				
9616	3	1½	Low	Low	0	15	Trace				
9616	5	3	Low	Low	0	95	10				
9617	1	0	Low	High	0	0	0				
9617	3	1½	Low	High	0	Trace	Trace				
9617	5	3	Low	High	0	5	35				
9615	1	0	High	Low	0	0	0				
9615	3	1½	High	Low	0	30	15	0	0	0	0
9615	5	3	High	Low	0	100	30	20	Trace	5	Trace
9614	1	0	High	High	0	0	0				
9614	3	1½	High	High	0	100	35	10	15	0	0
9614	5	3	High	High	0	100	50	40	90	30	5

* Final heat treatment of these samples before breaking was 1600° F.—3 hr., water quench; 1100° F.—1 hr., water quench.
** Casting numbered 1, 3, and 5 were slowly cooled from 1500° F., as described in the text. Castings numbered 2, 4, and 6 were cooled in the mold.

granular in nature. The extent of the intergranular fracture for steels of different nitrogen content and for variations in aluminum additions from 0 to 28 lbs. per ton is illustrated in Table 1.

A very definite correlation was noted between the per cent intergranular fracture and the amount of aluminum added. Increasing amounts of aluminum resulted in increasing percentages of the intergranular fracture up to about 8 lb. of aluminum per ton. When 20 lb. of aluminum per ton was added, the amount of the intergranular fracture was found to be less in most cases than was obtained with the 8-lb. addition. Additions of 28 lb. per ton caused a still more pronounced decrease in the per cent intergranular fracture.

A correlation between the per cent intergranular fracture and nitrogen content was also noted, although it was by no means so clear cut as was the correlation with the aluminum addition. When the nitrogen content was high, the intergranular fracture tended to appear in steels having low aluminum content. For a given aluminum content, the susceptibility to intergranular fracture increased with increasing nitrogen content.

Results obtained from mechanical tests parallel very closely the results obtained by the fracture test. It was evident that the same condition which caused the intergranular fracture also was capable of causing a very sharp lowering in elongation, reduction of area, and impact values, though tensile and yield strength values were relatively unaffected.

In some instances, where the intergranular fracture rating approached 100 per cent, the elongation and reduction of area of corresponding tensile tests fell almost to zero. Table 2 records the Izod values for the heat-treated specimens from steels of increasing aluminum content. The final hardness of the steels was adjusted by tempering to give values between 240 and 260 Brinell.

Steel cast in two sizes of keel blocks could not be compared satisfactorily, and this was cause for considerable concern as to the reproducibility of results.

For example, 2 lb. of aluminum per ton resulted in a conspicuous amount of intergranular fracture in steels cast into the heavier blocks, while steels cast in the lighter blocks, in most cases, did not show any intergranular fracture when aluminum additions of less than 4 lb. per ton were used.

Though it was not originally realized, later work established that cooling velocity after casting was a factor fully as important as either aluminum or nitrogen. The greater the cooling velocity, the less is the tendency for the intergranular constituent to form. Consequently, direct comparison of the steels cast in the two sizes of blocks was not possible because of the difference in casting size and the corresponding difference in cooling velocity after casting.

Effect of Hydrogen. One of the things not answered satisfactorily by the first group of heats was the role that hydrogen might play in causing the intergranular fracture. While hydrogen had been added in the form of wet lime to two of the heats, no means of eliminating it from the other heats had been employed, and it might be argued that there was sufficient hydrogen present in all of the heats to exert some influence on the fracture.

Detrimental Effects of Hydrogen

Slow cooling from casting and from hot-rolling temperatures had been resorted to in commercial practice for years as a means for eliminating the detrimental effects of hydrogen in steel. Consequently, a series of heats was planned in which one portion of each heat was to be slowly cooled from the casting temperature. The other portion of each heat was to be cooled in the mold and used for comparison.

Four heats were made: one with as low a nitrogen and hydrogen content as possible, another with low hydrogen and high-nitrogen contents, another with high-hydrogen and low-nitrogen contents, and the fourth with both high-hydrogen and high-nitrogen contents. All four heats had the same analysis used previously, that is, 0.30 per cent carbon, 1.60 per cent manganese, 0.50 per cent silicon, 0.50 per cent chromium, 0.35 per cent molybdenum, and 0.03 per cent maximum sulphur and phosphorus. To obtain high nitrogen, high-nitrogen ferrochromium was used for the chromium addition. Hydrogen was bubbled through the melt for 5 min. before pouring to obtain a high hydrogen content.

Each heat was split into six large double-keel blocks similar to the keel blocks previously described. No aluminum was added to the first two keel blocks poured from each heat, 1½ lb. per ton was added to the third and fourth blocks poured, and 3 lb. per ton was added to the fifth and sixth blocks poured.

Table 4.

RESULTS OF FRACTURE TEST ON BARS FROM HEAT WITH HIGH NITROGEN CONTENT AND DEOXIDIZED WITH 1½ LB. ALUMINUM PER TON

Casting quenched from approximately: *	*Per Cent Intergranular Fracture of Each Break*	*Average Per Cent Intergranular Fracture*
2200° F.	0, 0, 0	0
1700° F.	0, 0, 0	0
1500° F.	55, 45, 60	53
1300° F.	25, 15, 10	17
1100° F.	10, 30, 35	25
900° F.	30, 35, 40	35
Castings cooled to room temperature in the furnace:		
Casting A	10, 5, 5	7
Casting B	25, 25, 25	25

* All specimens were heated to 1600° F.—3 hr., water quenched reheated to 1250° F.—3 hr., and water quenched as a final treatment before fracturing.

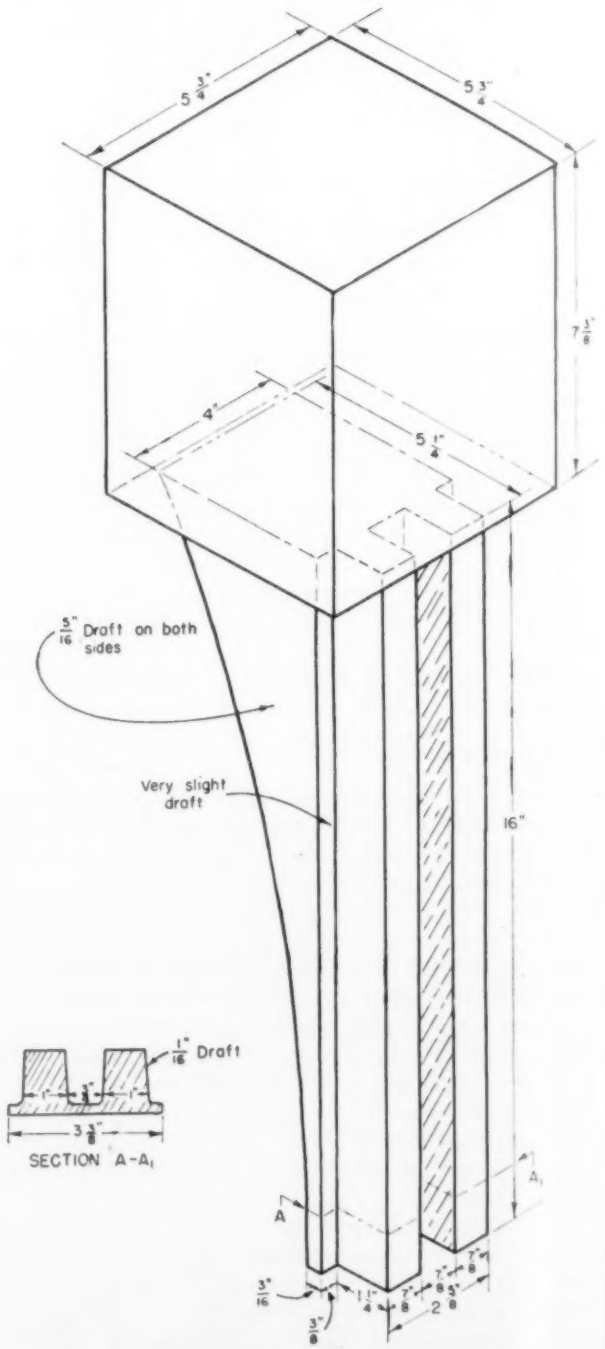


Fig. 4—Isometric drawing of wedge block.

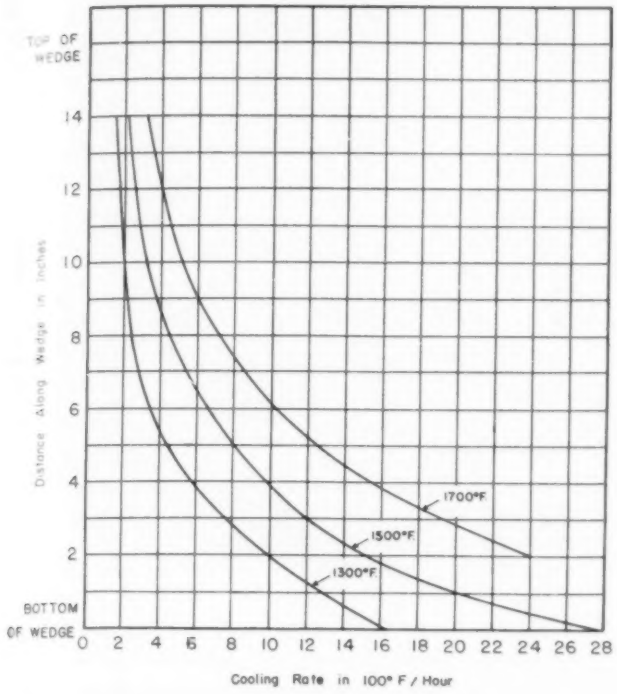


Fig. 5—Cooling rate of wedge block at three temperatures. Average of three tests.

The first, third, and fifth castings from each heat were broken from the mold as soon as solid and transferred to a furnace operating at 1600° F.

All of the castings were several hundred degrees hotter than the furnace when transferred. After sufficient time to permit equalization of the temperature within the furnace, the furnace control was set back to 1200° F and the furnace held for 15 hr. The furnace was then disconnected and the castings cooled in the furnace to approximately 150° F before being removed. Although the cooling rate used for these castings was not so slow as that frequently used commercially for cooling large sections, it should have been sufficiently slow to assure relief from hydrogen stress in view of the rather small sections.

Intergranular Fracture Ratings

The intergranular fracture ratings of these heats after various heat treatments are shown in Table 3.

There was no evidence that stress, resulting from hydrogen, was influencing the fracture. On the contrary, the castings that were slowly cooled to eliminate the effect of hydrogen consistently exhibited more of the intergranular fracture than did the more rapidly cooled castings. This suggested that some intergranular precipitate occurred at 1500° F or less.

Effect of High-Temperature Homogenizing Treatment. In the as-cast condition, none of the specimens had an intergranular type of fracture. Metallographic examination revealed that the entire series of castings contained quite a quantity of free ferrite in the as-cast condition and that, when the as-cast specimens were fractured, fracture occurred through the ferrite rather than through any intergranular constituent. Thus, it became apparent that breaking specimens in the as-cast condition does not provide a reliable index of the amount of intergranular constituent present. The fracture test becomes a useful tool in detecting the presence of the intergranular constituent only if the matrix is first put in the toughest possible state; that is, fully quenched and tempered.

The slowly cooled castings were somewhat improved by a 2300° F, 24-hr. homogenizing treatment, while the castings cooled in the mold were made worse by the same homogenizing treatment. The cooling rate after homogenizing at 2300° F is not accurately known, but was intermediate between the cooling rate of the two sets after casting. Furthermore, after homogenizing at 2300° F some bars appeared to fracture at grain boundaries established during the homogenizing treatment. This was in contrast to the relatively coarse primary grain-boundary fracture noted previously. These two observations suggested that partial solution of the intergranular constituent was occurring at 2300° F and that partial reprecipitation occurred as the steel was slowly cooled from 2300° F.

Heat Treatment of Samples

It was reasoned that, if partial solution occurred at 2300° F, more complete solution would occur at higher temperature. Consequently, two identical sets of samples were heated for 2 hrs. at 2400° F. One set was oil quenched and the other set was cooled in the furnace. Two other sets were treated similarly except that 2500° F was used rather than 2400° F. The results were in agreement with the hypothesis suggested by the 2300° F homogenize.

It should be noted from Table 3 that all of the samples containing 1½ lb. of aluminum per ton were completely free from intergranular fracture when quenched from either 2400 or 2500° F. The samples containing 3 lb. of aluminum per ton still exhibited a small amount of the intergranular fracture, but the amount had decreased decidedly.

Homogenizing at 2300° F was carried out in a different furnace from that used for homogenizing at 2400 and 2500° F, and the cooling rate after the

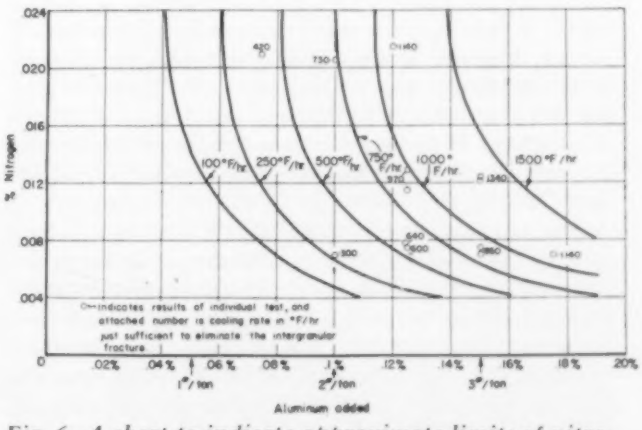


Fig. 6—A chart to indicate approximate limits of nitrogen and aluminum that may be tolerated in a base analysis containing 0.30 per cent C, 1.60 per cent Mn, 0.50 per cent Si, 0.50 per cent Cr, and 0.35 per cent Mo without development of the intergranular type fracture.

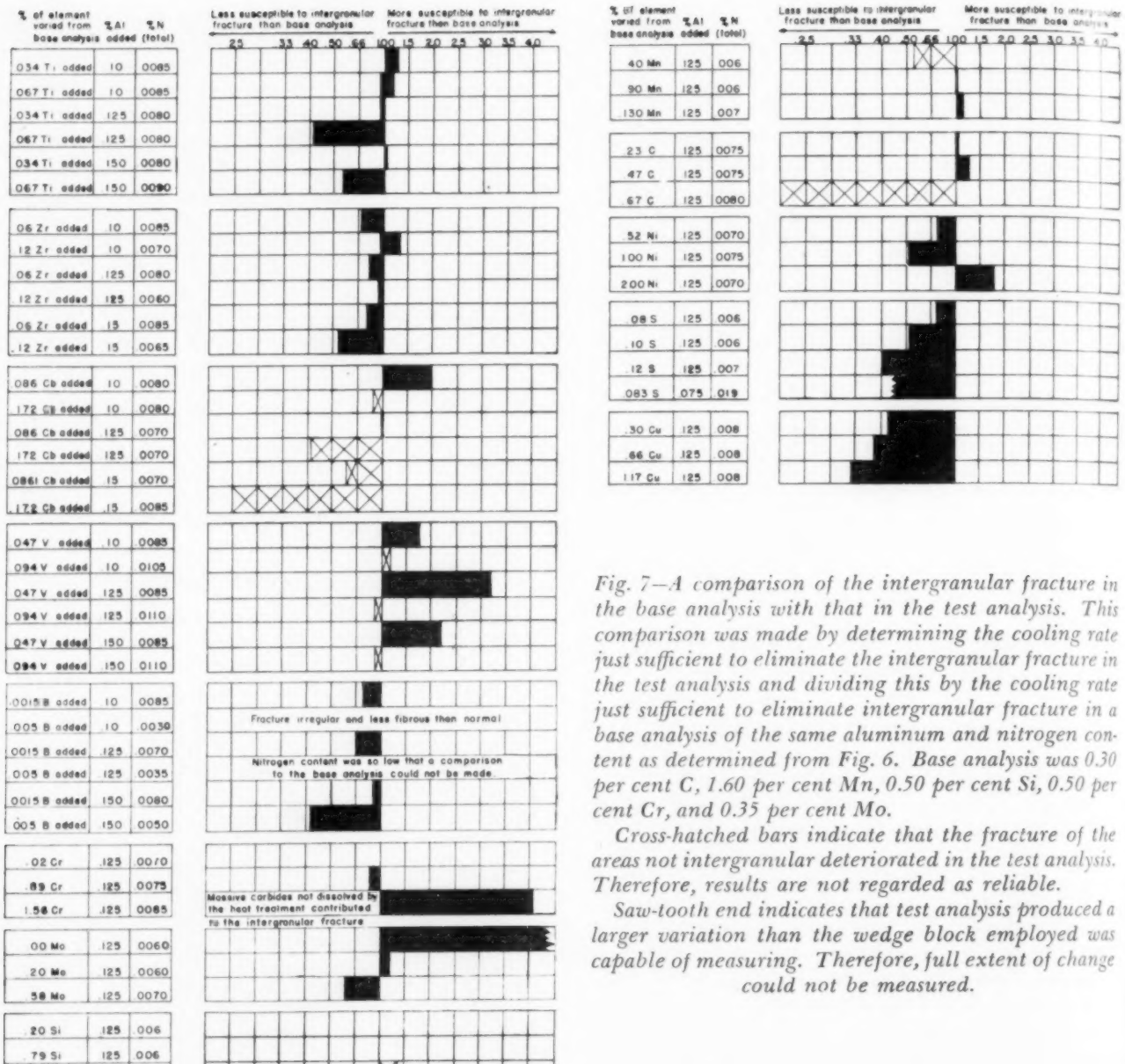


Fig. 7—A comparison of the intergranular fracture in the base analysis with that in the test analysis. This comparison was made by determining the cooling rate just sufficient to eliminate the intergranular fracture in the test analysis and dividing this by the cooling rate just sufficient to eliminate intergranular fracture in a base analysis of the same aluminum and nitrogen content as determined from Fig. 6. Base analysis was 0.30 per cent C, 1.60 per cent Mn, 0.50 per cent Si, 0.50 per cent Cr, and 0.35 per cent Mo.

Cross-hatched bars indicate that the fracture of the areas not intergranular deteriorated in the test analysis. Therefore, results are not regarded as reliable.

Saw-tooth end indicates that test analysis produced a larger variation than the wedge block employed was capable of measuring. Therefore, full extent of change could not be measured.

2300° F homogenize was consequently considerably slower than the cooling rate after either the 2400 or 2500° F homogenize. This apparently explains why there was a tendency for the specimens furnace cooled from 2300° F to contain more of the intergranular fracture than the specimens furnace cooled from 2400 and 2500° F.

Temperature of Precipitation. None of the foregoing tests had been designed to give information concerning the temperature at which the precipitation occurred. True, the castings slowly cooled from 1500° F suggested that precipitation had, in those particular cases, occurred at 1500° F or less, but this did not preclude the possibility of precipitation occurring at a higher temperature if the opportunity were provided.

To examine this point further, a steel which had been treated with 3 lb. of aluminum per ton was forged to ¾-in. square. This steel, before forging, but after being fully heat treated, exhibited a fracture that was completely intercrystalline. After forging, the intercrystalline network was sufficiently broken up so that the fracture was completely fibrous. Seven 6-in.-long pieces of the ¾-in. square were heated to 2500° F for 6 hr. At the end of this period, one piece was quenched in oil and the furnace set back to 2300° F. Two hours later, another piece was quenched in oil and the furnace set back to 2100° F.

This continued until a piece was quenched in oil at each 200-degree interval down to 1500° F. The furnace was then turned off, and the last sample cooled to room temperature with the furnace. Thus a series of samples was obtained which would permit following the course of the precipitation. The pieces were all reheated to 1600° F for 3 hr., water quenched and drawn at 1100° F for 1 hr., then notched, and fractured.

The only specimen exhibiting any of the inter-

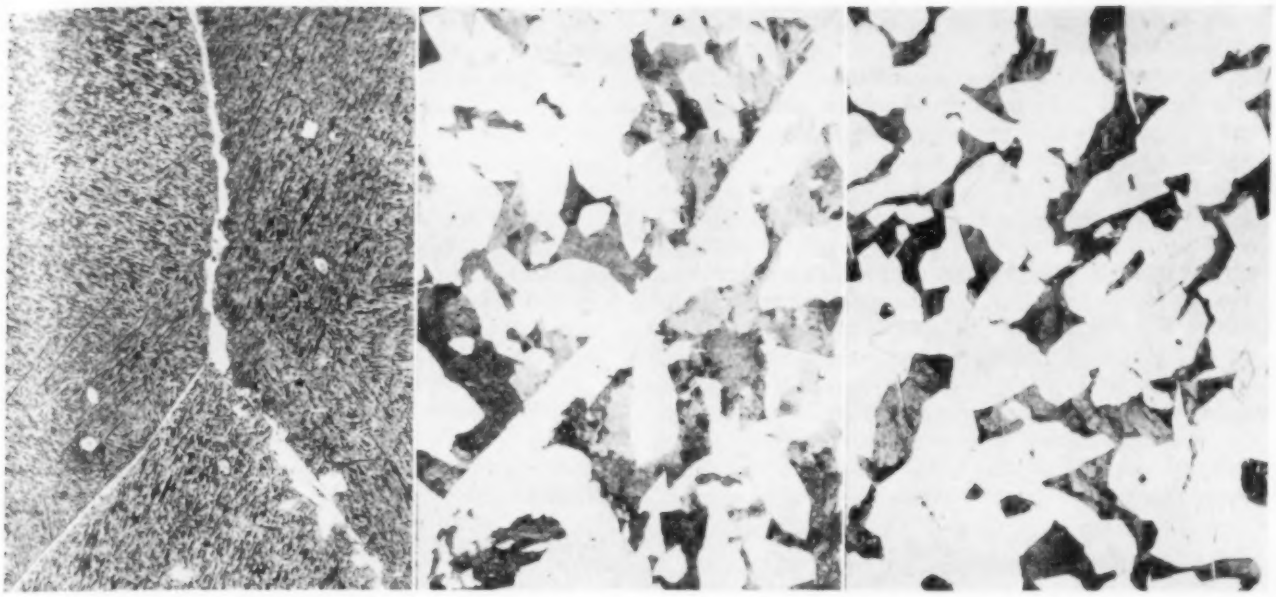


Fig. 8 (left)—Picral etch. Ferrite network found in steel that broke intergranularly in the as-cast condition. Body of each grain is low-carbon martensite. ×100. Fig. 9 (center)—Picral etch. Ferrite network in another steel that broke intergranularly in the as-cast condition. Body of each grain is a mixture of ferrite and pearlite, but the pearlite forms an almost continuous phase. ×100. Fig. 10 (right)—Picral etch. Structure found in a steel whose fracture was not intergranular when broken in the as-cast condition. This time, ferrite rather than pearlite formed a continuous phase throughout each grain.

granular fracture was one cooled to room temperature in the furnace, and this piece had a fracture which was 95 per cent intercrystalline and only 5 per cent fibrous. All other pieces had fractures which were completely fibrous. Thus it became apparent that precipitation occurred below 1500° F, at least when the cooling rate employed was not less than approximately 100 degrees per hr.

When the first heats were made, the influence of cooling rate from solidification on the formation of intergranular precipitate was not fully appreciated, and consequently, the cooling rate was not carefully controlled. A group of steels was, therefore, made in which care was taken to control the cooling rate after casting. These steels were designed to show (1) the temperature at which precipitation occurred after casting, (2) the limit of susceptibility so far as aluminum and nitrogen contents were concerned, and (3) the temperature and time required to effect solution of the precipitate.

Precipitation of Aluminum Nitride

The heat intended to show the temperature at which aluminum nitride precipitated on cooling from solidification was made with an intended nitrogen content of 0.015 per cent, and the entire heat deoxidized with 1½ lb. of aluminum per ton. Eight single-keel blocks were cast and all of the castings shaken from the mold as soon as solid. One of the castings was quenched immediately in water, and the other seven placed in an electric pit-type furnace already heated to 1700° F.

After 2 hr., one of the seven castings in the electric furnace was quenched in water and the furnace controller set back to 1500° F. Two hours later, another casting was quenched and the furnace controller turned back to 1300° F. The procedure was repeated until castings had been quenched down to 900° F. The furnace was then turned off and the two castings still remaining in the furnace cooled to room temperature with the furnace.

The legs removed from the castings were then heated to 1600° F for 3 hr, water quenched, reheated to 1250° F for 3 hr., water quenched, notched in three places, and broken as fracture tests. The results are shown in Table 4. It was evident from the table that precipitation of the aluminum nitride occurred between 1700 and 1500° F. So far as the results of this particular series of tests were concerned, there was no indication that precipitation continued at temperatures below 1500° F.

Intergranular Fracture Test Limitations

It is apparent from the individual ratings tabulated in Table 4 that a considerable variation in per cent of intergranular fracture may exist between different castings of the same heat although the castings are treated identically. Furthermore, it will be noted that the per cent of intergranular fracture was greatest in a casting quenched from 1500° F. Theoretical considerations do not appear to afford an explanation of this. It must be concluded, therefore, that the fracture test is not entirely satisfactory for use as a quantitative test of the amount of intergranular precipitate present, though it is certainly a good qualitative test and the best quantitative test available at the present.

Further Studies on Susceptibility to Intergranular Fracture. As regards the limit of susceptibility so far as the aluminum and nitrogen contents were concerned, a very definite correlation between the inter-

granular fracture and both the aluminum and nitrogen contents of the steel was again found. At low nitrogen contents, the fracture did not become intergranular until 2 lb. of aluminum per ton had been added, but with intermediate nitrogen, less than 1½ lb. of aluminum per ton could be tolerated, and with high nitrogen, less than 1 lb. of aluminum per ton could be tolerated.

The castings were cooled at 30° F per hr. through the range in which precipitation occurred. Previous tests indicated that more rapid cooling rates shift the points at which intergranular fracture appears to higher aluminum and nitrogen contents. Since a cooling rate of 30° F per hr. is slower than any rate likely to be encountered after pouring very large castings, the heats examined in this case indicated the practical limits of susceptibility so far as aluminum and nitrogen are concerned for steel of the base analysis used.

Solution of Intergranular Precipitate

In a check of the temperature and time required to effect solution of the precipitate, it was again shown that complete solution of the intergranular precipitate was effected by heating for 2 hr. at 2300° F and that partial solution was effected by heating for 6 hr. at 2100° F. Two hours at 2100° F and either 2 or 6 at 1900° F did not cause the per cent of intergranular fracture to decrease, but this cannot be interpreted as meaning that no solution took place. Additional tests are necessary before it can be determined whether any appreciable solution occurs below 2100° F.

Aluminum nitride apparently behaves as a very sluggish constituent. There appears to be a spread of several hundred degrees between the temperature of precipitation and the temperature of solution, even when moderately long times are permitted for precipitation or solution. It is significant that precipitation takes place preferentially at the grain boundaries where the steel lattice is least stable and consequently most easily disrupted to accommodate a foreign nucleus, and this is in spite of the fact that both aluminum and nitrogen must diffuse greater distances in a coarse-grained as-cast structure if precipitation is to occur principally in the grain boundaries.

Identification of the Intergranular Phase. While the correlation between the intergranular fracture and aluminum content on one hand and between the intergranular fracture and nitrogen content on the other suggested that the precipitate was aluminum nitride, positive identification was complicated by the extreme fineness of the precipitate. In fact, it was found that many times the intergranular chain of particles was not visible under the microscope even at high magnification unless the fracture was practically 100 per cent intergranular. At other times, samples exhibiting 40 or 50 per cent intergranular fracture contained a coarse network visible at high magnification.

In its crystalline characteristics, the precipitate was idiomorphic. Many of the particles appeared as needles. These were probably platelets which were sectioned by the metallographic surface. Other particles had axes that are more nearly equal and may be either needles or polyhedrons. The particles may be characterized, in general, as parallelograms when viewed under the microscope, through the ratio of width to length varies through a wide range.

Crystal Structure of Precipitate

The small crystalline particles were observed to be strongly anisotropic when viewed with reflected polarized light and, therefore, the crystalline structure must be other than cubic. This seems to agree with observations made on inclusions in heat-resisting alloys which have been identified as aluminum nitride. They were also noted to be somewhat translucent in reflected white light, and frequently the microscope could be made to focus at two locations, either at the metallographic surface or at the inclusion-metal interface.

An attempt to identify the inclusions by use of a series of etching reagents, each of which attacks some particular type of inclusion was not too successful. The fine intergranular precipitate dealt with did not seem

Fig. 11 (left)—Picral etch. As cast. Note fine chain-like precipitate. ×500. Fig. 12 (right)—Picral etch. Same sample as in Fig. 11 but in quenched and drawn condition. Again note fine chain-like precipitate. ×500.





to fit properly into any of the published schemes for chemical identification.

In order to produce a greater abundance of crystals, a small experimental heat of the same base analysis formerly used was made to which 1 per cent of aluminum was added. Calcium cyanamide was stirred into the heat periodically for several minutes, and the heat was then cast into small ingots. One ingot was immediately placed in the furnace operating at 2700° F and cooled with the furnace. Another ingot was cooled in air, and electrolytic separation was made of the residues in the steel.

Diffraction Pattern Study Made

An X-ray diffraction pattern made of this residue showed strong lines which fitted precisely with calculations of published parameter data for aluminum nitride. A number of other lines which did not fit the aluminum nitride pattern were also present, but these were relatively faint and diffused. The greater part of the residue probably consisted of complex carbides, but apparently these carbides were so small in size that they resulted in very diffused refraction.

A similar electrolytic separation was made of a specimen to which 28 lb. per ton of aluminum had been added and which had an intended nitrogen content of 0.020 per cent. Chemical analysis of this residue gave 16 per cent nitrogen and 31.4 per cent aluminum, a ratio that fits almost precisely with the formula for aluminum nitride.

As a further check on the identity of the intergranular precipitate, an attempt was made to produce the same phase in steel at subfreezing temperatures by affording several compounds to diffuse into the steel. Three different steels were used; one contained no aluminum, one had had 3 lb. of aluminum added per ton to the melt, and the third had had 20 lb. of aluminum added per ton to the melt. All three steels contained about 0.010 per cent nitrogen. Two samples of each steel were drilled and tapped in three places. The three holes produced in each sample were then filled separately with sodium cyanide, aluminum nitride and ferric oxide, and were then sealed.

The sodium cyanide was expected to furnish nitrogen and carbon for diffusion into steel, the aluminum nitride was to furnish both aluminum and nitrogen, and the ferric oxide was to furnish oxygen. One set of specimens was treated for 24 hr. at 2300° F and furnace cooled, while the other set was heated for 2 hr. at 2500° F and furnace cooled. After this, the specimens were sectioned and polished so that the periphery of each hole could be examined metallographically.

Non-Metallic Inclusions Studied

Metallographic examination of the diffused specimens showed that the aluminum nitride produced particles in all three steels of the same crystal habit and optical properties as the intergranular precipitate. Sodium cyanide produced no nonmetallic inclusions in the steel containing no aluminum, a few particles similar to the intergranular precipitate in the steel containing 0.15 per cent aluminum, and an abundance of particles similar to the intergranular precipitate in the steel containing 1 per cent aluminum. The particles formed in the 1 per cent aluminum steel took a network form of distribution.

The penetration of the particles formed in the 1 per cent aluminum steel with sodium cyanide was much greater than the penetration of the particles formed in the 0 per cent aluminum steel with aluminum nitride. This is reasonable if one considers that in the former case only nitrogen need diffuse, while in the latter case the penetration is controlled by the duel diffusion of aluminum and nitrogen. Aluminum, with its greater atomic radius than nitrogen, would be expected to diffuse more slowly than nitrogen and thus retard the rate of penetration.

The iron oxide produced an abundance of globular silicates and complex oxide particles in all three steels. There was also a distinct fringe of acicular particles resembling the intergranular precipitate found around the edge of the oxide-silicate zone in the 1 per cent aluminum steel. A similar but less distinct fringe was formed at the edge of the oxide-silicate zone in the 0.15 per cent aluminum steel. No acicular particles were evident in the steel containing no aluminum. This may be rationalized by assuming that oxygen lowers the solubility of nitrogen in steel. Thus, as the oxygen diffuses into the steel, the nitrogen would tend to concentrate in bands ahead of the oxygen and become sufficiently concentrated to form a considerable quantity of aluminum nitride in those steels containing aluminum.

Summary of Phase Identification

In summarizing the work on the identification of the intergranular phase, it was noted that the crystal habits, optical properties, and etching characteristics of the phase were different than the characteristics of the common inclusions found in steel. Inclusions exhibiting the same characteristics, but larger in size, were produced in experimental melts. These large inclusions were separated from the steel and found by X-ray diffraction to be aluminum nitride crystals.

Diffusion tests demonstrated that crystals similar in nature to the intergranular precipitate could be formed by use of aluminum nitride in steel containing no aluminum or by use of sodium cyanide in steel already containing aluminum. Although the procedures used were somewhat indirect, it appears that the evidence indicates rather conclusively that the intergranular precipitate is aluminum nitride.

Effects of Al, N, Cooling Rate and Steel Composition

To obtain reproducible cooling rates in different castings and thus make possible a study of the effect of aluminum, nitrogen, and cooling rate, and at the same time permit a comparison of different compositions, a wedge block was developed which gave a wide variation in cooling rates between different sections. A drawing of the wedge block is shown in Fig. 4. All wedges were cast vertically in a core sand mold. After pouring, a box 14 x 14 x 15 in. was placed around the square sink head and filled with Sil-O-cel to retard the cooling rate of the sink head. The two vertical bars attached to the side of the wedge, shown in Fig. 4, were sawed from the block and used as test bars.

The cooling rate obtained at different positions along the sides of the bars was determined by the use of thermocouples, and Fig. 5 indicates the average cooling

rate that was obtained at different positions on the bars. Variations in cooling rates between similar positions on bars of three different wedges were not more than plus or minus 10 per cent of the average shown in Fig. 5.

All of the heats of the same base analysis as used previously were made in a 300-lb. induction furnace, using the same scrap addition and melting practice. Wedges were not removed from the molds for at least 12 hr. after casting. After the bars had been sawed from the wedge block, they were heated to 1600° F for two hr., quenched in water, drawn for one hr. at 1200° F, and water quenched. The bars were then notched every 1½ in. along their length and broken at each notch.

To make apparent any differences that might exist because of differences in the breaking procedure, some of the bars were broken statically in a tensile machine and some of the bars were broken dynamically by use of a drop-hammer apparatus. Each fracture was then rated individually according to the percentage of intergranular fracture that was apparent.

Importance of Three Variables

Effect of Aluminum, Nitrogen, and Cooling Rate. The data summarized in Fig. 6 illustrate the importance of the three variables, aluminum content, nitrogen content, and cooling rate. The percentage of intergranular fracture decreased as the fast-cooled end of the test bars was approached. The variation in the percentage of intergranular fracture obtained on different parts of the same wedge block broken in the same manner was sometimes large when the percentage of intergranular fracture present was large, but appeared to become insignificant as the percentage of intergranular fracture became small.

The several comparisons afforded by two bars from one wedge, one of which was broken statically and one of which was broken dynamically, indicate that, on the whole, dynamic testing results in a slightly greater percentage of intergranular fracture than does static testing, and that, when statically tested, the bars become free from intergranular fracture at slightly slower cooling rates, other things being equal.

Wedge-block cooling rates at 1500° F have been used throughout in the preparation of Fig. 6 because 1500° F is known to be about the middle of the range at which the precipitation may occur. It is, of course, recognized that precipitation occurs at somewhat higher temperatures with slower cooling rates than it does with more rapid cooling rate. However, an attempt to take into consideration the change in temperature of precipitation with change in the cooling rate greatly complicates plotting of the data and probably is not justified because of the rather narrow range in which precipitation appears to occur, namely 1700 to 1300° F.

Aluminum Nitride Precipitation

Figure 6 is intended as a rough guide of the range where trouble from aluminum nitride precipitation may be expected. Areas to the left and below any particular curve are expected to be free from intergranular fracture, while those areas to the right and above any particular curve are expected to contain the intergranular fracture. Thus, if the approximate cooling rate of the casting at 1500° F is known, dangerous compositions, so far as aluminum and nitrogen content are concerned, may be obtained by reference to Fig. 6.

For example, if the cooling rate of the casting is known to be approximately 500° F per hr. at 1500° F, a nitrogen content of as high as 0.011 per cent may be associated with aluminum additions as great as 2 lb. per ton without anticipating trouble from aluminum nitride precipitation. But this same casting would be expected to show considerable intergranular fracture if the aluminum additions were raised to 3 lb. per ton, keeping the nitrogen content the same.

An Anomaly Explained

A certain degree of caution is necessary in the use of Fig. 6. Since these curves were first drawn, several examples of comercial steel have been observed which did not contain any intergranular fracture, though the aluminum added, the nitrogen content, and the cooling rate after casting were such that they were well within the "intergranular area" of Fig. 6. The most probable explanation of this apparent anomaly seems to involve a difference in the degree of oxidation of the steel prior to the aluminum addition.

The heats used in making Fig. 6 were small induction furnace heats, probably of low oxygen content. The observed steels which did not behave as would be predicted by Fig. 6 were from basic-electric single-slag heats which probably had a much higher oxygen content at the time the aluminum was added, and consequently finished with a lower metallic aluminum content. It would perhaps be more logical to plot metallic aluminum vs. nitrogen content in Fig. 6. However, reliable metallic aluminum analyses of the steels from which Fig. 6 was made are not available.

It must be emphasized that consistent results cannot be expected from fracture tests unless the specimens are fully hardened and drawn before breaking. Past work has shown that, when normalized, annealed, or as-cast specimens containing mixed microstructures are broken, the fracture may follow ferrite or bainite areas rather than the intergranular precipitate.

To illustrate the effect of bainite, several specimens were made from steels exhibiting considerable intergranular fracture when tested in the fully hardened and drawn condition. These specimens were quenched in lead at 850° F to form a structure that was 100 per cent bainite. The specimens were then drawn for 1 hr. at 1200° F, notched, and broken. None of the bainite specimens exhibited any intergranular fracture, although comparative, fully hardened and drawn specimens exhibited 20 to 90 per cent intergranular fracture.

Effect of Heat Treatment

Thus, on the whole, normalized, annealed, or as-cast structures show less intergranular fracture than do other specimens containing the same amount of intergranular precipitate but fractured in the quenched and drawn condition. Moreover, the amount of intergranular fracture obtained on specimens of mixed microstructure appears to be much less reproducible than that obtained on quenched and drawn specimens. Therefore, information obtained from the fracture of specimens which have not first been fully hardened and drawn is of questionable value.

Effect of Other Elements. Though aluminum, nitrogen, and cooling rate appear to be the primary factors

which control the precipitation of aluminum nitride as an intergranular phase, it was thought that other elements might indirectly effect such precipitation. Therefore, a series of wedge blocks were made to which titanium, zirconium, columbium, vanadium, and boron were added one at a time. Any of these elements may, under some conditions, act as a deoxidizer, and, consequently, they were added to the melt after the aluminum addition was made.

Another series was made in which the carbon, manganese, silicon, molybdenum, chromium, nickel, sulphur, and copper contents were varied individually from the base analysis. Except for the individual variations in each element given above, each of these wedge blocks was made and handled in the same manner as the wedge blocks used to evaluate the effects of aluminum, nitrogen, and cooling rate.

Comparison of Cooling Rates

A comparison of the cooling rate just sufficient to eliminate the intergranular fracture in the particular analysis being tested, with the cooling rate just sufficient to eliminate the intergranular fracture in the base analysis, made apparent the effect of the element being tested. The results are indicated in Fig. 7. Both bars from each wedge block were broken dynamically. In most instances, identical results were obtained on both bars of a wedge block; but in a few instances, where small differences were found, the results of the two bars from one wedge block were averaged (Fig. 7).

Titanium and zirconium are both powerful nitride-forming elements. Either of these elements should be capable of forming its respective nitrides before solidification of the steel, judging from thermodynamic data and, also, from the idiomorphic character of either zirconium or titanium nitride particles commonly observed in steel. These two elements, then, might be expected to decrease the susceptibility of a steel to intergranular fracture by decreasing the amount of nitrogen available to combine with the aluminum.

However, it is evident from Fig. 7 that the effect of either titanium or zirconium in this respect is not large. In fact, if titanium or zirconium is considered as a simple addition to the melt, the results as plotted in Fig. 7 do not differ sufficiently from the base analysis tests to be considered significant, except for three of the twelve wedge tests. An advantage from either titanium or zirconium does become apparent if, instead of considering them as added elements in the melt, they are considered as replacing a chemically equivalent amount of aluminum; i.e., a steel to which 0.10 per cent aluminum and 0.067 per cent titanium had been added would be compared with a base analysis to which 0.15 per cent aluminum had been added, since 0.067 per cent titanium reacts with the same amount of oxygen as 0.05 per cent aluminum.

Effect of Titanium and Zirconium

To summarize the observed effect of titanium or zirconium on the intergranular fracture, it would appear that neither causes a decided improvement by fixing the nitrogen as had been anticipated, but that either may result in a considerable improvement if considered from the standpoint of replacing a chemically equivalent amount of aluminum.

Columbium presents an example of how embrittlement of the entire structure may cause a decrease in the amount of intergranular fracture observed without causing any *real* improvement. Additions of columbium caused the precipitation of a pale yellow phase principally at the interdendritic area. It was not identified, but is assumed to be some columbium compound.

Additions of 0.047 per cent vanadium caused a conspicuous increase in the amount of intergranular fracture found in the wedge bars as indicated in Fig. 7. Additions of 0.094 per cent vanadium did not cause a further increase in the intergranular fracture. Instead, the amount of intergranular fracture observed was about the same as would be expected from the base analysis, but, at the same time, areas of the fractures not intergranular appeared less tough and fibrous than normal. Vanadium carbides, precipitated after casting and undissolved by the heat treatment employed, may explain the increased brittleness in the 0.094 per cent vanadium wedge blocks.

Effect of Boron

When 0.005 per cent boron was added to the base analysis, a steel of unusually low nitrogen content was obtained. The nitrogen content was sufficiently lower than for any other heat in the series that it can hardly be regarded as a matter of chance but is instead evidence that boron will remove nitrogen from steel. If the 0.005 per cent boron steels are compared with a base analysis of the same nitrogen content, as is done in Fig. 7, no large improvement is evident. But, if the 0.005 per cent boron steels are compared with a base analysis whose nitrogen content is "normal" for the practice used (and this is justified if boron additions are capable of removing nitrogen from the steel), then the improvement resulting from the boron addition appears very large indeed.

Elimination of chromium from the base analysis and also an increase in the chromium content to 0.89 per cent caused no significant change in the location of the first trace of intergranular fracture on the test bar. However, a wedge block made with 1.56 per cent chromium required a much more rapid cooling rate to eliminate intergranular fracture than did the base analysis. At the same time, the intergranular areas became brighter in appearance. Microscopic examination indicated the presence of massive carbides not dissolved by the heat treatment. This, then, seemed to show that massive carbides properly positioned in the matrix may add to the aluminum nitride effect.

Effect of Molybdenum

Variation in the molybdenum content produced a surprisingly large effect. Elimination of molybdenum caused a tremendous increase in the cooling rate that could be tolerated, and additions of 0.20 and 0.58 per cent molybdenum decreased the tolerable cooling rate in a consistent manner. No explanation of the way in which molybdenum affects the intergranular fractures was apparent by metallographic examination. As a matter of speculation, it may be that the massive molybdenum atoms, when present in the austenite lattice, effectively impede the diffusion of aluminum and/or nitrogen and, thus, greatly retard the precipitation of aluminum nitride.

Wedge blocks with silicon contents of 0.20 per cent and 0.79 per cent were no different than the base analysis; but, it was noted that 1.15 per cent silicon resulted in a steel slightly more susceptible to intergranular fracture, and, at the same time, caused an apparent decrease in the toughness of the fracture that was not intergranular in nature.

In the manganese series, only the low manganese (0.40 per cent Mn) was significantly different from the base analysis. This wedge block, though apparently less susceptible to intergranular fracture, exhibited fractures that were less tough in the areas not intergranular than did the base analysis. Therefore, the results can hardly be interpreted as indicating a real improvement.

Effect of Carbon

Variations in carbon had no significant effect until the carbon was raised to 0.67 per cent. Then an apparent improvement was noted, but again, this apparent improvement was probably not real because it occurred simultaneously with a decrease in the toughness of the fracture areas that were not intergranular.

Nickel failed to show a consistent trend. Additions of 0.5 and 1.0 per cent resulted in fracture bars that were less susceptible to intergranular fracture, but the 2.0 per cent nickel wedge blocks appeared to be more susceptible.

Sulphur contents of 0.08, 0.10, and 0.12 per cent all produced a decided decrease in the susceptibility of the steel to intergranular fracture without any apparent decrease in the toughness of the fracture not intergranular. Very likely, this improvement resulted from the formation of aluminum sulphide and the consequent decrease in the amount of dissolved aluminum available to react with the nitrogen.

Another series of wedge blocks, results for which could not be shown in Fig. 7, were made with aluminum additions of 0.025 per cent to 0.075 per cent and sulphur contents of 0.03 per cent to 0.09 per cent. About 50 per cent of the sulphur was present in some of the wedges as Type II network sulphides, but not a trace of intergranular fracture was evident at any location in the fracture bars. Neither did the fractures appear more brittle than the base analysis.

These results suggest that aluminum nitride precipitation and Type II sulphide inclusions probably do not occur in the same steel. When the residual aluminum content is sufficiently low that Type II sulphides are formed, it is probable that it is also sufficiently low that aluminum nitride will not precipitate. Furthermore, the Type II sulphides formed in these steels were not sufficient alone to cause intergranular fractures.

Copper caused a conspicuous decrease in the susceptibility to intergranular fracture, for which no explanation can be offered at the present time.

Ferrite Precipitation in As-Cast Structures

A number of test bars were received from one manufacturer of cast steel which showed large percentages of intergranular fractures. The test bars had been cooled at various rates after casting and then fractured without further treatment. The test bars showing the intergranular fracture invariably contained a ferrite network about each primary austenite grain.

As illustrated in Fig. 8 and 9, the body of each primary austenite grain was sometime martensite and sometimes mixtures of pearlite and ferrite in which the pearlite formed the continuous phase. Whether the center of each austenite grain was predominately martensite or mixtures of pearlite and ferrite depended on both the chemical analysis of the test bar and the cooling rate after casting. However, either of these types of structures resulted in intergranular types of fracture.

If the cooling rate became sufficiently slow to cause ferrite to become the continuous phase in the center of the primary austenite grains, as shown in Fig. 10, the fracture still occurred through the ferrite but was no longer intergranular in appearance. After heat treatment, the bars no longer contained coarse ferrite network, and consequently, the fractures became fibrous and tough rather than intergranular.

The presence of a ferrite network in the as-cast structure can hardly be considered as indicative of poor quality in the casting. Its formation is dependent on a chance combination of hardenability and cooling rate after solidification. The fact that such networks have been found to cause an intergranular fracture serves as another illustration of the necessity of heat-treating fracture specimens before testing.

Internal Hot Tears

The cause of intergranular fracture in two specimens examined was thought to be internal hot tears, formed as the casting cooled from solidification, that were not exposed to the atmosphere at any time prior to fracturing. The evidence is entirely circumstantial. Hot tears which had opened to the surface of the casting and were consequently oxidized were noted on both the above-mentioned castings.

No heat treatment (even a 15-hr. soak at 2300° F followed by rapid cooling—which is capable of taking aluminum nitride into solution) affected the amount of intergranular fracture that appeared after breaking. Metallographic examination failed to reveal any kind of precipitate that might be the cause of the intergranular fracture, but, at the same time, also failed to reveal the presence of any discontinuities which might be connected with the fracture.

Thus, if internal hot tears were the cause of the intergranular fracture, either the metallographic section chosen was not such that it intersected any of the fissures, or the fissures were so tightly pressed together that they could not be seen with the microscope—as hydrogen ruptures frequently are, for example.

Type II Sulphide Inclusions

Type II sulphide inclusions (network type) have been suspected of causing intergranular fractures in three different specimens that were submitted for examination. However, the evidence was again entirely circumstantial, and the principal reason for suspecting the Type II inclusions was that they were the only possible clues that became evident from a rather careful metallographic examination.

It is known from the experience with wedge block tests that when the inclusions are about 50 per cent Type II and 50 per cent Type III, the fractures may be fibrous and tough and be completely free from intergranular fracture. If, then, Type II sulphides do cause

intergranular fractures, it must be that a rather extreme case of Type II inclusions is required.

An Unidentified Intergranular Phase

Two samples examined were found to contain a fine chain-like precipitate which followed the primary grain boundaries. Neither of the samples showed any intergranular fracture when broken in the as-cast condition; the reason apparently being that, as cast, the body of each grain was weaker than the area through which the fine precipitate passed.

After heating for 2 hr. at 1600° F, water quenching, and drawing for 1 hr. at 1200° F, both samples exhibited fractures that were approximately 50 per cent intergranular. After heating for 15 hr. at 2300° F, furnace cooling, reheating for 2 hr. at 1600° F, water quenching, and drawing for 1 hr. at 1200° F, the fractures of both were completely fibrous. The fine chain-like precipitate was observed in the as-cast specimens, Fig. 11, and in the specimen heated 2 hr. at 1600° F, water quenched, and drawn 1 hr. at 1200° F, Fig. 12, but was not present in the sample heated 15 hr. at 2300° F.

The precipitate was not evident before etching in the as-cast sample or the sample quenched from 1600° F. The precipitate has much the appearance of fine carbides; but, since it was observed by repolishing etched specimens that the chain-like precipitate etched more deeply than the other carbides, and since these particles did not go into solution when heated 2 hr. at 1600° F, they probably are not carbides.

Conclusion

The work upon which this paper is based was intended as a survey of the causes of intergranular fracture in fracture test specimens. Some of the conclusions, particularly those concerning the indirect effect of alloying elements on aluminum nitride precipitation, are based on single tests without the benefit of attempts to repeat the first results. In any investigational work this is a hazardous procedure, for there is always the possibility that some important factor has not been recognized and properly controlled, and that subsequent tests, therefore, will fail to duplicate the original results.

For this reason, the results deserve to be checked in other laboratories and under somewhat different conditions before the conclusions can be advanced with certain validity—and before the conclusions can be dogmatically applied to commercial practice. Furthermore, it is very probable that there are still other causes of intergranular fractures, not suspected at the present time, which will become evident from continued use of the fracture test.

Prime causes of intergranular fracture found and identified in samples either prepared in the laboratory or submitted for examination are as follows:

1. Aluminum nitride precipitation at primary austenite grain boundaries. Aluminum content, nitrogen content, and cooling rate after casting control the precipitation. Use of either Ti, Zr, B, S, Mo, or Cu decreases (by some indirect means) the tendency toward intergranular fracture.

2. Ferrite precipitation as a network on the primary grain boundaries. (Only causes intergranular fracture when specimens are fractured in the as-cast condition, since heat treatment obliterates such a network.)

3. Carbide precipitation at primary grain boundaries. (May cause intergranular fracture if tested in the as-cast condition or if tested after a heat treatment not capable of completely dissolving such carbides.)

4. Extreme cases of Type II sulphide inclusions. (The evidence is rather weak. Moderate amount of Type II inclusions mixed with Type III inclusions observed in a number of samples had no apparent effect on the fracture.)

5. Internal hot tears not exposed to the atmosphere prior to fracturing. (If exposed to the atmosphere as the casting cooled, the surface is conspicuously oxidized, and the fact that such defects were hot tears is obvious.)

6. A chain-like precipitate of some unidentified compound. The precipitate did not become evident until the metallographic specimens were etched. It was not dissolved by heating for two hr. at 1600° F, but was dissolved by heating for 15 hr. at 2300° F.

Acknowledgment

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The results of the study were described in O.S.R.D. Report No. 1941, Serial No. M-153, Final Report on N.D.R.C. Research Project NRC-4, Part II—Aluminum Nitride as an Intergranular Precipitate, October 19, 1943, and in O.S.R.D. Report No. 3688, Serial No. M-286, Final Report on N.D.R.C. Research Project NRC-14, Part VII—The Causes of Intergranular Fracture in Cast Armor, May 18, 1944. The writers are indebted to the following members of the Battelle Staff for their helpful assistance: G. K. Manning, P. C. Rosenthal, N. H. Keyser, and John G. Kura.

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DISCUSSION

Chairman: J. A. Rassenfoss, American Steel Foundries, Indiana Harbor, Ind.

Co-Chairman: G. Vennerholm, Ford Motor Co., Dearborn, Mich.

Milton Tilley:[1] I would like more information on the hair line cracks in Fig. 2. Are they hot tears or are they just the cavity in which the precipitate is found or is it a crack due to the precipitating compound producing stresses?

[1] National Malleable and Steel Castings Co., Cleveland.

DR. LORIG: I think those cracks are merely indications of the areas where the precipitate occurred and have been etched out by the etching reagent. They merely indicate that solution in the etching reagent was very rapid in areas occupied by the precipitate.

C. W. BRIGGS:[2] I would like more information on the residual aluminum content. I realize that it is difficult to obtain good aluminum recovery in commercial practice, but in control studies, especially on induction furnace work, it is easy to get a good aluminum recovery. It is known that in commercial practice when an addition of, say, 2 lb. of aluminum per ton is made, it is even possible to get no aluminum recovery owing to those general conditions producing FeO in the melt such as on transfer of metal from the furnace to the ladle, from the ladle to hand shanks, from hand shanks to mold, etc. There is an uncertainty just when the intergranular type of inclusions, especially aluminum nitrides are obtained. Surely there must be some time when the residual aluminum content must be fairly high. In some of the research studies of the Steel Founders Society, the residual aluminum contents were fairly high and the intergranular type of fracture was obtained. At that time we did not have sufficient information on the hydrogen content. However, the steels were made in the induction furnace similar to those heats of Dr. Lorig. I would like to obtain information about the quantity of the residual aluminum that may be responsible for intergranular fracture.

DR. LORIG: If the residual aluminum content is in the neighborhood of, say, 0.06 per cent or above, the nitrogen contents are in the neighborhood of 0.01 per cent, I think one might observe intergranular fracture in some castings. It depends somewhat on the size of casting and the rate at which it cools in the mold. Recoveries of aluminum in our practice tend to be in the neighborhood of 60 per cent. If 0.10 per cent is added the steel will then have about 0.06 per cent residual aluminum.

MR. BRIGGS: From what I could observe from commercial work, it looked to me as if it was running somewhere around 0.06 or 0.07 per cent normally.

MEMBER: I would like to know whether this intergranular condition exists if there is no aluminum added.

DR. LORIG: Yes, it may exist. Anything that will change the strength of the matrix at the grain boundary may bring about intergranular fracture. Such precipitations as ferrite networks around the grain boundaries may induce intergranular fracture. Various other factors were mentioned in the paper. Whenever there is non-uniformity in strength within the matrix, especially when the constituent at the grain boundary is different and is of different strength than the matrix itself, intergranular fracture may occur without relation to the aluminum or nitrogen content of the steel.

MEMBER: Did you notice any difference when the molybdenum content was in excess of 0.35 per cent?

DR. LORIG: Yes, molybdenum was quite effective in our tests in decreasing the amount of intergranular fracture resulting from aluminum nitride. We made only one test on each steel and probably the effect of molybdenum should be checked again. Our results indicated that the steel is less susceptible as the molybdenum content is increased.

R. C. WAYNE:[2] Did you say that any non-uniform constituent in the grain boundary, such as a network of ferrite or nitride, would cause intergranular fracture? It seems somewhat anomalous that a tough, ductile material like ferrite would have that effect.

DR. LORIG: I think it is perfectly feasible for a tough material if confined as a thin membrane between stronger, less tough material to fracture with little ductility. In Fig. 12 the matrix is primarily pearlitic and the grain boundary is ferritic. The ferrite is much lower in strength than the matrix and at the same time, while it is ductile, fracture can easily propagate through the ferrite because of stress concentration. Thus it is feasible to have intergranular fracture with both a tough material in the grain boundary and with a brittle material in the grain boundary.

MR. TILLEY: Can you give us more information on when and under what conditions you get the ferrite network and what you do to avoid it or to correct it when you have it?

DR. LORIG: That depends upon the nature of the steel that is being tested. The steel that I used in my illustration was one that was highly alloyed, hence, it had a reasonable amount of hardenability but not sufficient to avoid completely ferrite precipitation. As a consequence, it showed this ferrite network pattern upon cooling. If this same steel were cooled more slowly to obtain more ferrite in the form of equiaxed grains, it undoubtedly would have fractured with a fibrous fracture. In fact, we reheated the steel and cooled it very slowly, thereby changing the nature of its microstructure. The steel was then ductile. The rate of cooling, the hardenability of the steel, and its composition, etc., affect ferrite precipitation. It is hard to set down definite statements as to what steels will and what steels will not show the deleterious type of ferrite precipitate. It depends upon many factors and the size of section that you are dealing with.

C. E. SIMS:[3] I would like to discuss the ferrite network. While it is probably one of the unusual types, rather easily corrected by heat treatment, the principle of having a brittle fracture caused by a ductile material is not as incongruous as it may seem at first thought. Grossman in his Howe Memorial Lecture of 1946 clearly brought that out. He showed that fine films of ferrite caused brittle fractures. Because it is much weaker than the material around it, it is the material that will fail and, because it is so thin, triaxial stresses are set up and there is no chance for slip to take place along the slip planes. The result is a cleavage type fracture.

I recall one instance of a casting examined, a part of an airplane landing gear that failed in service. It had the typical brittle intergranular fracture. On close examination, however, what caused the brittle fracture in this case seemed to be an extreme case of No. 2 type sulphide. It is not likely that one would find both nitride and sulphide, because the conditions that would give the No. 2 sulphides, would not furnish enough residual aluminum to form the nitride.

DR. LORIG: In a few tests on physical properties of steel, the indications were that if the amount of intergranular fracture exceeded a certain percentage, say 25 per cent, the reduction of area and elongation values were adversely affected; and when intergranular fracture approached 100 per cent there was very little ductility, the elongation and reduction of area being reduced to values in the neighborhood of one or two per cent. The tensile strength and yield strength were not noticeably affected as far as one could tell, but the impact and tensile ductility values were modified by the presence of the intergranular fracture.

CHAIRMAN RASSENFOSS: This sort of thing might be obtained, perhaps even without aluminum additions to the steel. We were making some low alloy steels with small additions of copper and molybdenum and various combinations of those and certain of those were quite sensitive to intergranular fracture if they were stripped from the molds immediately after casting and allowed to cool rapidly in the open air. When pulled in a tensile test after a 400° F. aging treatment, which gave a straw-colored appearance to the machined test bars, the fracture exhibited a straw-colored appearance too. Apparently, the rapid cooling from the solidification of these particular steels resulted in their cracking before they were even put through the final heat treatment and machining operation and, as a consequence, they exhibited a fracture of this type, and some of them were actually quite low in tensile strength, too.

[2] Steel Foundries Society, Cleveland.

[3] Battelle Memorial Institute, Columbus, Ohio.

DENSITY OF MOLDING SAND

H. W. Dietert, H. H. Fairfield, E. J. Hasty*

ABSTRACT

Foundry experiments indicated that the chilling effect of sand upon liquid metal was related to the density of the sand as rammed. Increasing the density of the sand increased its chilling effect upon the metal.

Density of rammed sand is closely related to mold hardness. Grain size and distribution, clay and moisture content have an appreciable effect upon the density of rammed sand.

Density values on sands rammed by jolting, squeezing, ramming and blowing are given.

The density of rammed sand is modified by additions of commonly used foundry materials.

THIS INVESTIGATION was undertaken in order to find out first, whether density of molding sand had an appreciable effect upon castings and secondly, to obtain sufficient information to enable foundrymen to control or modify density of core and molding sands.

It was considered advisable to present the results in three parts as follows:

Part I	Effect of Sand Density on Metal Solidification
Part II	Effect of Moisture, Clay, Grain Size, and Other Variables on Sand Density
Part III	Effect of Ramming Method upon Sand Density

Part One
Effect of Mold Density on Metal Solidification

The phenomena of heat transference into molds after pouring has attracted the attention of a number of investigators. By placing thermocouples in molds at various distances from the casting, the nature of heat flow has been demonstrated. Scott[1] gives thermal gradients in a mold for a 16 lb iron casting. Briggs and Gezelius[2] show sand temperatures after pouring 31 and 106 lb castings. Lebesch, Hambley and Geist[3] describe sand cooling curves for a mold containing a 42 ton casting.

The properties of a metal casting are considerably influenced by the rate that its heat is removed by the sand. Womochel and Sigerfoos[4] indicate how sand properties affect shrinkage in gray iron. Dietert and Woodliff[5] demonstrated that hardness of gray iron is affected by molding sand. Chamberlin and Mezoff[6] show how physical properties of magnesium castings are influenced by cooling rates and how different mold materials produce different cooling rates.

Thermal conductivity of sand is therefore, an important factor to be considered. Dietert and Doelman[7] compared the thermal conductivity of a number of sands and inferred that density has a considerable effect upon thermal conductivity.

Since sand density affected thermal properties, it was concluded that the rate of heat transfer into molds was dependent in part upon the density of the molding sand. An investigation was, therefore, carried out to test this hypothesis. The technique used by Briggs and Gezelius[2] in their work on rate of skin formation was thought to be suitable to the measurement of relative chilling effects of molding sand.

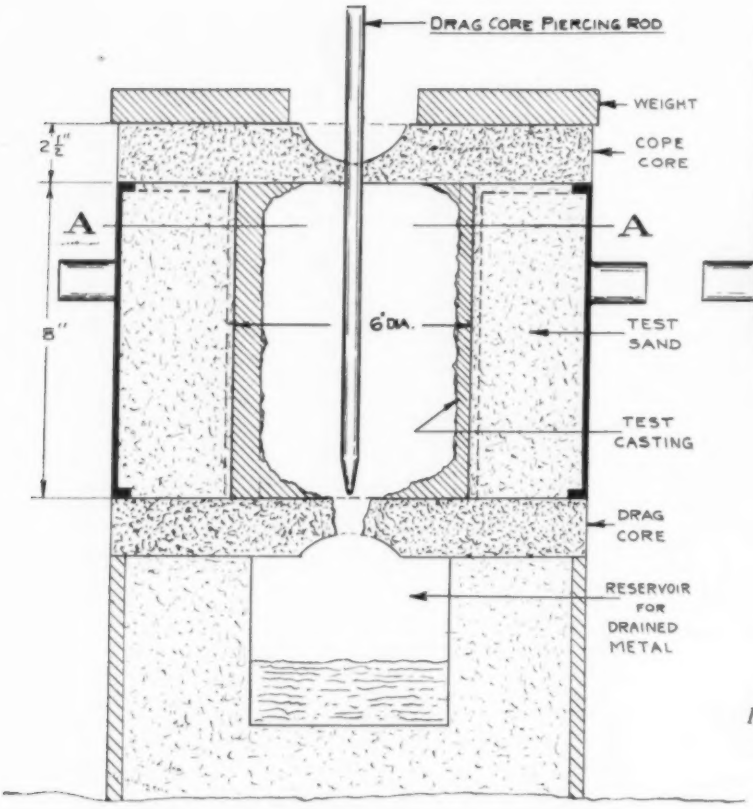
Equipment and Method

Dry Sand Mold—Two types of patterns were proposed for this project. The first is shown in Fig. 1. A cylindrical pattern 6 in. in diameter and 8 in. high was rammed up in a cheek flask. The circular flask was divided into four compartments, so that four different kinds of sand could be rammed around the cylindrical pattern. The sands used are described in Table 1. This mold was dried for 5 hr at approximately 400 F and then set on top of a slab core. The cope consisted of another slab core.

The mold was poured, and after 10 min had elapsed, an iron rod was pushed down through the sprue and the bottom of the mold was knocked out. This allowed the liquid metal to drain out of the mold, leaving a shell of metal on the mold surface. The thickness of the metal shell was indicative of the cooling rate of the sand. Table 2 indicates the chemical analysis of the metal used for these experiments.

Green Sand Mold—The second type of pattern used is shown in Fig. 2. A different type of sand was rammed around each wedge. The casting was made upright in an open top mold using green sand. Three minutes after pouring, the mold was tipped over and the unsolidified metal was poured out.

*President, Foundry Consultant and Assistant Foundry Consultant, respectively, of the Harry W. Dietert Company, Detroit.



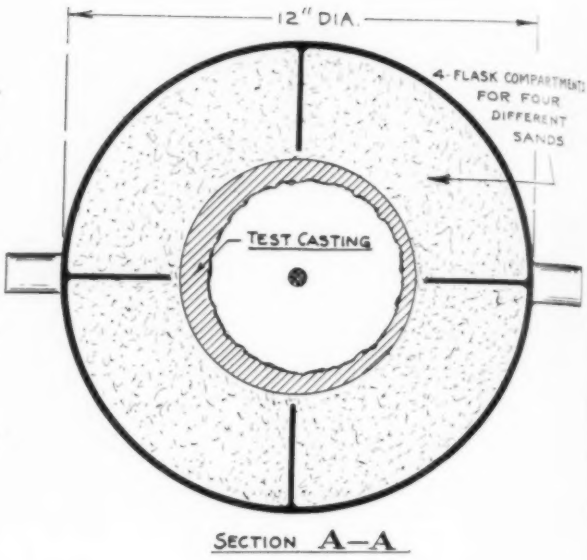
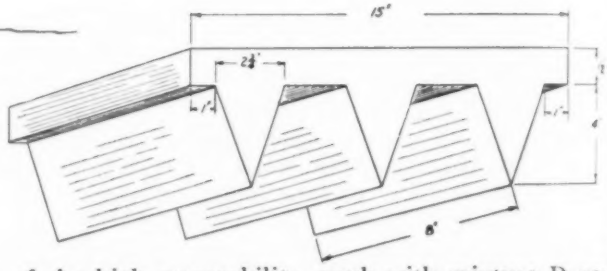


Fig. 1—Cylindrical Type Density Test Pattern (above)

Fig. 2—Wedge Type Density Test Pattern (below)



Density of Green Molding Sand

A survey of mold hardness was made on each mold. Table 3 gives the hardness test results on the mold in which cast sections shown in Fig. 3 were made. Because of the high permeability, work with mixture D was discontinued and results are not shown for it in Tables 3, 4 and 5.

TABLE 1.—SAND MIXTURES USED FOR TEST CASTINGS

Sand Mixture	A	B	C	D
Coarse Silica Sand	61.33	—	—	90.0
Fine Silica Sand	—	94.0	95.0	—
Fireclay	8.0	—	—	10.0
Bentonite	—	6.0	4.0	—
Silica Flour	30.67	—	—	—
Cereal Binder	—	—	1.0	—
	Physical Properties			
Moisture, per cent	6.6	2.8	2.8	2.8
[1]Green Permeability	38	100	29	590
[1]Green Compression, psi.	7.8	10.0	4.8	6.0
[1]Green Deformation, in./in.	.024	.015	.017	.019
[1]Density, lb./cu. ft.	125	96	94	105
Retained on U. S. Sieve Number	Screen Analysis Percent Retained			
6	—	—	.1	—
12	—	.1	.2	.06
20	.5	—	.3	1.54
30	19.9	.3	.2	40.1
40	37.2	1.2	.2	41.5
50	3.5	.6	.3	3.7
70	.8	29.9	.2	1.7
100	.2	43.7	3.1	.6
140	.6	14.4	36.8	.6
200	1.6	3.0	35.0	.3
270	2.9	.6	14.2	.2
Pan	11.3	.2	4.8	.1
Average Grain Fineness	43.1	70.8	137.4	27.7
A.F.A. Clay Content, Per Cent	21.5	6.0	4.6	9.6

[1]These tests made on 2 in. diameter, 2 in. long specimen rammed according to A.F.A. procedure.

Specimens of the same sands were rammed in a standard 2-in. inside diameter specimen tube using a standard AFA rammer and the density obtained by the relationship:

$$\text{Density (lb./cu. ft.)} = \frac{\text{weight (in lb.)}}{\text{volume (in cu. ft.)}}$$

In the case of a 2 in. diameter sand specimen weighing 165 grams:

$$\text{Density} = \frac{165 \times 1/454}{\frac{\pi \text{ length}}{1728}} = \frac{0.36}{\text{length} \times \frac{\pi}{1728}} = \frac{200}{\text{length}}$$

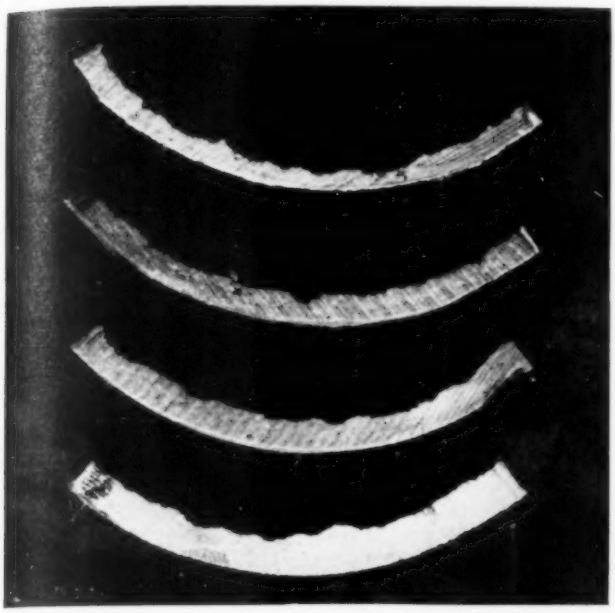
$$\text{Density} = \frac{200}{\text{length (in in.)}}$$

When the specimen weight is 194 grams, this becomes:

$$\text{Density} = \frac{235.5}{\text{length (in in.)}}$$

TABLE 2.—CHEMICAL ANALYSIS OF IRON

Carbon	3.5
Silicon	1.95
Manganese	0.65
Sulphur	0.095
Phosphorus	0.40



Sand Mixture	Molding Sand Density, lbs/cu ft	Average Metal Thickness Inches
C	81	0.213
B	95	0.270
A	122.5	0.330
B	91.5	0.235

Fig. 3—Sections of solidified metal shell from cylindrical density test pattern

With the weight of the sand and diameter of the specimen constant, density is proportional to length. In order to facilitate the measurement of sand density, the device shown in Fig. 4 was used. This instrument indicates the length of the standard green sand specimen as rammed. The scale of this instrument reads directly in lb per cu ft.

Using the indicator described above, 2x2-in. specimens of sand were rammed to varying degrees of compactness and their density determined. Five mold hardness readings were taken on each end of each sand specimen. Each hardness value reported in Table 4 is an average of 10 observations or more. The density of the sand at the surface of the experimental mold was assumed to have the same correlation with mold hardness as the 2x2-in. specimens described in Table 4.

Using the same technique, the density of the mold for the wedge-type casting was determined. Both mold hardness and density measurements were made on green sand. All densities mentioned in this report are for the sand in the green state. Density of the dried sand can be estimated as follows:

If a green sand containing 5 per cent moisture and having a density of 100 lb/cu ft were dried, its dry density would be:

Dry density = 100 (100 per cent − 5 per cent) lb/cu ft
Dry density = 95 lb/cu ft

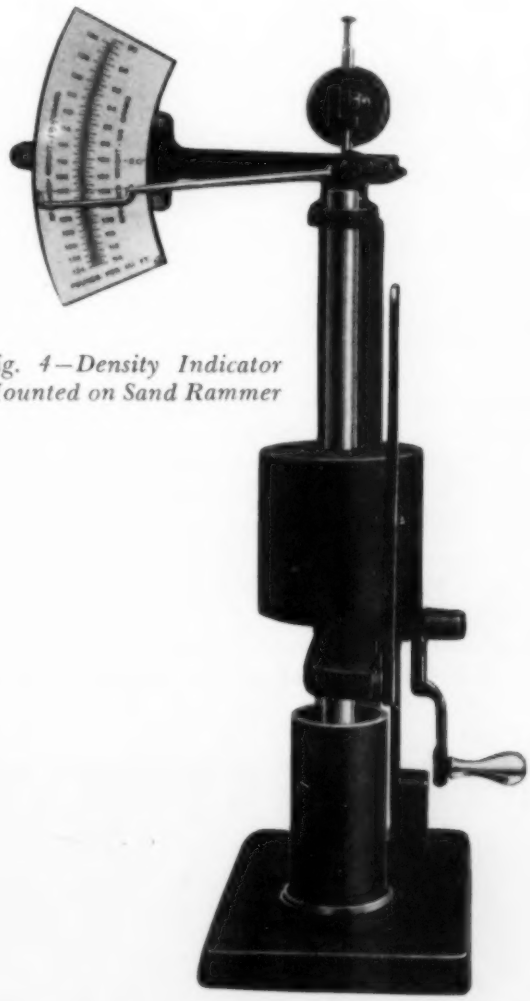


Fig. 4—Density Indicator Mounted on Sand Rammer

Solidified Metal Shell Thickness

Castings Made in Dried Mold—The solidified metal shell remaining on the inside surface of the mold made from the cylindrical test pattern had a rough and irregular inside surface as shown in Fig. 3. The top and bottom of the cylindrical casting were cut off and a section about 4 in. in length remained. This shell was cut into four pieces, each piece coinciding with one of the test sands. The pieces were weighed and measured. The thickness values reported are calculated from surface area and weight. The test data showing the average thickness of the metal which had solidified 10 min after pouring is tabulated in Tables 5 and 6.

TABLE 3.—MOLD HARDNESS SURVEY, CYLINDRICAL TEST MOLD

Sand Mixtures	A	B	B	C
	88	69	74	40
	81	72	80	45
	80	69	76	50
	79	69	75	55
	80	72	75	52
	76	74	73	53
	78	72	77	51
	84	71	76	46
	80	73	76	
	77			
Average	80.3	71.2	75.7	49
Number of Tests	10	9	9	8
Standard Deviation	3.3	2.5	1.9	4.64

TABLE 4.—DENSITY AS RELATED TO MOLD HARDNESS

Sand Mixtures	Mold Hardness	Density, Lb/ cu. ft.
A	71	113.5
	72.5	116.5
	75	119.0
	80	122.5
	84	126.0
B	66	88.5
	71	91.5
	72	92.5
	76	95
	77	96
C	41	79.5
	49	81
	57	82.6
	68	89.5
	59	95
	73	101
	82.5	105

Tests made on 2 x 2 in. dia. test specimens.

Casting Made in Green Sand Mold—The sections of solidified metal as obtained from the wedge test pattern, Fig. 3, three minutes after pouring are shown in Fig. 5. The height of the solidified metal in the wedge section is tabulated below:

Sand Mixture	C	B	A
Height of Wedge in in.	1.3	1.5	2
Sand Density in lb /cu. ft.	80	90	119

A graphical presentation of the effect of sand density on the rate of metal solidification is shown in Figs. 6 and 7.



Fig. 5—Sections of Wedge Type Density Test Castings

Dietert and Doelman[7] demonstrated in previous tests that heat flow into molding sand increases with sand density. Table 7 and Fig. 8 show the sands used and the thermal conductivity of these sands at various temperatures. Using the thermal conductivity values of this work, the thermal conductivity of the sands used in this report were estimated.

Discussion

The relationship of density to mold hardness of 2-in. compression test specimens was assumed to be the same as the relationship of mold hardness to density of an actual mold when the same batch of sand was used for both tests.

It is realized that the technique of using mold hardness as an indicator of density involves some error. However, it is the only non-destructive technique available at the present time, and foundrymen are accustomed to mold hardness as an index of density of ramming.

From the casting tests made, it is evident that as sand increases in density it has a more chilling effect upon the metal.

It has been established by Dietert and Doelman[7] that thermal conductivity of sand increases with sand density. From their test data, which was obtained on Ottawa silica sands similar to the ones used in these experiments, it is possible to make an estimate of the probable thermal conductivity of the sands described in Table 1 of this paper.

TABLE 5.—EFFECT OF DIFFERENT SANDS ON SOLIDIFICATION OF GRAY IRON IN A DRY SAND MOLD
Poured at approx. 2450°F.

Sand Mixture	A	B	B	C
[1]Thermal Conductivity, Btu	—	10.3	9.3	6.3
Mold Hardness (green)	80	76	71	49
[2]Density, lb./cu. ft.	100.5	95	91.5	81
Average Thickness of Chilled Metal, in.	.33	.274	.235	.213

[1]Btu/hr/ft [2]/deg. F./in. Measured at 2300°F. This value is estimated from tests made on similar sands. For complete details see reference No. 7.
[2]Density is estimated from the correlation between density and mold hardness recorded in Table 4.

TABLE 6.—EFFECT OF DIFFERENT SANDS UPON SOLIDIFICATION OF GRAY IRON IN A DRY SAND MOLD
Poured at approx. 2525°F.

Sand Mixture	A	D	B	C
[1]Thermal Conductivity, Btu	—	12.2	7.3	6.1
Mold Hardness (green)	72.5	73	66	41.5
[2]Density, lb./cu. ft.	115	101	85	80
Average Thickness of Chilled Metal, in.	0.210	0.217	0.150	0.140

[1]Btu/hr/ft [2]/deg. F./in. Measured at 2300°F. This value is estimated from tests made on similar sands. For complete details see reference No. 7.
[2]Density is estimated from the correlation between density and mold hardness recorded in Table 4.

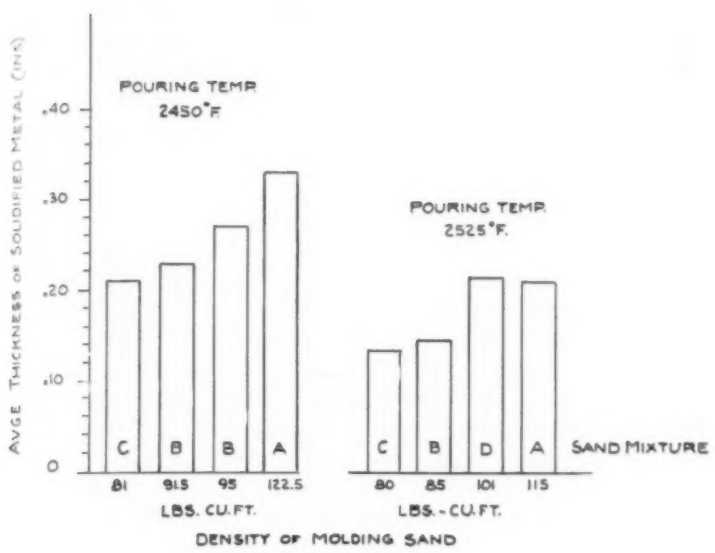


Fig. 6—Amount of Metal Solidification, Cylindrical Pattern Dry Sand Mold

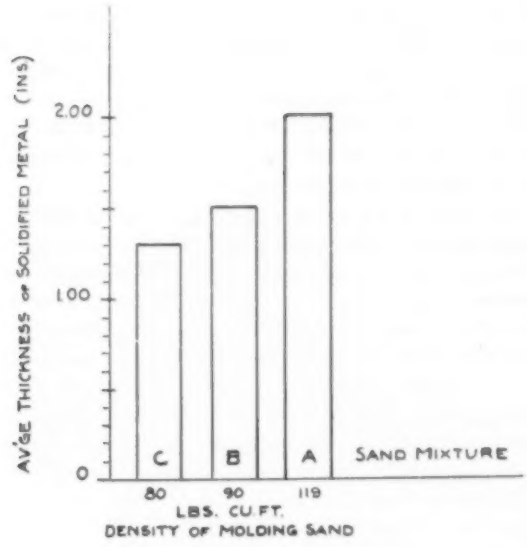


Fig. 7—Amount of Metal Solidification, Wedge Pattern Green Sand Mold

Castings made with sand mixture D exhibited the phenomenon of metal penetration. This sand differed markedly from the other sands in that it had a permeability of 590, whereas the sand mixtures A, B and C had permeabilities of 38, 100, and 29, respectively. It was observed that the amount of metal which solidified against sand D was greater than could be attributed to the density of this sand (Fig. 6 and Table 6). It was inferred therefore, that permeability also affects thermal conductivity and hence affects chilling action of the sand.

Sand mixture B when rammed to a mold hardness of 91.5, chilled 0.235 in. of liquid iron. When rammed to a mold hardness of 95, this same sand chilled 0.274 in. of liquid metal in the same mold (Table 5). This demonstrates that harder ramming increases chilling effect, other factors being equal.

The evidence presented in this paper is sufficient to indicate that it would be worthwhile to conduct further investigation into sand density and other factors which affect the rate of chilling of metal in sand molds.

Conclusions

1. Either a cylindrical or wedge-shaped pattern may be used to study satisfactorily the rate of metal solidification by the mold bleeding test. The wedge-shaped test pattern requires the least amount of pattern equipment, and it is easy to measure the height of the metal solidified in the wedge section, thus making it a little easier to use than a cylindrical pattern.

2. The density of molding sand, whether controlled by the degree of ramming or by choice of ingredients, exerts sufficient influence on the rate of metal solidification of gray iron to make it of practical importance. As the density of the sand increases, the rate of metal solidification increases. Thus, the density of the sand may affect the structure of the metal and the manner in which the casting shrinks.

TABLE 7.—PROPERTIES OF SANDS USED FOR THERMAL CONDUCTIVITY MEASUREMENTS[7]

Sand Sample No.	No. 1	No. 2	No. 3	No. 4
Grade	Coarse	Fine	Fine	Coarse
Mesh—through	30	100	100	30
Mesh—on	40	140	140	40
Percentage Western Bentonite	4	4	4	4
A.F.A. Green Specimen Weight in Grams	164.1	140.6	130.5	164.1
A.F.A. Dry Permeability	1250.4	74.5	110.0	1503.0
Thermal Conductivity	4 in. dia.	4 in. dia.	4 in. dia.	4 in. dia.
Specimen Size	1 in. thk.	1 in. thk.	1 in. thk.	1 in. thk.
Density	1.595	1.363	1.26	1.363
Wt./cu. ft.	100.6	89.7	80.0	89.7
Mold Hardness	63	63	45	50
Specimen Weight in Grams, dry	321.7	274.9	250.6	274.9
Specimen Volume, cc.	205.91	205.91	205.91	205.91
Thermal Conductivity[2] at 2300° F.	11.9	8.8	6.1	—
Thermal Conductivity[2] at 2000° F.	9.7	6.8	5.1	6.9

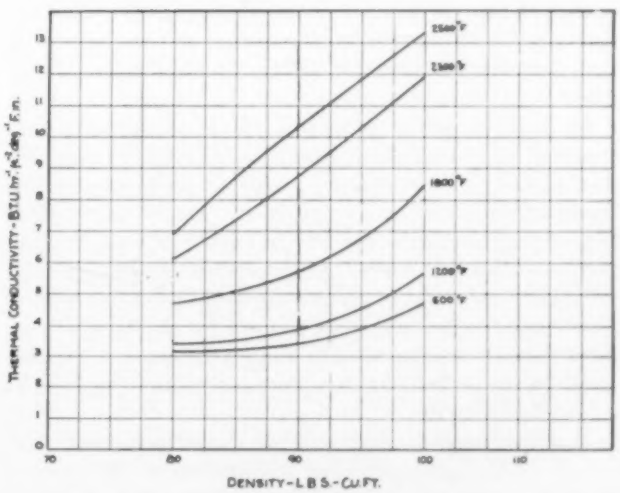


Fig. 8—Relationship between Thermal Conductivity and Density of Dried Molding Sand

3. With sands of exceedingly high permeability, for example 600, one can expect large void spaces opening up into the mold cavity into which molten metal would flow, thus making greater surface contact with the molding sand. This causes the heat transfer to be greater than one would expect from the thermal conductivity of the sand.

4. It is recognized that density of molding sand is not the sole factor that affects the rate of metal solidification of a casting due to heat absorption from the molten metal. Such factors as moisture, permeability, mold atmosphere, void size, gaseous reaction within the mold and others contribute to this phenomenon.

5. Mold hardness must be controlled to exercise control over the density of a sand in a mold. A 10 per cent change in mold hardness may mean a 10 per cent change in the density of some sands.

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Part Two
Effect of Variables on Sand Density

Foundry test work described in Part One of this paper demonstrated that the chilling effect of sand is dependent in part upon the density of the sand as rammed in a mold. The control of sand density is therefore, important to the production of good castings. This investigation was undertaken in order to find out the relationship between density of molding sand and some of the other sand properties.

In order to cover as wide a range of properties as possible, 45 test patches of sand were carefully weighed and mixed in a laboratory muller according to AFA standard procedures. Except in the cases where moisture was deliberately varied, the sand was tempered to the best workable moisture. If moisture had been held constant in a series of test sands, some mixtures would have been too dry and some too wet for molding. It was considered more practical to temper the sand to its optimum point by feel. At this point, green deformation is about 0.015 in/in.

A density indicator (Fig. 4) was used to measure the height of the specimen after it was rammed in a standard AFA rammer by three drops of the weight. By using an exact weight of sand, the density in lbs per cu ft is indicated directly from the rammed specimen length.

Table 8 and Fig. 9 show the effect of moisture content on the density of four different molding sands. Other sand properties are also tabulated in Table 7 which help to describe the sands under observation.

To study the effect that clay content of molding sand would have upon the density, mixtures were made up of an AFA standard testing sand and various percentages of fireclay type bonding clay from 8 to 16 per cent. These sands were mixed for four minutes in a laboratory muller. The test data covering the effect of percentage of clay on the density of sand is tabulated in Table 9 and graphically in Fig. 10.

A study of molding sand density would not be complete without determining the influence of grain size. Five sands were prepared of different finenesses, ranging from an average grain fineness of 28.4 to 140.7. These sands were bonded with 12 per cent fireclay type bonding clay and mulled in a laboratory size muller. The test data is tabulated in Table 10.

Four sands were prepared by screening washed and dried Ottawa silica sand. The spread in grain size was progressively increased. Twelve per cent fireclay type bonding clay was added to each sand and sufficient water to bring it to the best workable moisture. The sands were mixed for four minutes in a laboratory muller. Fig. 11 shows the cumulative screen analysis for the four sands and the density obtained.

TABLE 8.—EFFECT OF MOISTURE UPON DENSITY
Standard A.F.A. Ramming Method

Type of Sand	Albany Molding Sand		Heap Sand		A.F.A. Std. Sand with 4% Bentonite		A.F.A. Std. Sand with 12% Bond Clay	
	Moisture	Density	Moisture	Density	Moisture	Density	Moisture	Density
	5.0	104.2	3.9	99.2	1.5	93.0	2.4	101.0
	5.5	103.4	4.5	100.4	1.75	92.8	3.0	100.1
	6.0	104.4	5.4	101.8	2.0	92.3	3.8	101.0
	6.5	104.9	6.2	106.4	2.25	93.4	5.0	106.7
					3.7	94.5	5.5	107.8
					4.1	96.5		
					4.6	96.8		
Physical Properties								
Moisture, %			6.4		5.4	1.9		3.6
Green Permeability			8.5		109.0	227.0		154.0
Green Compression, psi.			14.5		8.6	5.4		8.8
Green Deformation, in/in.			.014		.0165	.015		.018
Flowability, %			61		66	83		85
Screen Analysis								
U. S. Sieve Number			Percentage Retained					
6			—		—	—		—
12			.2		.8	—		—
20			.2		3.6	—		—
30			.1		3.2	.04		.1
40			.3		9.8	.06		.2
50			.1		6.6	.2		.5
70			1.4		47.8	95.1		87.2
100			9.5		10.0	.6		.4
140			19.7		4.4	.1		.1
200			18.5		1.5	.1		.1
270			11.0		.8	—		.2
Pan			19.2		1.1	—		.2
Average Grain Fineness			165.4		54.5	50.2		54.0
A.F.A. Clay Content			19.8		10.4	3.8		11.0

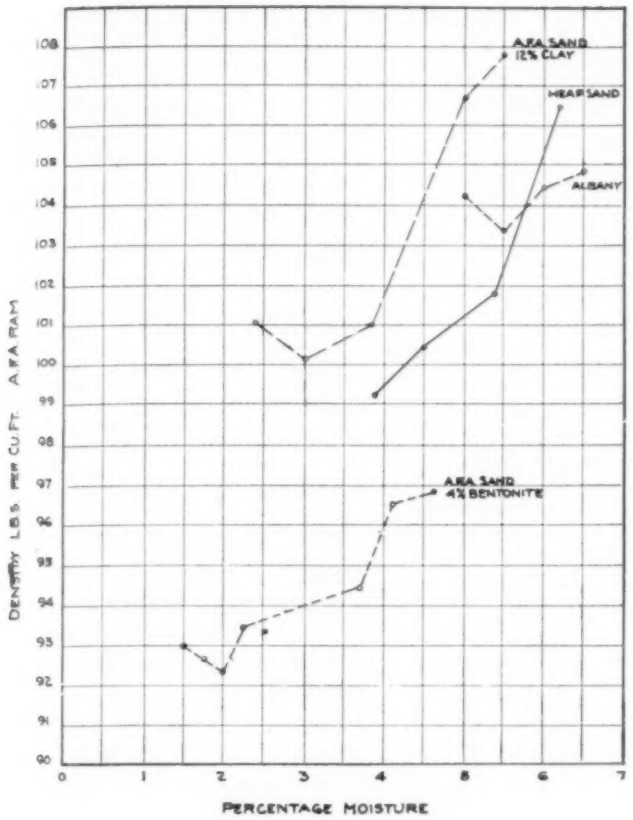


Fig. 9—Effect of Moisture upon Density in Different Sands

Sand Density vs. Mold Hardness

In order to obtain the wide range of ramming necessary to show the relationship between mold hardness and density, shown in Table 11, the following procedure was adopted. Starting with a weight of 120 grams, the sand was placed in the specimen tube and then rammed until the specimen was found to be 2 in. long as shown by the density indicator. The sand specimen was then carefully removed from the tube and 5 mold hardness readings taken on each end of it.

Increasing weights of sand samples were used and each one was rammed with increasing effort to the 2 in. length, recording the mold hardness on each end. The final sample weighing 180 grams required about 20 drops of the standard AFA rammer in order to ram the sample to a 2-in. length. The mold hardness and density of these sand specimens is given in Table 12. It is evident that the density of a molding sand can be controlled to a considerable extent by the hardness to which it is rammed. For example, the bentonite bonded sand has a density of 75.8 lb/cu ft at a mold hardness of 48. When the mold hardness is increased to 88.5, the density is 93.9 lb/cu ft. Density is more closely related to mold hardness than to any other variable.

Discussion

Briggs and Morey[1] showed that there is an optimum moisture content at which density of rammed sand is the lowest. Increasing moisture beyond this point results in increased density. They also showed that density of rammed sand increases with clay content. The results of this investigation tend to verify the work of Briggs and Morey.

The investigation of the effect of clay on density was determined when the sand was tempered to the optimum moisture content. It is apparent that some sands can be used with a much wider range of moisture content than others without encountering a wide variation in density. The Albany sand examined, for example, did not change appreciably in density with wide variations in moisture. Sands which are sensitive to slight changes in moisture, for example, heap sand as shown in Fig. 10, will require much closer control than an insensitive sand. It is apparent therefore, that the consistency of mold hardness and mold density in the foundry depend in part on selection of sands and binders.

An increase in the spread of grain distribution causes a rapid increase in the density of rammed sand. Sand with a uniform grain size possessed a rammed density of 100.5 lb/cu ft. When the majority of the sand grains were spread over three mesh sizes, then the density increased to 105.2 lb/cu ft. When 90 per cent of the sand was spread over six mesh sizes, the density of the rammed sand was 115.6 lb/cu ft. Brinson[2] states that a wide grain size distribution gave superior thermal sta-

Table 9.—Effect of Clay upon Density

Percentage Fireclay Type Bonding Clay	Percentage A.F.A. Standard Sand	Percentage Moisture	Density
6.0	94	2.2	96.1
8	92	1.8	97.6
10	90	2.4	99.2
12	88	2.75	100.3
14	86	3.3	102.0
16	84	4.2	103.6

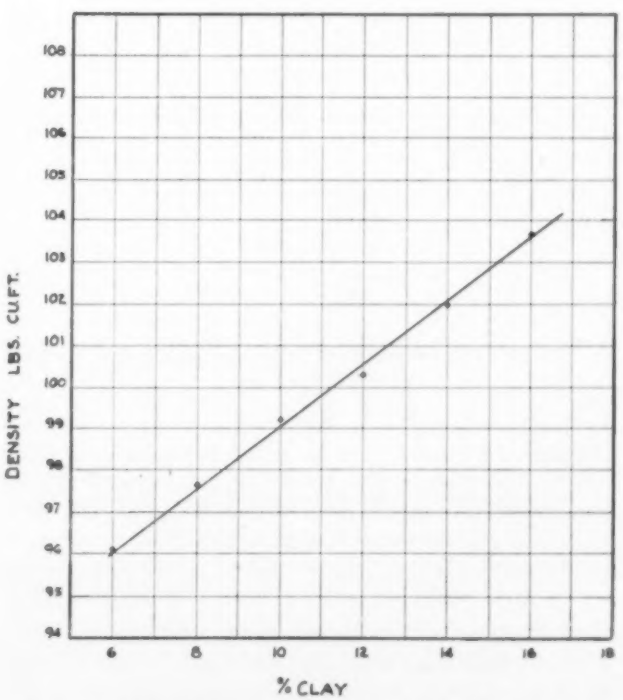


Fig. 10—Effect of Clay on Density of AFA Testing Sand (Fire-Clay Type Bonding Clay)

TABLE 10.—EFFECT OF GRAIN SIZE UPON DENSITY

Sand Number	C-1	C-3	C-5	C-6	C-7
U. S. Sieve Number		Percentage Retained			
6	—	—	—	—	—
12	.14	—	—	—	—
20	.86	.2	.2	.2	—
30	31.1	8	.2	.1	—
40	50.5	8.4	.4	.5	2
50	4.0	43.0	1.0	.6	.2
70	1.8	32.4	75.2	.4	.2
100	.2	2.7	9.4	3.0	.4
140	.1	.9	2.0	68.0	15.4
200	.1	.4	.1	14.7	60.2
270	.2	.2	.1	.5	11.2
Pan	.2	—	.4	.4	.6
Average Grain Fineness	28.4	44.8	54.3	105.8	140.7
A.F.A. Clay Content	10.8	11.0	11.0	11.6	11.6
		Physical Properties			
Density, lb./cu. ft.	104.3	104.6	103.0	101.0	102.8
Moisture, Percent	2.6	2.9	2.8	3.6	3.6
Green Permeability	530	180	130	35	19
Green Compression, psi.	11.4	12.5	9.0	7.4	7.5
Green Deformation, in/in.	.012	.011	.016	.016	.019

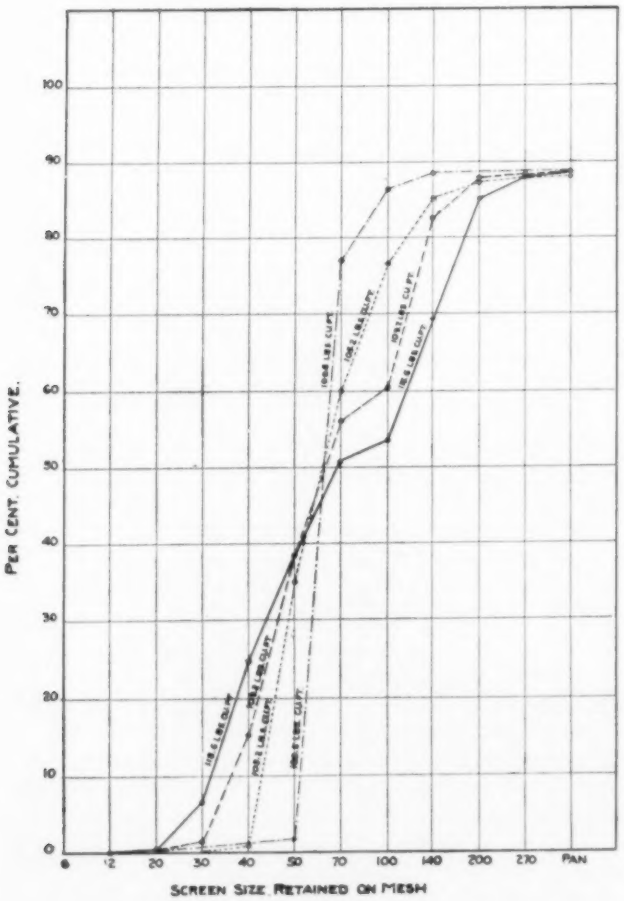


Fig. 11—Effect of Grain Spread upon Density

bility to a molding sand and prevented buckles. Since grain spread and density are related, density might prove to be a useful indication of refractory properties of a sand.

TABLE 11.—EFFECT OF GRAIN DISTRIBUTION UPON DENSITY

Sand Number	C-5	C-4	C-3-4-6	C-1-3-4-6-7
U S. Sieve Number		Percentage Retained		
6	—	—	—	—
12	—	—	—	.04
20	.2	—	.3	.16
30	.2	—	1.2	6.4
40	.4	.4	14.0	18.6
50	1.0	35.0	23.8	13.3
70	75.2	25.7	17.3	12.3
100	9.4	16.7	4.4	2.7
140	2.0	8.6	22.2	16.3
200	.1	2.2	5.2	15.7
270	.1	.4	.3	2.9
Pan	.4	—	.3	.4
A.F.A. Clay Content	11.0	11.0	11.0	11.2
		Physical Properties		
Density, lb./cu. ft.	100.5	105.2	109.2	115.6
Moisture, Percent	2.8	2.9	4.0	4.5
Green Permeability	130	190	74	36
Green Compression, psi.	9.0	11.3	8.8	7.8
Green Deformation, in/in.	.016	.012	.023	.026

TABLE 12.—DENSITY AS RELATED TO MOLD HARDNESS

Density, lb/cu. ft.	Weight of 2x2 in. specimen in grams	Mold Hardness*		
		100% Albany Molding Sand 6.4% Water	96% A.F.A. Sand 4% Bentonite 1.9% Water	88% A.F.A. Sand 12% Bonding Clay 3.6% Water
66.7	110	—	12	—
69.7	115	12	18.5	—
72.7	120	—	33.5	10
75.8	125	—	48	15.5
78.8	130	38	53	30
81.8	135	—	60	38
84.8	140	57	72.5	48
87.9	145	—	78.5	65
90.9	150	71.5	86.0	71
93.9	155	76	88.5	80
97.0	160	84	91.5	85
100.0	165	89	93	90
103.0	170	91.0	—	93 5
106.0	175	94.5	—	95.5
109.0	180	—	—	96.0

*Each value is an average of 10 or more observations.

Ash and Lissell[3] have indicated that rammed sand composed of round grains is more dense than rammed sand made up of angular grains. It was noted that the sand selected for grain size, described in Table 10, varied in grain shape. The coarse grains were rounded, the finer grains were angular.

Conclusions

Density of molding sands as determined by measuring the volume and weight of a 2x2-in. specimen rammed by the standard AFA procedure is affected by the following variables:

1. Density increases with mold hardness.
2. Density increases with moisture content.
3. Bentonite bonded sand has a lower density than fireclay bonded sand of the same grain shape and classification.

4. Density increases with clay content.
5. Coarse sand has a slightly higher density than fine sand.
6. A sand that has a wide spread in grain size will have a higher density than a sand which has grains all of equal size.

The above conclusions apply to sands tempered to, or near, the optimum moisture content.

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Part Three
Effect of Ramming Method on Sand Density

In the foundry, sand is rammed by jolting, squeezing, slinging, butt-ramming and blowing. It was decided to study the effects of these different methods of compacting upon the density of molding sand.

Using density as an index of the effectiveness of the ramming procedure, a comparison of these ramming methods, with the exception of slinging, was made.

Sand properties were varied in order to see how the density of sand rammed by various methods was affected.

Forty-five different sand mixtures as used and described in the second section of this paper were available for this investigation.

Reproducible butt ramming was obtained by using a standard AFA sand rammer, equipped with a density indicator as shown in Fig. 4. The degree of ramming was changed by altering the height from which the rammer weight was dropped and the number of drops. The sand samples were weighed to exactly 165 grams for sands ranging from 80 to 120 lb/cu ft density, and 194 grams for sands ranging from 120 to 140 lb/cu ft.

Figure 12 shows the device used to prepare a sand specimen by jolting. A standard specimen tube with a plug in the lower end was used. A small cope weight weighing 200 grams was placed on top of the sand to be jolted. This weight was intended to represent the weight of 4 in. of loosely packed sand. It was found that 9 drops from a 4¾ in. height gave a degree of hardness similar to that obtained by the AFA standard ramming procedure.

For the squeezing type of ramming, an air squeezer as illustrated in Fig. 13 was used. The squeezing force was obtained from a 2-in. inside diameter cylinder using 50 psi air pressure. This method of squeezing gave a mold hardness on a 2-in. diameter specimen approaching that obtained by AFA standard method of ramming.

Figure 14 illustrates the equipment used to blow 2x2-in. specimens of molding sand. Three hundred thirty grams of sand were loaded into the hopper on top of a paper diaphragm. The vent at the bottom of the specimen was 2 in. in diameter. An air pressure of 115 psi was suddenly applied in the sand hopper causing the paper diaphragm to break and allowing the sand to be blown into the specimen tube. The bottom of the sand reservoir consisted of a plate. When this reservoir was slid over the top of the mold, the specimen was sheared to a 2-in. length. Some patching was done with a slick to insure that no cavities existed in the top of the specimen.

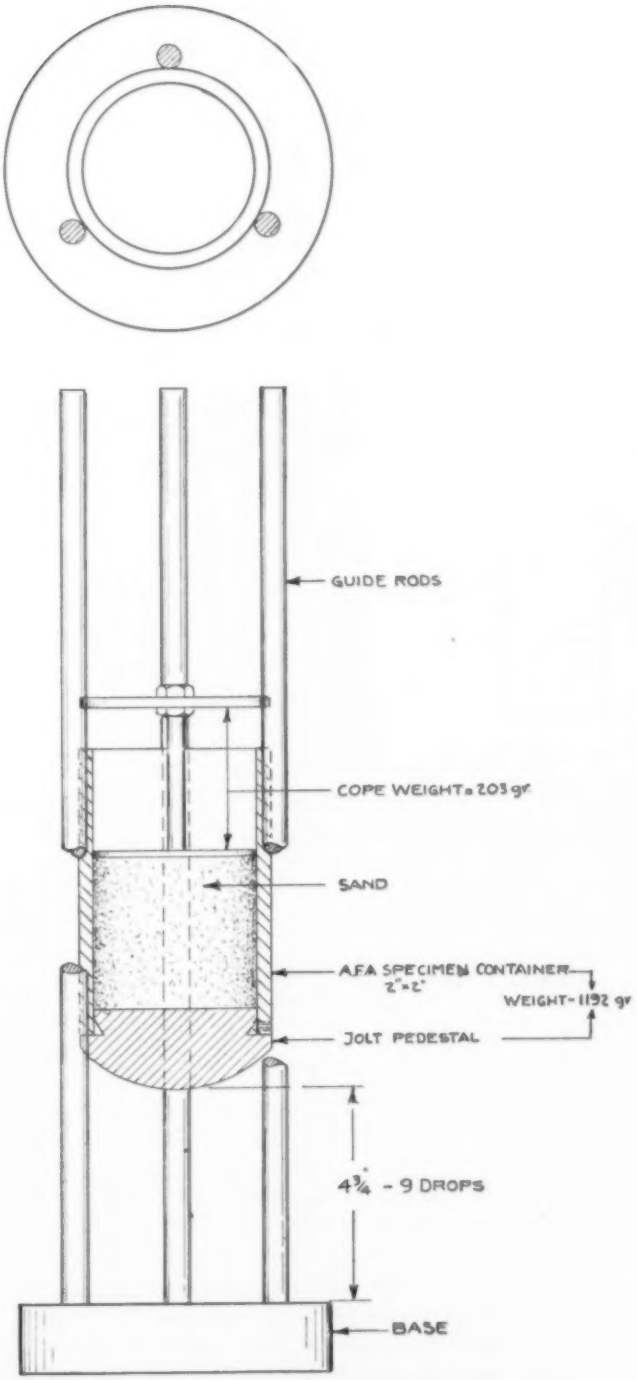


Fig. 12—Equipment for Jolt Ramming a 2x2-in. Sand Specimen

In the study to determine the sensitiveness of butt, jolt, blow and squeezing ramming to a change in the moisture content of a sand, the four sands as described below were selected:

1. A natural bonded Albany sand.

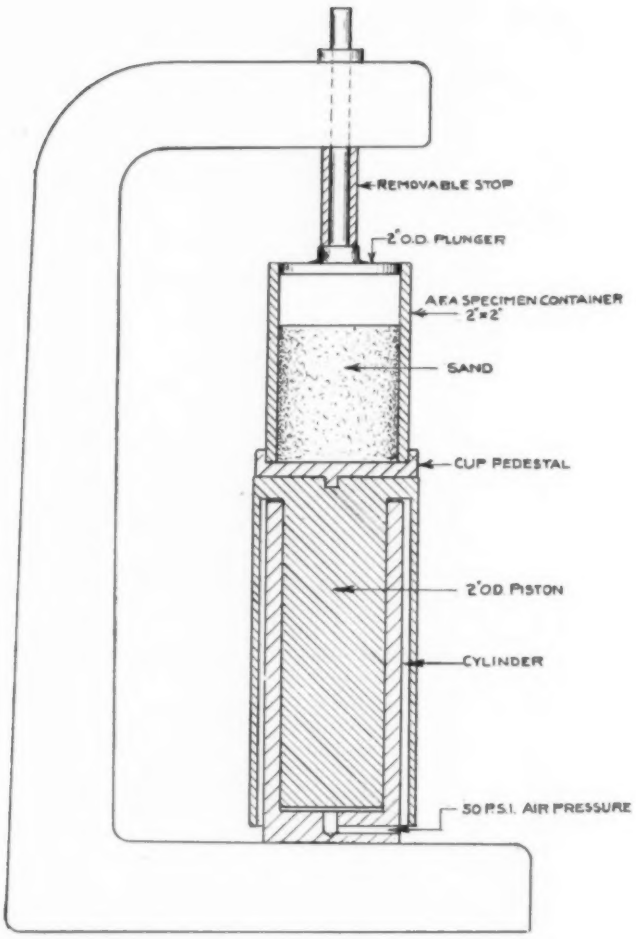


Fig. 13—Equipment for Squeezing a 2x2-in. Sand Specimen

2. A heap molding sand.

3. A synthetic molding sand consisting of AFA standard testing sand bonded with 4 per cent western bentonite.

4. A synthetic molding sand consisting of AFA standard testing sand bonded with 12 per cent bonding clay.

Batches of each type of sand were prepared with different moisture contents. With each method of ramming, 2x2-in. test specimens were compacted for each moisture content. The density of the sand was read directly from the density indicator for butt ramming. For all other types of ramming, the density was computed from the length and weight of the specimen. When 165 grams of sand are used,

$$\text{density (lb/cu ft)} = \frac{200}{\text{Height (in.)}}$$

When 194 grams of sand are used,

$$\text{density (lb/cu ft)} = \frac{235.5}{\text{Height (in.)}}$$

The test data obtained from the work on the effect of moisture upon density of sand is tabulated in Table 8. The relationship between moisture content and density obtained by each type of ramming is shown graphically in Fig. 15 for Albany sand, Fig. 16 for heap sand, Fig. 17 for bentonite sand and Fig. 18 for clay bonded sand.

A change in clay content of a sand produces an appreciable change in practically all other sand properties. Thus, a change in clay content would be expected to have some effect on the effectiveness of the different types of ramming, using density of the sand as rammed as the index.

For this study, AFA standard testing sand, bonded with a fireclay type bonding clay and tempered to the best workable moisture content, was used. Rammed 2x2-in. specimens were made by the butt, jolt, blow and squeeze method of ramming. The test data is found tabulated in Table 13 and graphically illustrated in Fig. 19.

Five Ottawa silica sands of different grain finenesses were bonded with 12 per cent bonding clay and tempered to the best workable moisture content. Sand specimens 2x2 in. in size were rammed by butt, jolt, squeeze and blow. The density of the rammed specimens obtained from each method of ramming is tabulated in Table 14 for each sand ranging from an average fineness number of 28.4 to 140.7.

Ingredients such as seacoal, cereal binders, wood flour, wood sulphates and silica flour, all change the

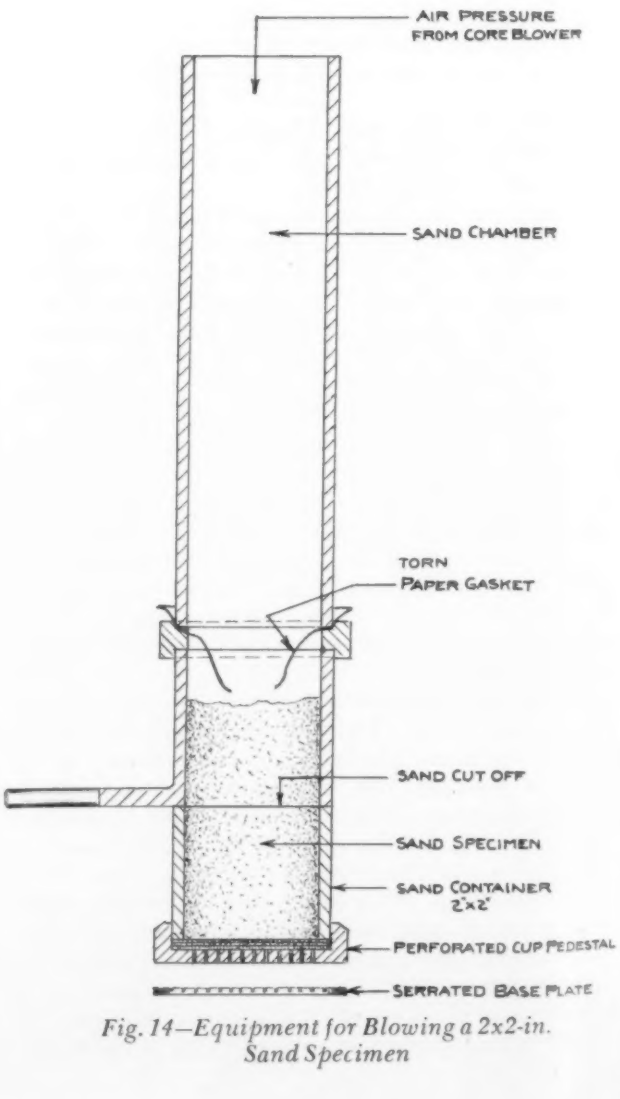


Fig. 14—Equipment for Blowing a 2x2-in. Sand Specimen

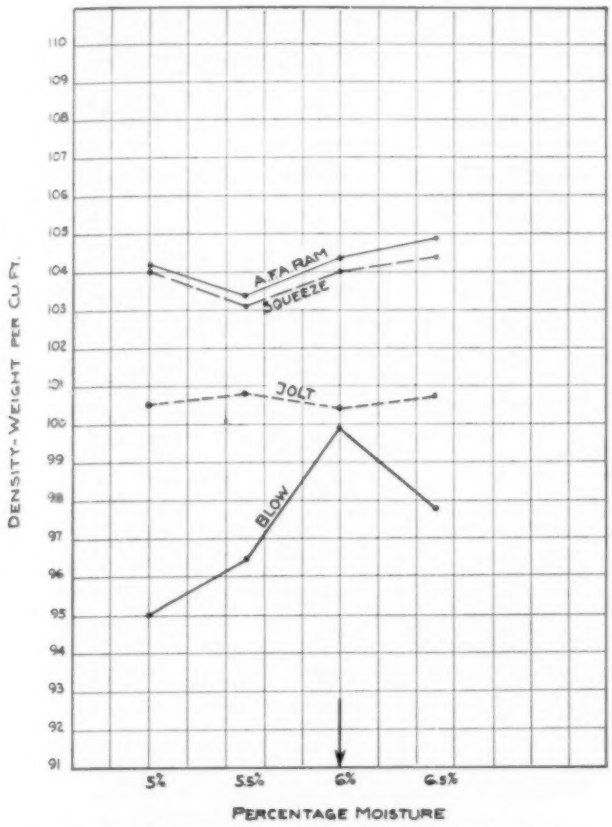


Fig. 15—Effect of Moisture on Density of Albany Molding Sand Rammed by Four Different Methods

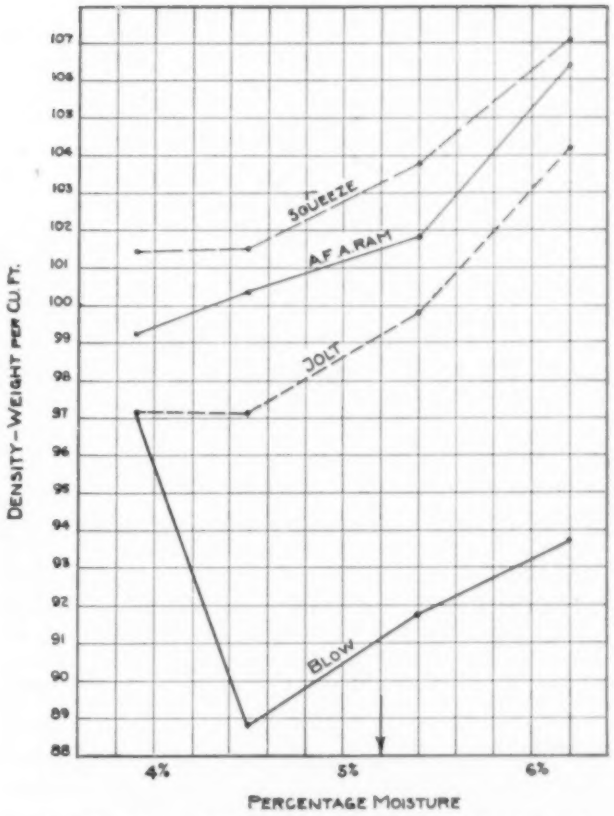


Fig. 16—Effect of Moisture on Density of Heap Sand Rammed by Four Different Methods

Fig. 17—Effect of Moisture on Density of AFA Test Sand Bonded with 4 Per Cent Bentonite, Rammed by Four Different Methods

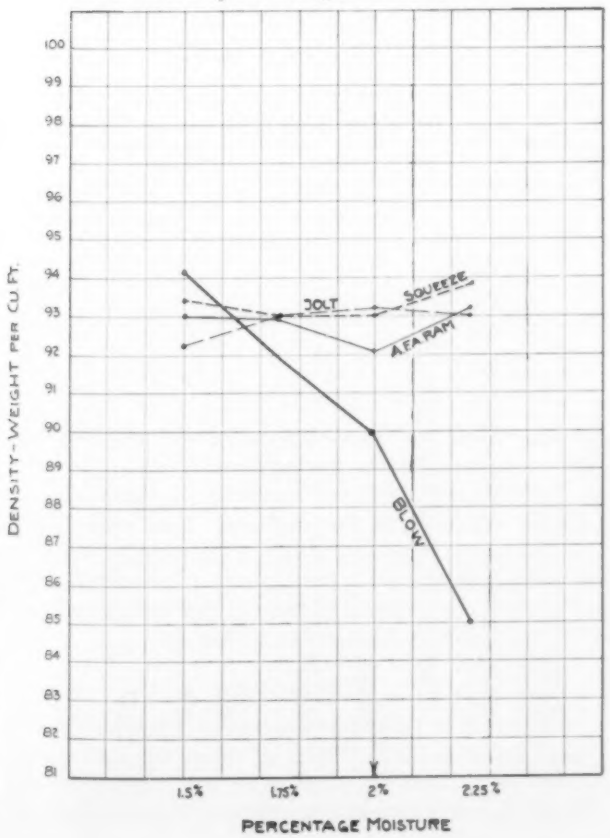
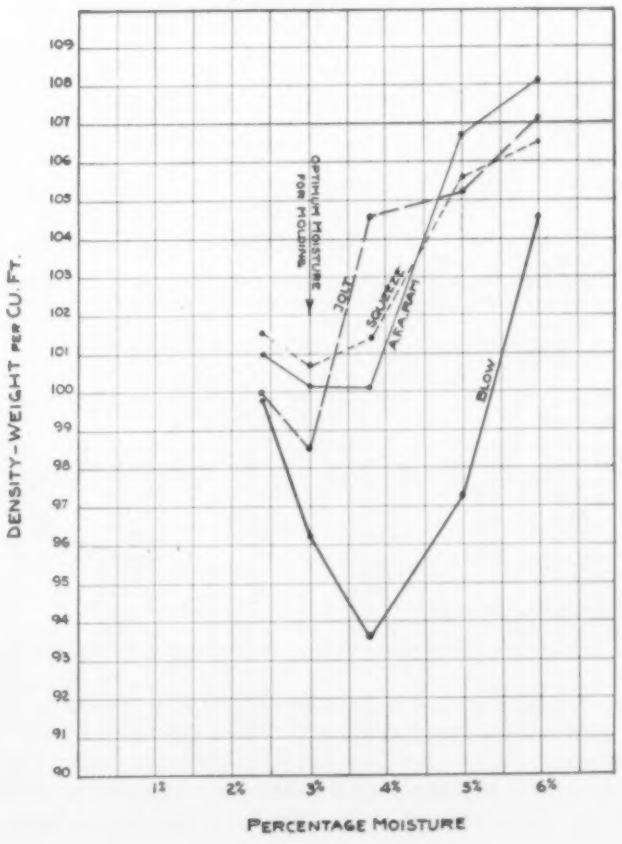


Fig. 18—Effect of Moisture on Density of AFA Test Sand Bonded with 12 Per Cent Fireclay, Rammed by Four Methods



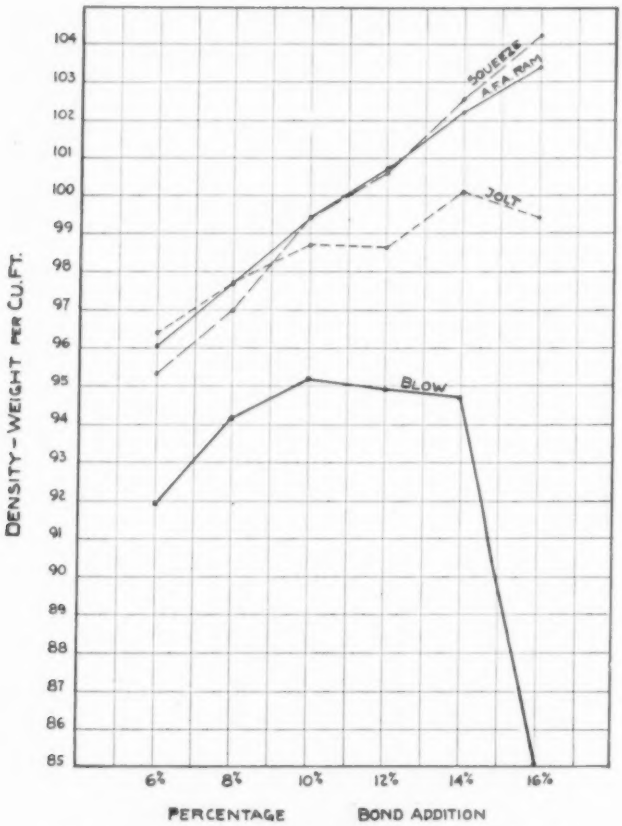


Fig. 19—Effect of Clay Content upon Density of Sand Rammed by Four Different Methods

TABLE 13.—EFFECT OF CLAY CONTENT AND RAMMING METHOD UPON SAND DENSITY

Percent Clay Addition	Moisture Percent	Density, Lb./Cu. Ft. A.F.A. Butt Ramming	Jolt	Squeeze	Blow
6	2.2	96.1	96.4	95.4	91.9
8	1.8	97.6	97.7	97.0	94.1
10	2.4	99.2	98.7	99.4	95.2
12	2.75	100.3	98.6	100.6	94.9
14	3.3	102.0	100.1	102.5	94.7
16	4.2	103.6	99.4	104.2	85.0
Maximum Change in Density		+7.5	+3.7	+8.8	−10.2

TABLE 14.—EFFECT OF GRAIN SIZE AND RAMMING METHOD UPON SAND DENSITY

Average Fineness Number	Moisture Percent	Density, Lb./Cu. Ft. A.F.A. Butt Ramming	Jolt	Squeeze	Blow
28.4	2.6	105.6	104.2	106.2	98.4
44.8	2.9	104.6	104.0	105.9	99.7
54.3	2.8	100.5	98.9	100.6	92.0
105.8	3.6	99.7	97.5	99.5	96.8
140.7	3.6	101.7	98.4	101.9	97.9
Maximum Change in Density		−5.9	−5.8	−6.7	−7.7

main physical properties of a sand, and thus they naturally would be expected to have an influence on how densely a sand would be packed by different methods of ramming. Batches of AFA standard testing sand bonded with 12 per cent clay and containing the various ingredients named above were prepared with the best workable moisture content for gray iron molds.

TABLE 15.—EFFECT OF VARIOUS INGREDIENTS ON DENSITY OF SAND RAMMED BY A.F.A. BUTT RAMMING

Ingredients	Clay, %	Moisture, %	Density, Lb./Cu. Ft.
33% Silica Flour	12	5.4	117.5
50% Silica Flour	12	6.1	114.7
25% Silica Flour	12	4.8	109.5
10% Silica Flour	12	4.0	105.0
1% Wood Sugar	12	2.7	102.2
1% Wood Flour	12	3.7	101.0
1% Pitch	12	2.84	101.0
None	12	2.7	100.3
6% Seacoal	12	3.3	98.8
1% Dextrine	12	1.8	95.7
1% Cereal Binder	12	2.0	95.6

TABLE 16.—EFFECT OF VARIOUS INGREDIENTS ON DENSITY OF SAND WHEN RAMMED BY SQUEEZING

Ingredients	Clay, %	Moisture, %	Density, Lb./Cu. Ft.
10% Silica Flour	12	4.0	101.0
None	12	2.7	100.3
1% Wood Flour	12	3.7	99.4
1% Pitch	12	2.84	99.3
1% Cereal Binder	12	2.0	99.2
1% Wood Sugar	12	2.7	98.2
6% Seacoal	12	3.3	95.9
1% Dextrine	12	1.8	94.2

TABLE 17.—EFFECT OF VARIOUS INGREDIENTS ON DENSITY OF SAND RAMMED BY JOLTING

Ingredients	Clay, %	Moisture, %	Density, Lb./Cu. Ft.
10% Silica Flour	12	4.0	106.4
1% Wood Sugar	12	2.7	102.7
1% Wood Flour	12	3.7	102.0
1% Pitch	12	2.84	101.4
None	12	2.7	100.3
6% Seacoal	12	3.3	98.8
1% Cereal Binder	12	2.0	95.6
1% Dextrine	12	1.8	95.7

TABLE 18.—EFFECT OF VARIOUS INGREDIENTS ON DENSITY OF 2x2-IN. SAND SPECIMEN RAMMED BY BLOWING

Ingredients	Clay, %	Moisture, %	Density, Lb./Cu. Ft.
None	12	2.7	100.3
1% Wood Flour	12	3.7	98.6
1% Pitch	12	2.84	98.3
10% Silica Flour	12	4.0	95.2
1% Wood Sugar	12	2.7	94.6
1% Dextrine	12	1.8	91.2
6% Seacoal	12	3.3	90.5
1% Cereal Binder	12	2.0	88.1

The effect of different ingredient additions on the density of the sand rammed with butt ramming obtained by three drops of the AFA rammer weight is tabulated in Table 15. In Table 16 is found tabulation of density for squeeze ramming, for jolt ramming in Table 17 and for blowing in Table 18.

Discussion

Further work will be required to supply more complete details on the subject of ramming methods and their effect upon the density of the compacted sand. From the work done to date, it is evident that sand con-

trol for the foundry should consider the characteristics of the sand being used and also the method of ramming.

If the foundryman desires to produce molds of constant properties, every mold should have the same hardness and density. If a consistent ramming procedure is used, control over the sand properties must be maintained closely enough to prevent wide variations in density.

The experiments reported in this paper indicate some of the factors which will affect the density of molds. Moisture, clay content, grain size and distribution, and additives have been found to affect density.

The exact nature of the relationship between the above variables and the density of the mold will vary with different sands and with different methods of ramming.

Conclusions

The following conclusions are based upon the limited amount of experimental evidence available at this date. Tests were made at or near the best workable moisture content of the sand. With sand on the dry or wet side, slightly different results might be obtained.

1. Butt, jolt and squeezing forms of ramming are less affected by a moisture change than blow ramming. Blown cores or molds require much closer moisture control than is necessary for other molding procedures.

2. Some sands, such as Albany and western bentonite bonded sands, show less density change in the rammed condition than do other clay bonded sands.

3. Jolt ramming is affected less by a change in clay content than butt or squeeze ramming.

4. The density of rammed sands increases with clay content when rammed by butt, jolt and squeeze methods.

5. The density of blown sands is affected by clay content. The test mixtures used in this investigation had the highest density when they contained 10 per cent clay.

6. As the grain size is decreased, density of rammed sand decreases when rammed by butt, jolt, squeeze and blow.

7. The blow method of ramming sands is more sensitive to a change in grain size than jolt, butt or squeeze ramming.

8. The addition of seacoal, dextrine and cereal binders to a clay bonded sand caused a reduction in the density of the sand when rammed by butt or jolt.

9. The density of blown sand was decreased by each one of the materials added to the base mixture.

It would seem therefore, from the foregoing experimental results that the control of sand properties for blowing and for jolting requires different considerations than when sand is prepared for butt ramming and for squeezing. If density is used as a measure of ramming efficiency, sand mixtures must be developed to suit each method of ramming.

Appendix A

Reproducibility of Mold Hardness Tests—A batch of the heap sand described in Table 8 was retempered and mixed for four minutes in a laboratory muller. The moisture content was 4.2 per cent and the flowability 75 per cent.

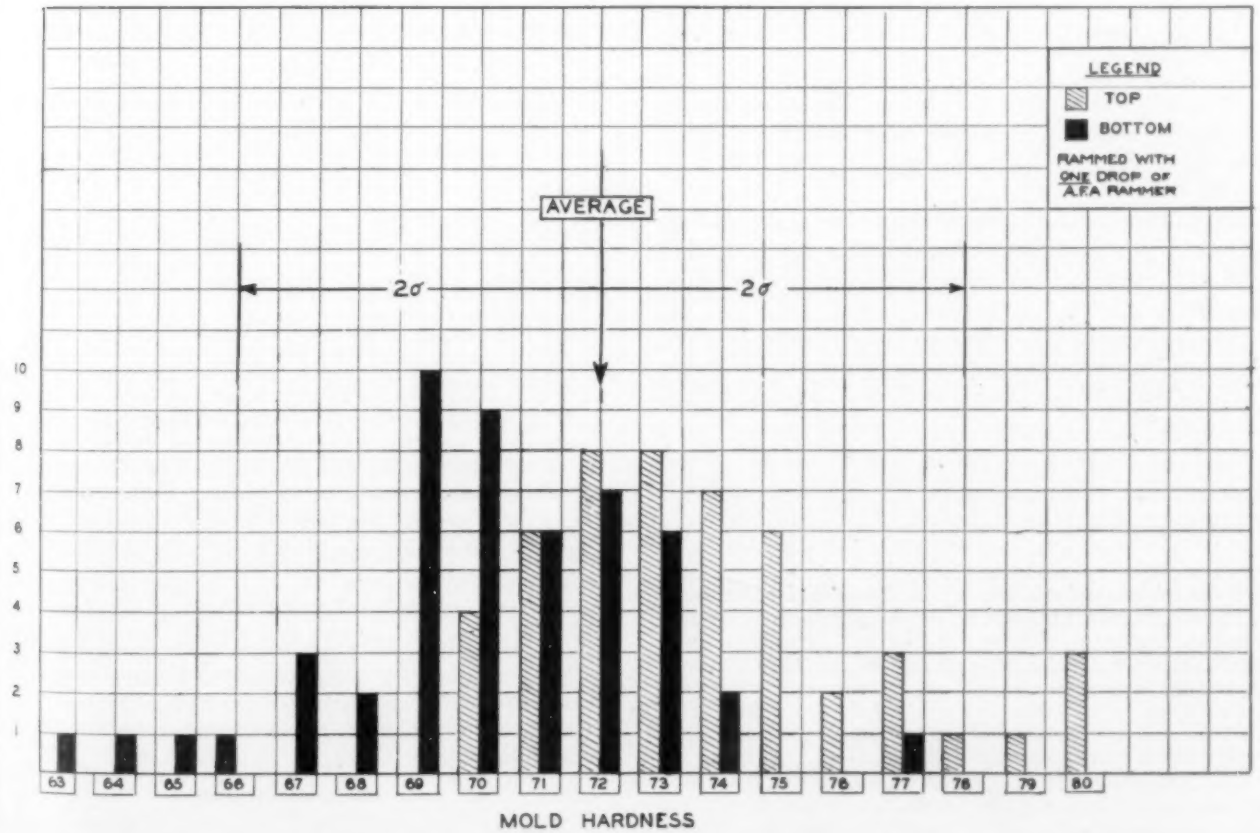


Fig. 20—Variation of Mold Hardness Tests on 2-in. Dia. Specimen Rammed with One Drop of Rammer

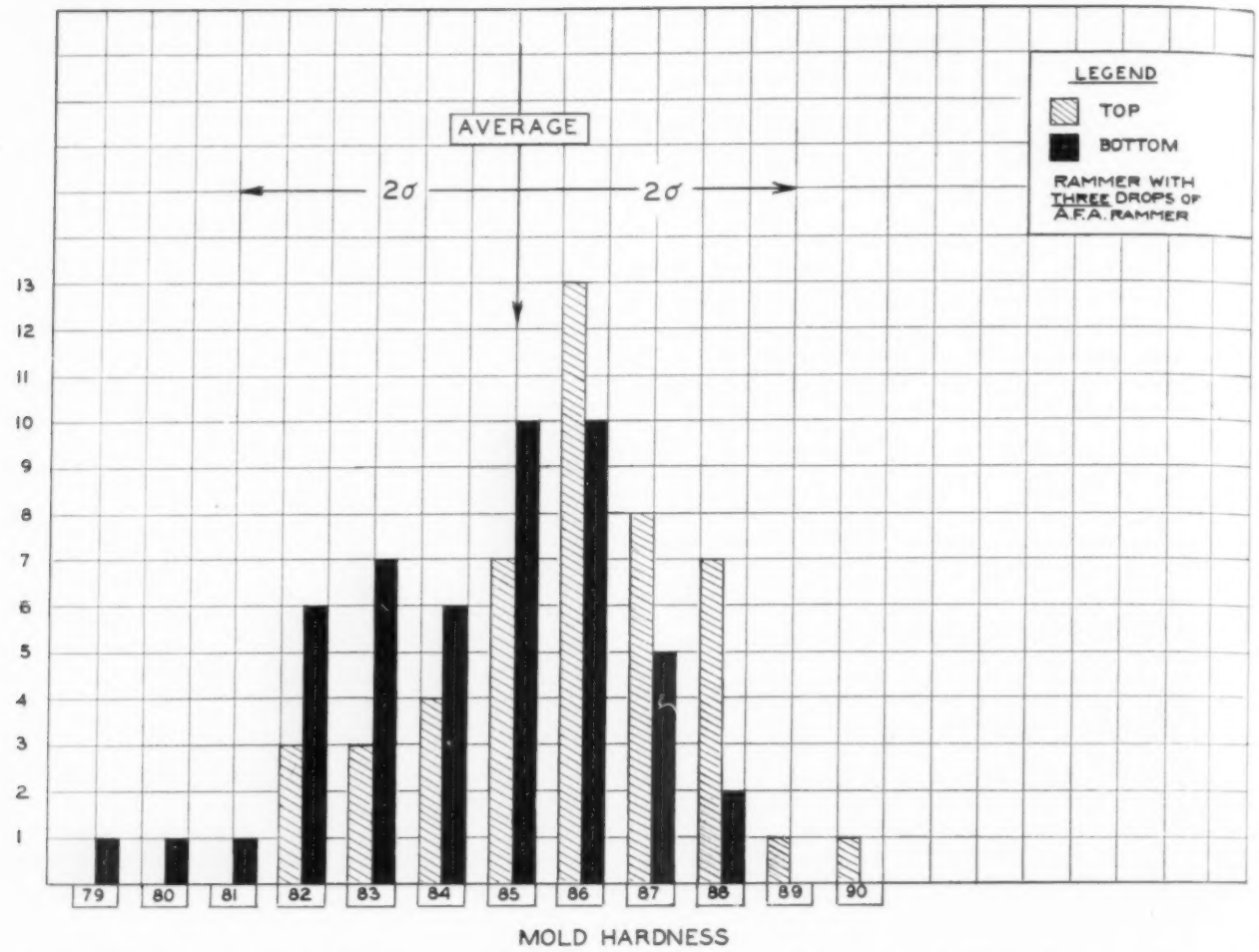


Fig. 21—Variation of Mold Hardness Tests on 2-in. Dia. Specimen Rammed with Three Drops of Rammer

Ten specimens were formed in a 2-in. specimen tube by *one drop* of the standard AFA rammer. Density measurements on the ten samples are shown in Table 19.

Ninety-nine mold hardness values were taken on the ten sand specimens. Five readings were taken on each end of the specimen. These observations are shown in Fig. 20.

From the same batch of sand another ten specimens were compacted using *three drops* of the standard AFA rammer. Density measurements obtained are shown in Table 20.

Mold hardness measurements made on the above specimens are shown in Fig. 21. The results of the foregoing experiments are summarized in Table 21.

TABLE 19.—DENSITY OF 10 ONE-DROP RAMMED SPECIMENS, LB/CU FT

101.3	101.1
100.9	100.5
100.8	100.0
101.5	100.9
100.9	100.8

Average = 100.87 lb/cu ft
Standard deviation = 0.4 lb/cu ft

TABLE 20.—DENSITY OF 10 THREE-DROP RAMMED SPECIMENS, LB/CU FT

107.5	107.8
108.0	108.4
108.5	108.5
108.6	108.5
108.2	108.4

Average = 108.24 lb/cu ft
Standard deviation = 0.18 lb/cu ft

TABLE 21.—STANDARD ERROR OF MOLD HARDNESS OBSERVATIONS

Density	No. of Rams 1	No. of Rams 3
Average Density, Lb/cu. ft.	100.87	108.24
No. of Observations	10	10
Standard Deviation, Lb/cu. ft.	0.4	0.18
Mold Hardness		
Average Mold Hardness	72	85
No. of Observations	99	99
Standard Deviation	6	4
Standard Error of Average of 10 readings	1.9	1.26

Some error is involved in that neither the mold nor the 2-in. specimen can be considered to be uniform in density. However, the mold hardness test is at present the only practical means of evaluating the density of rammed molds.

Appendix B

In order to present the information clearly, average figures were reported in many cases. Complete data for one experiment are recorded in Tables 22 and 23 to indicate the variation in observations obtained with different ramming methods.

Acknowledgments

The authors are indebted to Asst. Prof. Wm. Spindler of the foundry department of the University of Mich-

TABLE 22.—EFFECT OF MOISTURE AND RAMMING METHOD ON DENSITY OF ALBANY SAND

Moisture, Percent	Method of Ramming			
	A.F.A. Rammer	Jolt	Squeeze	Blow
	Density lb/cu. ft.	Density, lb/cu. ft.	Density, lb/cu. ft.	Density, lb/cu. ft.
5	102.5	100.6	102.6	97
	103.5	101.4	104.0	94
	105.0	100.4	104.8	93.4
	103.4	100.0	104.6	95.6
	103.6 Avg.	100.6 Avg.	104 Avg.	95 Avg.
5½	103.9	100.5	104.6	93.1
	102.6	101.3	102.2	96.5
	103.7	100.7	102.8	100.0
	103.4	100.7	102.8	96.0
	103.4 Avg.	100.8 Avg.	103.1 Avg.	96.4 Avg.
6	104.5	100.4	103.8	100.5
	104.2	100.4	103.9	101.5
	104.2	100.2	103.5	99.0
	103.1	100.6	104.8	98.2
	104 Avg.	100.4 Avg.	104 Avg.	99.8 Avg.
6½	104.9	100.7	105.2	96.4
	104.4	101.2	105.7	97.1
	104.9	100.6	103.3	98.8
	105.4	100.3	105.0	98.9
	104.9 Avg.	100.7 Avg.	104.8 Avg.	97.8 Avg.

igan for his assistance in the preparation of the sands used in this investigation, and to Ray Wormley, Foundry Superintendent of the United States Radiator Corporation, Detroit, for his assistance in making the test castings.

The design of the cylindrical test pattern for measuring the rate of metal solidification by draining a partly solidified casting was patterned after a design used by the Norfolk Navy Yards.

R. L. Doelman of Miller & Company, Chicago, suggested the wedge type test pattern and gave valuable assistance in carrying out the foundry tests.

DISCUSSION

Chairman: H. F. TAYLOR, Massachusetts Institute of Technology, Cambridge, Mass.

Co-Chairman: B. H. BOOTH, Carpenter Bros., Inc., Milwaukee, Wis.

D. C. WILLIAMS:[1] Do you have 165 grams on the density indicator dial in Fig. 4? Is it all based on 165 grams?

MR. DIETERT: No, that is not right. If we used 165 grams of sand in all cases, then we would end up with a specimen that would be too short in some cases, or too long in others. They would fall too far outside of the A.F.A. tolerance limit. Thus, we found it necessary to use two different weights of sand that would end up more closely to the length of two inches, as specified by the A.F.A. specifications.

MR. WILLIAMS: In using the four per cent Western bentonite mixture I find about 152 grams is required.

MR. DIETERT: You could use 165 grams and end up with a longer specimen. You would not use that specimen for the strength test or the permeability test.

MR. WILLIAMS: Then the permeability values and the green compression values you get which should correspond with the densities, would be different than the one I used for the specimen that weighed 154 grams. In other words, the physical properties on a 2 in.-long specimen would be different than the physical properties on a longer specimen.

MR. DIETERT: Technically speaking, that might be true. From a practical control viewpoint, the difference is so small that the variation in reading of densities by using this density indicator are not noticeable. We can use an exact 2-in. sand specimen and calculate the density by formula as given in the paper. We know the density then of the 2 x 2 in. A.F.A. specimen meets the A.F.A. tolerance length. Then we can use that same sample of sand and ram up a specimen using the density indicator and that specimen may not fall within the tolerance length specified by A.F.A. But we arrive at the same density within 2.0 lb. per cu. ft., which is within 2 per cent, which we feel is close enough for measuring density of foundry sands for foundry molding purposes.

TABLE 23.—VARIATION IN DENSITY OBSERVATIONS

Relative consistencies of density observations made on rammed, olted, squeezed and blown sand.

Moisture	Standard deviation for group of four observations			
	A.F.A. Rammer	Jolt	Squeeze	Blow
5%	.84	.51	.874	1.41
5½%	.495	.30	.90	2.44
6%	.534	.14	0.484	1.05
6½%	.35	.32	.90	1.08
Average Standard Deviation	.55	.32	.79	1.5

For the above series of observations on Albany sand, the least consistent results were obtained when the sand specimen was blown. Jolting gave the most uniform density.

MR. WILLIAMS: Do you give it 3 rams?

MR. DIETERT: We give it 3 rams if we want to use the A.F.A. method of ramming. Suppose that our mold was rammed to a hardness of 60. Then we want to find out the density of the sand at 60 hardness. Then we could ram our specimen to 60 hardness by adjusting the drops of the 14-lb. rammer weight.

There is another way of doing it. We could do it by calculation. We know the density of 80 hardness, which is 3 drops for some sands, and we can calculate then the density for a mold hardness of 60. So there are several ways of arriving at the answer, and you can cross check yourself.

MR. WILLIAMS: On page 177 there is a statement, "The density of the sand at the surface of the experimental mold was assumed to have the same correlation with mold hardness as the 2 x 2-in. specimen described in Table 4." What would lead you to make that assumption?

MR. DIETERT: The mold hardness readings on a 2 x 2-in. specimen were taken on the surface of a sand specimen which lies against a metal surface. This would give us, then, the skin hardness of a 2 x 2-in. A.F.A. specimen that was produced when the sand flows against a pattern. Thus, when we ram sand against a firm surface, and when we measure the mold hardness of a mold in the foundry, we are only measuring the skin hardness; we are not measuring the interior at all.

MR. WILLIAMS: Did you try that with your mold?

MR. DIETERT: Yes, we tried it with the mold hardness testing.

MR. WILLIAMS: Then, on page 178 in the second printed column, there is a statement regarding the conductivity. Is there any connection between Table 7 and Table 1? They are not the same mixtures at all, as I understand. How can you say that there is any connection between the thermal conductivity of just straight sand,—between the mixtures you have in Table 7 and Table 1? Do you have any basis for making that comparison?

MR. DIETERT: Yes, we do have a basis for making that comparison. In a previous investigation, we found that the heat absorption upon a molding sand was related to the density of the sand. Knowing the density of the sand, we could then interpret the amount of heat that would flow through the sand or the amount of heat that the sand would absorb per square foot of surface. If we have the same density in the new sand mixtures, A, B, C and D, to which you refer, then we can estimate the heat absorption of these particular sands.

MR. WILLIAMS: On page 179, the first paragraph, you state about the effect of permeability with sand D. I am wondering if it is not the effect of porosity with that sand rather than permeability.

MR. DIETERT: I think you are right in that statement. It was the porosity on the face of the mold into which the metal penetrated mechanically, so it was metal penetration, and permeability was just the spot control check indicating that this was a very porous sand.

[1] Ohio State University, Columbus, Ohio.

MR. WILLIAMS: On page 180, Conclusion 3, you state in the last sentence: "This causes the heat transfer to be greater than one would expect from the thermal conductivity of the sand." It seems to me that if the thermal conductivity of the sand is a figure, how would you expect it to be greater?

MR. DIETERT: Because you have metal solidification and you also have metal penetrating into the sand. Thus, you have two figures that someone might interpret was the amount of metal that was solidified due to the heat absorption in that sand, and the heat absorption of that metal into the sand had nothing to do with heat conductivity.

F. P. GOETTMAN:[2] It seems to me that this attachment is an unnecessary piece of equipment. The way we obtain the density figure, is to weigh the sand to make a 2-in. specimen. We make sure we get a 2-in. specimen and then the density is nothing more than multiplying by a factor. You have a 2-in. diameter core that has a 2-in. height and you know the grams weight of sand used. You can compute the density formula to one factor (0.606 × wght. of sand) and multiply that by the weight of sand used each time and get the density. In that manner the density is obtained on cores that you next run compression and permeability tests on, instead of taking one where you put in 165 grams or some other definite number of grams resulting in a specimen that is not 2 in. high.

It was said that it makes no difference. It may not. However, when it is so simple to take a density on the specimen that you are already using, I see no need to go into any other method and put on this additional work and then leave yourself open to the objection of having an off-size specimen. The fact of the matter is, if you want to study density after 5 rams, you can use the flowability indicator and from that obtain the height of specimen. In other words, you ram 3 times to get the 2-in. height and then you give it two more rams and on the fifth one you measure the flowability and from that flowability indicator, by calibration, you can measure the height of that specimen and compute the density from a table. I think the density should be computed on some method like that, so it is correlated with your other work and does not allow the objection that Mr. Williams introduced.

MR. DIETERT: Whenever possible, the human equation should be eliminated. In other words, we are trying to do one thing, and that is avoid having someone make an arithmetical error. All we ask him to do here is to copy down the figure that he reads from the dial indicator on the density. We have also given a method by which one can compute the density by a simple formula. We feel there are cases in a control laboratory where the operator should read the density figure direct from a dial and not compute it.

As for the specimen being at times outside of the A.F.A. narrow limits of length, we never use that particular specimen for our permeability or compression test, but we do know that the density indicator will be within the required tolerance, even though the specimen falls a short distance outside of the A.F.A. tolerance marks.

[2] Geo. F. Pettinos, Inc., Philadelphia.

SPECTROGRAPHIC ANALYSIS IN THE MANUFACTURE OF BRASS AND BRONZE INGOTS

By
G. E. Staahl and G. P. Halliwell *

ABSTRACT

The use of the spectrograph in the production of copper base foundry ingot is described. Among the applications discussed are the control of furnace production, the analysis of scrap and stock materials, the sorting of metals, the analysis of samples from outside sources as a customer service, and the use as a qualitative aid to the chemical department. Among the elements determined spectrographically are tin, lead, zinc, iron, nickel, antimony, aluminum, manganese, silicon, magnesium, beryllium, arsenic, and chromium. A description of the apparatus and sampling method used and the manner of preparing standard samples are included. Curves are given to show the relationship between conditions in the analytical gap and the length of preburn time. The analytical procedure is described and results are given for several of the alloy types run under routine conditions. A study of the data obtained on the 85-5-5-5 alloy shows that the accuracy expected is 3 per cent for the elements iron, nickel and antimony.

APPLICATION OF THE SPECTROGRAPH to foundry control analysis is well recognized in the ferrous and light metal industries and has been reported on previously in the TRANSACTIONS of the American Foundrymen's Association.[1, 2] Its application to the copper base and particularly the red metal alloys has not been as extensive probably for two reasons.

The first is the large number of different copper base alloys which are made today. Since the nature of the method of analysis is a comparative one, in that an unknown sample is measured in terms of a standard similar sample, the problem of preparing these standards and analytical curves becomes enormous.

The other reason is that the range of concentrations of the alloying constituents in copper base alloys may run from a mere trace to 40 per cent. Where the accuracy expected by the usual spectrographic methods of analysis would be acceptable for concentrations below two per cent, this accuracy is insufficient for control work at the higher ranges encountered in most of the alloys used for brasses and bronzes.

Another factor that has been found to influence the accuracy of results is the apparent effect that a slight variation in one of the major constituents has on the overall analysis. This type of influence has been found to be true in other than copper base alloys.[3, 4]

* H. Kramer & Co., Chicago.

Spectrographic Methods Developed

Some workers [3, 4] have developed systems of corrections to extend the useful range of the analytical curves, thus reducing the number of standards required. Others have applied the solution method [5, 6] to the analysis of higher concentrations or have used specially designed [7] controlled source units for greater accuracy.

It is not the purpose of this paper to make a critical study of a particular method of analysis but rather to show how the spectrograph has been successfully applied to the manufacture of brass and bronze ingot supplied to the foundry trade. An outline of the method of analysis used and the results of investigations of some variables involved are included. Results as obtained by the analytical procedure are given for several of the alloy types run by different operators.

At the present time six applications can be mentioned where the spectrograph is used either quantitatively or qualitatively to reduce the time required for the release of results or to make a greater number of analyses than was possible by chemical methods alone.

1. First in importance is the control of large reverberatory furnaces used in the smelting and refining processes. These furnaces have a capacity of about 120,000 lb per charge. As soon as the metal is molten, samples are taken at frequent intervals, in order to follow the progress of refining and to control additions of alloying metals to the furnace.

Rapid Analysis Promotes Control

A number of elements may be run on a single sample. Those most commonly determined in non-ferrous foundry alloys are tin, lead, zinc, iron, nickel, antimony, aluminum, silicon, manganese, arsenic, beryllium, magnesium, and chromium. Speed and accuracy are essential and results must be reliable and representative of the entire charge in the furnace.

The average time consumed in making quantitative determinations on six elements by spectrographic procedure usually is about 15 min. A corresponding

TABLE 1.—DISTRIBUTION OF ALLOYING ELEMENTS IN SPECTROGRAPHIC SAMPLES
(Chemical Analysis)

	Cu	Sn	Pb
Iron Mold Pin Tip	85.46	4.85	4.90
Iron Mold Pin Bottom	85.51	4.81	4.91
Iron Mold Head Drillings	85.60	4.77	4.89
Glass Tube Pin Tip	85.28	4.85	4.95
Glass Tube Pin Middle	85.26	4.86	4.99
Glass Tube Pin Bottom	85.23	4.83	4.94

chemical analysis would take an hour or more. The saving in time thus realized makes possible closer control on many elements contained in the various alloys.

Two important elements which are controlled almost exclusively by spectrographic methods in our copper-tin-lead bearing alloys are zinc and iron. The analysis of zinc particularly is a long and difficult procedure by wet chemical methods, and there are many alloys in which zinc must be kept below a certain maximum value. Spectrographic analysis of these alloys has resulted in a considerable saving both in operating costs and in the reduction of losses by oxidation of other metals such as tin and lead.

In this type of control work, furnace drift may be considerable for some elements as the refining processes proceed. For example, in making an 80-10-10 copper-tin-lead alloy, the first sample taken from the furnace may show from 2-3 per cent zinc, which must be reduced by refining usually to a 0.75 per cent maximum value. In this case, the analytical procedure is varied depending on the zinc content in order to secure the correct result. Several analytical line pairs may be used to cover a long range of concentration or if a number of suitable line pairs are not available, a filter is used to reduce the intensity of the useful ones.

Check Small Batch Production

2. The second use is in checking small furnace and pot production for minor elements and for the presence of unwanted impurities. Some alloys such as the high strength manganese bronze, aluminum bronze and hardeners are made in smaller heats depending upon the composition of the alloy and the requirements of the customer. A number of heats may be made of a single alloy in this manner. Each is analyzed spectrographically and the average results compared with the chemical analysis made on a composite in which each heat is equally represented. Heats that may be outside of specification limits or that might contain some impurity that would be injurious to the alloy or to its application can readily be identified, and adjusted in this manner.

It would be economically impractical for the chemical laboratory to run these heats individually and here the spectrograph has found an important application because of its speed and ability to handle a large volume of analysis with a minimum of manpower.

3. All scrap and stock materials including virgin metals are analyzed before they are used in the furnace. The spectrograph is used to determine the minor elements in amounts below 2 per cent, as previously mentioned. The chemical laboratory runs the major constituents—copper, tin and lead. In this way the production manager has a complete analysis of the material going into the furnace.

Sample Form Varies

The sample in this case may be in one of several forms. A piece of tubing, a large casting, or even grindings may be sent in for analysis. The technique employed will have to suit the size and shape of the sample. If at all possible a rod sample is preferred, but provision is made to handle large and odd shaped pieces.

4. The fourth use of the spectrograph is as a qualitative aid to the chemical department in the determination of those elements present in appreciable amounts in a sample to serve as a guide in their analysis. Many times samples are submitted of which little metallurgical history is known. A quick spectrographic analysis saves many hours in looking for elements which may or may not be present. It has also proved valuable in this respect to the research department in many of its investigations.

5. The spectrograph is also used to good advantage in sorting alloys that would be difficult to separate otherwise. For example, some condenser tubes contain about one per cent of either aluminum or tin, and their ultimate separation is highly desirable for their most economical use. It requires only a few minutes to obtain a spectrographic identification and from thence a sorting procedure can be based on accompanying physical characteristics.

6. Samples are frequently submitted by customers for chemical analysis. If the material is in such form that a pin may be machined from the casting, quantitative spectrographic determinations are made for minor elements or impurities. Otherwise semi-quantitative results are obtained which may be sufficient for the particular problem under consideration or may serve as a guide to a wet chemical analysis.

Apparatus Complete

The equipment used consists of a multisource unit, a large Littrow spectrograph, densitometer-comparator unit, calculating boards, an electrode shaper and other accessories. A small bench lathe is used to prepare surfaces on the samples to be sparked or arced. A high frequency induction furnace is available for preparing standard samples.

The darkroom is equipped with a developing machine of the rocking type, with the temperature controlled at 70 F. An electric drier is used to speed the drying of the photographic plates.

Both the spectrographic laboratory and the darkroom are air conditioned with the temperature maintained from 72 to 75 F and the relative humidity at

TABLE 2.—UNIFORMITY OF GLASS TUBE/ROD
(Spectro-Analysis)

Section No.	1	2	3	4	5	6	7
Nickel, per cent	0.63	0.63	0.65	0.65	0.62	0.68	0.63
Iron, per cent	0.24	0.26	0.25	0.25	0.25	0.25	0.25
Antimony, per cent	0.22	0.21	0.21	0.21	0.22	0.21	0.20

about 50 per cent. Air conditioning of the laboratory is necessary because large variations, particularly in the relative humidity, will cause the sensitivity of the photographic plates to change.

SAMPLING METHOD

When the spectrograph was installed, samples were cast in a split iron mold. Considerable difficulty was experienced in filling the mold completely and in securing good spectrographic pins. It was finally abandoned in favor of the pyrex glass tube now used in all our work. In this method metal is sucked up a pyrex glass tube to form a rod ¼ in. in diameter and 5 to 15 in. long. It was adapted as best suited to our needs for the following reasons:

1. The rods are representative of metal in large heats.
2. They give reproducible results.
3. Operation is simple.
4. Contamination from previous heats is eliminated.

Sample Uniformity Checked

Numerous tests were made to determine the uniformity of the chill cast specimen and also whether the sample taken in this manner would represent the furnace charge. Chemical analysis made on sections near the tip, middle and bottom of the pin were compared with the routine method of ingot sampling for chemical analysis. Table 1 shows some of the results.

Chemical analyses for the minor elements—iron, nickel and antimony—indicate that these elements are uniformly distributed throughout the entire casting.

Table 2 shows the results of spectrographic analyses made on a sample taken in a glass tubing and sparked in ¼ in. section along its entire length.

A number of investigators have claimed that the accuracy and reproducibility of spectrographic analyses are influenced by the grain size of the metal or alloy. Our experience indicates that pins taken from the same batch of metal in rapid succession by the glass tube method, where some are air cooled and others immediately quenched in water, show no noticeable difference in the accuracy or reproducibility of analysis.

The effect of pouring temperature on sampling was also investigated. A charge of 85-5-5-5 alloy was heated in a high frequency induction furnace to 2175 F. The metal was then allowed to cool as samples were taken in a glass tube at regular intervals. Results are shown in Table 3 and pouring temperature apparently has little effect on the composition of samples taken by the glass tube method.

Pin Size Established

Sample pins of various sizes have been tried ranging from ⅛ in. to ½ in. in diameter. Little difference is noticed in analytical results. However, if the pin is too small the spark tends to jump around the prepared surface and strike along the side of the pin or the pin may become excessively heated and give erroneous results. It is difficult to take samples in the foundry if the size of the glass tubing is greater than ¼ in. inside diameter.

From the above results and other similar tests, it was concluded that the samples as taken in the glass tubing were uniform in composition and representative of the charge. This method is used for all foundry work and has proved very satisfactory.

TABLE 3.—EFFECT OF POURING TEMPERATURE
(Spectro-Analysis)

Temperature, °F.	2175	2100	2050	2000	1950	1900	1875
Tin, per cent	5.25	5.35	5.30	5.25	5.15	5.10	5.20
Lead, per cent	5.10	5.00	5.10	5.05	4.95	4.85	5.05
Zinc, per cent	3.95	3.95	3.85	3.70	3.75	3.60	3.50
Nickel, per cent	0.76	0.75	0.76	0.77	0.76	0.76	0.77

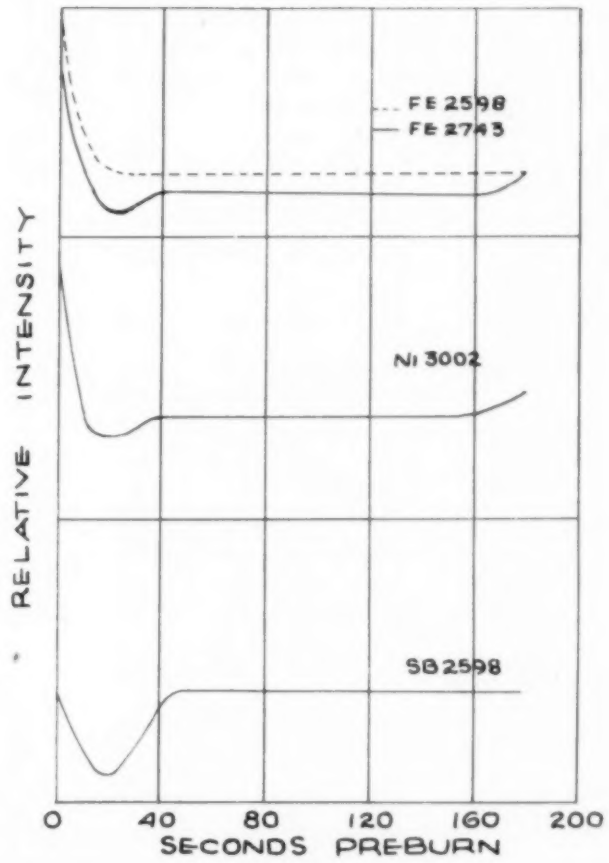


Fig. 1—Preburn curves.

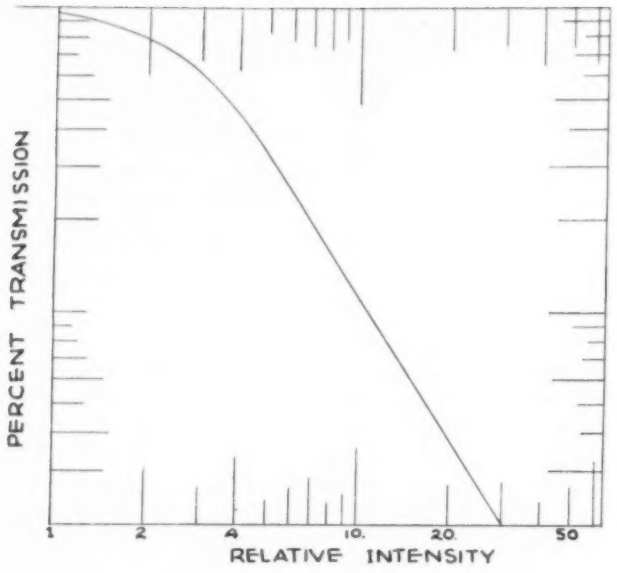
PREPARATION OF STANDARDS

Virgin metals are used in preparing standard samples and are melted in a high frequency furnace. These heats are remelted at least three times in order to secure uniform alloying of the various constituents.

In the preparation of standards to cover a particular range of concentrations two heats are made, one containing the maximum and the other the minimum amounts of the constituents to be determined. By proper mixing of these two heats the intermediate values are easily obtained.

Spectrographic samples are taken by pyrex glass tubes as previously mentioned. These samples are analyzed by the chemical laboratory using the best approved methods.

The section of the pin used for analytical work is the end furthest from the molten metal as the sample is taken in the foundry. The end immersed in the



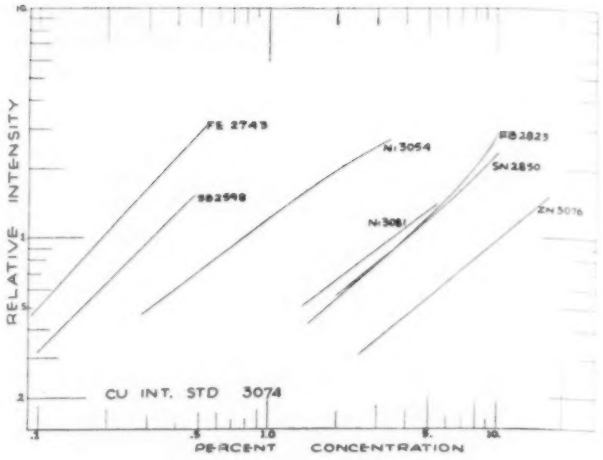


Fig. 2, left—Plate calibration curve.
Fig. 3, above—Analytical curves for 85-5-5-5 alloy.

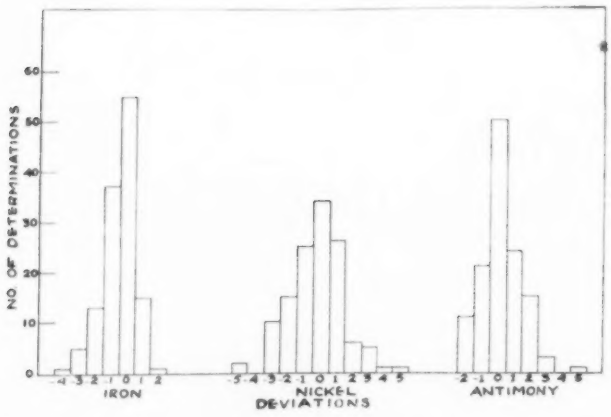


Fig. 4—Precision of iron, nickel, antimony determinations expressed in hundredths of a per cent deviation from chemical analysis. Average deviation, 4%.

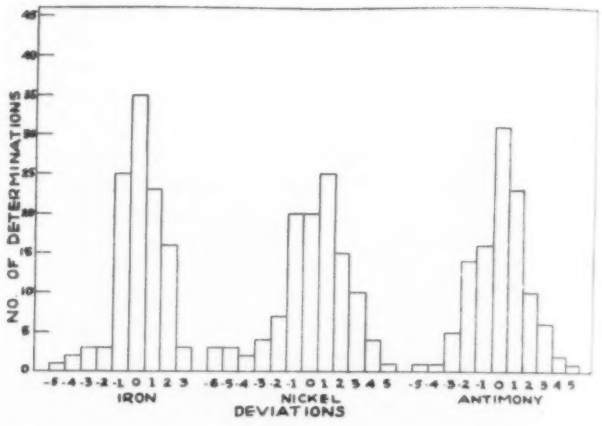


Fig. 5—Accuracy of iron, nickel, antimony determinations expressed in hundredths of a per cent deviation from chemical analysis. Average deviation, 3%.

TABLE 4.—OPERATING CONDITIONS

Source	A Spark	B Arc
Capacitance	2 Mf.	50 Mf.
Inductance	50 Mh.	480 Mh.
Resistance	Residual	40 Ohms
Upper Electrode	¼ in. Pins Machined Flat	¼ in. Pin—120°
Lower Electrode	¼ in. Graphite 120°	¼ in. Pin—120°
Preburn Time	30 Sec.	15 Sec.
Exposure	60 Sec.	5 Sec.
Spectrograph	Set for Region	2580-3580 A
Slit Width	35 Microns	35 Microns
Cap Width	3 mm.	3 mm.
Distance Source to Slit	60 cm.	60 cm.
Plates	S.A. No. 1	S.A. No. 1
	Development, D 19	3 min.
	5% Acetic Acid Solution	½ min.
	X-Ray Fixer	1 min.
	Wash	1 min.
	Dry	2 min.
	Total Processing Time	7½ min.

TABLE 5.—LINE PAIRS FOR RED BRASS ALLOYS

Int. Std. Line	Element Line	Range	Remarks
Cu 3074	Sn 2850	2.5 – 7.0	
Cu 3074	Pb 2823	2.5 – 7.0	
Cu 3074	Zn 3076	2.5 –10.0	Iron Interference [1]
Cu 3074	Fe 2743	0.08 – 0.50	
Cu 3074	Fe 2966	0.50 – 1.00	
Cu 3074	Ni 3051	0.10 – 0.50	
Cu 3074	Ni 3054	0.50 – 1.00	
Cu 3074	Sb 2598	0.08 – 0.80	Fe Interference [2]
Cu 3074	Si 2881	0.005– 0.05	
Cu 3074	Al 3092	0.005– 0.05	
Cu 3290	Zn 3303	2.0 –10.0	Used in Conjunction with Screen Filter [3]

[1] Fe Line at 3075.73 not resolved with a slit width of 35 microns.

[2] Fe Line at 2598.38 very sensitive and intense. Forms background when Fe greater than 0.20 per cent.

[3] To cut down intensity of Zn 3303 Line at concentration greater than 1.0 per cent, screen filter used at source.

TABLE 6.—REPRODUCIBILITY TESTS ON 85-5-5-5 ALLOY

Date	Iron, percent	Nickel, per cent	Antimony, per cent
10-24-44	0.15	0.49	0.18
10-26-44	0.15	0.49	0.19
10-28-44	0.16	0.50	0.17
10-31-44	0.15	0.50	0.17
11- 1-44	0.16	0.50	0.19
11- 3-44	0.16	0.50	0.19
11- 4-44	0.16	0.50	0.19
11- 7-44	0.16	0.48	0.18
11- 8-44	0.16	0.50	0.18
11-13-44	0.16	0.51	0.19
11-16-44	0.16	0.50	0.19
11-18-44	0.16	0.49	0.19
11-22-44	0.18	0.50	0.18
12- 7-44	0.16	0.48	0.16
12-15-44	0.16	0.50	0.18
12-23-44	0.16	0.51	0.18
1- 4-45	0.15	0.49	0.18
1- 8-45	0.15	0.48	0.17
Average Spectrographic Analysis	0.16	0.50	0.18
Original Chemical Analysis	0.16	0.50	0.16

TABLE 7.—REPRODUCIBILITY TEST FOR 80-10-10 ALLOY

Date	Fe, per cent	Ni, per cent	Sb, per cent	Zn, per cent
9-30-44	0.028	0.27	0.26	0.55
10-13-44	0.035	0.27	0.26	0.55
10-16-44	0.035	0.28	0.26	0.56
10-24-44	0.028	0.27	0.25	0.53
10-26-44	0.025	0.28	0.26	0.56
10-28-44	0.028	0.28	0.23	0.53
10-41-44	0.025	0.27	0.21	0.54
11- 1-44	0.027	0.28	0.24	0.54
11- 2-44	0.025	0.27	0.21	0.55
11- 3-44	0.023	0.27	0.22	0.56
11- 4-44	0.025	0.27	0.23	0.55
11- 7-44	0.027	0.27	0.23	0.53
11- 9-44	0.026	0.29	0.25	0.55
11-14-44	0.028	0.29	0.25	0.52
11-17-44	0.025	0.28	0.25	0.54
11-21-44	0.030	0.30	0.28	0.53
1- 1-45	—	0.27	0.27	0.53
Average Spectrographic Analysis	0.028	0.28	0.25	0.54
Original Chemical Analysis	0.03	0.28	0.26	0.55

metal usually becomes oxidized and deformed because of collapse of the glass tubing at the high temperature.

PREBURN TIME

It would be expected that in an alloy such as the 85-5-5-5, 80-10-10 or similar red brass where there are several constituents with varying melting and boiling points, equilibrium conditions would not be attained immediately in the analytical gap. This was found to be true as shown in Fig. 1.

Minimum Preburn 30 Seconds

In order to determine the minimum time necessary to secure equilibrium conditions for a given alloy, the spark is allowed to run continuously for a period of about 3 min. During this time, a series of exposures is taken with increasing pre-burn intervals. Curves as in Fig. 1 result when the computed relative intensities of the selected line pairs are plotted as a function of the preburn time. In routine work a minimum of 30 sec. preburn was found necessary for the analysis of the impurities.

TABLE 8.—COMPARATIVE RESULTS FOR 85-5-5-5 ALLOY

	Cu [1]	Sn	Pb	Zn [2]	Fe	Ni	Sb
S.A.	84.65	4.39	5.36	4.70	.19	.60	.16
C.A.	84.71	4.29	5.33	4.69	.20	.63	.15
	85.88	4.35	5.50	4.60	.18	.32	.17
	84.80	4.36	5.49	4.74	.17	.30	.14
	87.70	4.05	3.80	3.60	.28	.43	.14
	87.43	4.29	3.95	3.49		.42	.14
	88.64	4.30	3.70	2.70	.18	.45	.13
	88.62	4.20	3.75	2.67	.18	.45	.13
	86.21	3.90	4.45	4.65	.25	.39	.15
	86.57	3.63	4.39	4.73		.39	.14
	84.66	4.25	4.95	5.40	.17	.40	.17
	84.37	4.39	5.02	5.41	.17	.37	.17
	87.43	4.25	5.05	5.15	.23	.43	.16
	84.54	4.58	5.07	4.91	.22	.43	.15
	84.75	4.30	5.45	4.45	.22	.53	.20
	84.87	4.42	5.52	4.10	.20	.55	.21
	84.59	4.30	4.80	5.50	.23	.48	.10
	84.10	4.44	5.06	5.59	.24	.49	.08
	85.07	4.50	4.48	5.00	.19	.49	.17
	84.90	4.59	4.70	4.89	.18	.18	.16

[1] Spectrographic Analysis, Cu by Difference.
[2] Chemical Analysis, Zn by Difference.

TABLE 9.—COMPARATIVE RESULTS ON 80-10-10 ALLOY

	Fe, per cent	Ni, per cent	Sb, per cent	Zn, per cent
S.A.	0.010	0.12	0.10	0.37
C.A.	0.004	0.12	0.09	0.41
	0.01	0.23	0.11	0.62
	0.027	0.21	0.08	0.56
	0.02	0.28	0.12	0.56
	0.04	0.32	0.09	0.45
	0.02	0.32	0.14	1.10
	0.03	0.32	0.12	1.07
	0.01	0.19	0.13	0.21
	0.02	0.17	0.15	0.18
	0.06	0.45	0.25	1.45
		0.42	0.27	1.46
	0.03	0.28	0.26	0.77
		0.25	0.26	0.70

ANALYTICAL PROCEDURE

The sample as received in the laboratory is in the form of a pencil ¼ in. in diameter and about 5 in. long. A length of ¼ in. is cut from the upper end of the

TABLE 10.—COMPARATIVE RESULTS ON LOW STRENGTH MANGANESE BRONZES

	Sn	Pb	Fe	Al	Mn	Ni
S.A.	0.26	0.25	1.20	1.40	0.36	0.08
C.A.		0.22	1.19	1.14	0.31	0.09
	0.18	0.15	1.07	1.24	0.28	
	0.16	0.17	1.11	1.20	0.27	
	0.90	0.51	0.75	1.25	0.32	0.23
	0.95	0.60	0.78	1.11	0.33	0.30
		0.15	1.30	1.08	0.41	
		0.21	1.24	0.98	0.44	
	0.69	0.72	1.03	0.93	0.18	0.08
	0.69	0.61	1.12	1.20	0.28	0.10
	0.11	0.18	1.25	1.15	0.26	
	0.11	0.15	1.47	1.17	0.34	
	0.28	0.24	0.95	0.96	0.24	
	0.31	0.26	0.94	1.01	0.26	
	0.07	0.07	1.13	1.19	0.23	
	0.10	0.02	1.23	1.14	0.22	
	0.15	0.17	1.17	1.04	0.26	0.05
		0.17	1.21	1.03	0.28	

pin and discarded, and the same cut surface on the remaining piece is machined flat and as smooth as possible. The edges are rounded off slightly. It is then sparked with a counter electrode of special high purity spectroscopic graphite pointed to a 120° cone. It was found that by using a counter electrode of graphite the length of exposure necessary to secure the correct line densities on the photographic plate was considerably less than when two pins of the same material were used. A standard is included on each plate to check the analytical curves.

All samples from foundry production are run in duplicate, the pin being resurfaced on the lathe and a clean graphite rod used as the counter pin. The average of the two results is taken and reported directly to the foundry foreman. Care must be taken at all times that the pin is sound and has no central pipe, a condition which sometimes occurs when samples are not taken properly.

Technique for High Sensitivity

In some cases, where higher sensitivity is required, the sample is arced. Two sample pins are used with ends machined to a cone with a 120° included angle. The arc is also used in determining the antimony content when the iron present is over 0.25 per cent, due to the interference of the iron line at this concentration when the spark source is used. The nature of this interference is the formation of a variable background from the iron line in the neighborhood of the antimony line on the photographic plate.

The plate is then developed, the selected lines read on the densitometer, and the percentage transmission values converted directly into per cent concentrations by the use of specially constructed calculating boards. The analytical scales of the calculating boards are made by extending the curves of Fig. 3 on to a straight line, and arranging these scales on the board so that the "index points" lie on the same vertical line. The index point is that value of the concentration for a particular element for which the corresponding relative intensity has the value one or unity.

The plate characteristics are checked whenever a new batch of plates or when a new supply of developer are used. The standard sample sparked on each plate also is used as an indication of any changes in plate characteristics. A typical plate calibration curve is shown in Fig. 2. The complete conditions as used in the analytical procedure are given in Table 4.

The line pairs used for the red brass alloys are given in Table 5. Fig. 3 shows the analytical curves for 85-5-5-5 alloy as typical of this group.

Standard Sample Corrects Difficulty

One particularly annoying factor encountered in analytical work from time to time is the apparent shift of the analytical curves, even though all conditions are maintained as constant as possible. This necessitates the use of a standard sample on each plate which is about the only effective means of dealing with the difficulty. Other workers [8] have found this to be true and up to date no definite cause for the shift has been found. More investigations are planned to determine if there is a single cause or whether it is the result of a number

TABLE 11.—COMPARATIVE RESULTS ON HIGH STRENGTH MANGANESE BRONZE

	Fe, per cent	Al, per cent	Mn., per cent
S.A.	2.65	5.80	3.25
C.A.	2.68	5.76	3.18
	2.60	5.85	3.30
	2.82	6.07	3.40
	2.55	5.40	3.35
	2.75	5.85	3.27
	3.60	5.65	3.80
	3.55	5.60	3.80
	2.35	5.80	3.10
	2.27	5.75	3.40
	2.75	5.65	3.40
	2.73	5.80	3.36
	2.73	5.50	3.45
	2.82	5.60	3.51
	2.40	5.63	3.15
	2.32	5.63	3.09
	2.50	5.85	3.25
	2.48	5.71	3.24
	2.45	5.80	3.10
	2.26	5.70	3.18
	2.45	6.00	3.20
	2.14	5.77	3.14

of factors. This curve shift is not serious for concentrations below one per cent, but may be considerable for values from 1 to 10 per cent.

Results

Tables 6 and 7 give the results as determined on two samples over a period of months. They served as daily check standards and indicate the reproducibility of the method used since in this case no corrections were made for curve shifting which is sometimes found as mentioned above.

Check results as accumulated over a period of time are given in Tables 8, 9, 10, 11 and 12 for some of the alloys that were run both spectrographically and chemically. In each case the spectrographic values are shown above the corresponding chemical values.

Discussion

Figures 4 and 5 show in block diagram the precision and accuracy of the iron, nickel and antimony determinations for the 85-5-5-5 alloy. This alloy represents a major proportion of our production and considerable data are available for study. Deviations in both figures are expressed as the difference in results between chemical and spectrographic analysis on the same sample and are given in actual hundredths percentage values.

Figure 4 represents a total of 125 determinations made on the same sample over a period of months and on many different plates. The average deviation for this sample was $\pm$ 4 per cent of the amount present for the elements iron, nickel and antimony.

The data for Fig. 5 was taken from production records and includes about 110 different samples which were run by the spectrographic and chemical laboratories. Deviations are again expressed in hundredths of a per cent and the average deviation for each element is about $\pm$ 3 per cent. The average deviation is a little less than for the precision of the method, because corrections for the apparent index point shift are made in production work. Results for the higher concentrations, as shown in Tables 8, 9, 10, 11 and 12 in the range 1 to 10 per cent, usually show a deviation of about 3 per cent under routine conditions.

Conclusions

The spectrograph has been adapted to many of the processes involved in the manufacture of brass and bronze ingots and has proved to be invaluable in performing the tasks to which it has been applied. Closer control over many elements and savings both in time and operating costs have been realized.

The success of quantitative analyses may be ascribed to several factors.

1. Strict control over all external conditions affecting the analytical procedure, such as air conditioning, temperature control of developing solutions, voltage regulation of the source unit and densitometer, and sampling method.
2. The use of carefully analyzed standards for each alloy.
3. Frequent checks on analytical curves and plate characteristics by the use of standards.
4. Recognition of limitation of the method.

TABLE 12.—COMPARATIVE RESULTS ON ALUMINUM BRONZES

	Fe, per cent	Al, per cent	Zn, per cent	Mn, per cent	Ni, per cent
S.A.	4.20	10.10	0.15	0.30	2.25
C.A.	4.13	10.11	0.10	0.34	2.28
	4.00	9.63		0.28	2.10
	3.92	9.43		0.31	1.92
	4.03	9.55	0.17	0.27	2.00
	4.36	9.25	0.17	0.27	1.92
	4.05	10.20	0.16	0.27	2.00
	4.03	10.38	0.16	0.27	1.85
	3.80	10.90		0.46	0.57
	3.83	10.88		0.44	0.56
	3.95	9.30	0.18	0.24	2.05
	3.87	9.09	0.20	0.25	2.03
	2.65	10.70			
	2.81	10.50			
	3.92	9.80		0.39	2.06
	3.94	10.00		0.36	2.12
	3.95	9.90			3.40
	4.17	9.96			3.38
	3.80	11.10	0.09	0.04	0.09
	3.93	11.23			

Acknowledgments

The success of the quantitative work is due in great measure to the many and careful chemical analyses made in investigational work and in preparation of the standard samples. The cooperation of A. B. Shapiro, chief chemist, and members of his staff is greatly appreciated.

Acknowledgment is also given Miss Ruth Orensten for assistance in collecting data and to H. Kramer and Company for permission to publish this paper.

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DISCUSSION

Chairman: H. M. ST. JOHN, Crane Co., Chicago

Co-Chairman: A. K. HIGGINS, Allis-Chalmers Mfg. Co., Milwaukee

W. W. EDENS *(written discussion)*[1]: The authors of this paper are to be complimented for bringing to the A.F.A. Brass and Bronze Division membership this excellent paper on Spectrographic Analysis in the Manufacture of Brass and Bronze Ingot. The use of this analytical tool in the manufacture of ingot brings to us as operating brass and bronze foundrymen not only a better controlled raw material for our use, but also points the way toward better analytical control in our own operations for the improvement of our products and of the industry as a whole.

During the war our company operated a spectrograph as an integral part of an aluminum bronze boring reclamation plant for the purpose of converting our own borings which were carefully segregated and returned to ingot form for reuse in our foundries. The conservation program then in effect on copper and aluminum made this operation not only desirable but necessary, and it proved to be economically sound in the conversion of millions of pounds of aluminum bronze borings.

The equipment was all Dietert-ARL consisting of the grating spectrograph, multi-source unit, comparator-densitometer along with film developing and drying equipment, calculating board, etc.

Initially we had considerable difficulty which was traced to poor voltage regulation. This was not the fault of the multi-source unit but rather a wide variation in voltage coming into our plant largely due to a very heavy variable demand on the feeder line coming into our immediate industrial area. To overcome this it was necessary to install a motor generator set ahead of the multi-source unit. This connection plus the air conditioning of the spectrographic laboratory corrected our major difficulties.

The sample was a cast slug poured in a chill open face mold measuring 2¼ in. diameter and ¾ in. deep. The drag side of the slug was machined off to clean metal and was sparked on a petrey spark stand with a high purity graphite electrode using a 3 mm. gap. Spark conditions of excitation were as follows for the major elements (copper, aluminum, iron, nickel, manganese and silicon)

5 mfd. capacitance
75 L inductance
10 ohms resistance
10 sec. preburn
40 sec. exposure
Phasing switch—180°
Rotary gap pointer—90°
Discharge voltage—940V, power circuit
Discharge amperage—2A power circuit
Grating 8-8

No difficulty was experienced with this type of sample on the aluminum bronze alloys when properly cast except for some of those in the high alloy range (15 per cent Al. 5 per cent Fe. Bal. Cu) where lack of uniformity presented some problems. Attempts to use this type of sample on other alloys especially the red brasses gave considerable variation in results. It seems entirely probable that the narrow solidification range of the aluminum bronzes along with chill cast sample preparation gave a homogeniety entirely acceptable whereas the red brasses with their comparatively long solidification range showed a lack of homogeniety which gave variable analytical results. Brief experimental work indicated that the pencil technique practiced by the authors was far superior for the red metals.

The following wave length lines were used in the analysis of aluminum bronze:

Copper	— 2997A — 3307.9A
Aluminum	— 2652A
Iron	— 2831.6A — 3050.9A — 2733.5A
Nickel	— 3050.8A — 2992.5A — 3414-A
Manganese	— 2933A — 2922.5A
Silicon	— 2881.59A
Tin	— 3034.12A — 2839.9A
Lead	— 2833A
Zinc	— 2608.6A — 3076A

The specific line pairs used varied with the particular alloy as related to the alloying amounts present.

A check period covering upwards of 50 analysis for each alloy showed the following deviations from chemical laboratory analysis taken from drilling of the spectrographic sample:

		Deviation, per cent Greatest	Deviation, per cent Average Deviation	Average Analysis, per cent	Error, per cent (Av. Deviation)
(ALLOY A)	Cu	0.31	0.11	89.33	0.12
	Al	0.25	0.10	9.83	1.0
	Fe	0.12	0.04	0.70	5.5
(ALLOY B)	Cu	0.34	0.13	85.46	0.15
	Al	0.22	0.11	10.64	1.00
	Fe	0.27	0.10	3.55	2.8
	Ni	0.06	0.03	0.36	8.3
(ALLOY C)	Cu	0.28	0.15	81.47	0.18
	Al	0.30	0.16	10.11	1.5
	Fe	0.18	0.08	2.69	3.0
	Ni	0.29	0.12	4.76	2.5
	Mn	0.13	0.04	0.90	4.5

These results were determined before the air conditioning installation was complete, and further improvement was made subsequent thereto. This plus greater standardization of practice, both in sample preparation and in spectographic and dark room techniques, gave us a very satisfactory analytical control in the reclamation of segregated aluminum bronze borings.

The conclusions expressed by the authors regarding the prerequisites of successful quantative analysis through the use of the spectrograph are heartily ascribed to. Attention to detail and complete standardization will bring success. Again I want to congratulate the authors on an excellent paper and thank them for a worthwhile contribution to the Brass and Bronze literature.

J. F. EDNIE[2]: After reading this excellent paper and the discussion it has instigated, one wonders whether a word of caution is not in order. I have been familiar with spectrographic procedures for the past 15 years or more, having operated an instrument back in 1930 when there were only about 3 or 4 spectrographs in all of Pittsburgh. Since that time phenomenal advancements have been made, until today practically every modern, well-equipped laboratory has a spectrograph. It has been successfully applied to more and more commodities with tremendous savings of time and money. Messrs. Staahl and Halliwell have just shown that the spectrograph is successfully invading the realm of foundry alloys in the control of composition and impurities in the metal. But my main point is whether we are ready to suggest that the average foundry should own a spectrograph. I do not believe the authors would make such a recommendation,—even in spite of the success they have had.

We have had a spectrograph at Duquesne Smelting for the past couple of years and we do not hesitate to confess that we are not far along with its application to the various bronzes as the authors indicate. There are many problems. The fact that we have different equipment than the H. Kramer Company has prevented us from adopting the procedures used by the authors in our laboratory. At this time, we would like to thank the authors, as well as Mr. Edens, for the fine cooperation they have given us in the past, but you simply cannot take their methods and put them into your own laboratory and be assured of duplicate results. I think the average foundryman ought to know the other side of the picture. Between cost of equipment and air conditioning including temperature and humidity control, and manufacture of standards our company has spent well over $100,000 so far, and we are not as well advanced as the authors. We will get there but it takes time and money. I think the average foundry would be better off to delay spending this kind of money until more of the problems have been worked out and the methods become more standardized.

W. J. EDGAR *(written discussion)*[3]: In view of the adverse criticism brought up after the presentation of this paper by Mr. G. P. Halliwell, I would like to pass along a few remarks which might shed a little more light on this subject.

First of all, one of the most important steps is the method of sampling. We, at Federal-Mogul Corp., have found that the use of chill cast pins of the alloy to be most satisfactory and we make

[1] Badger Brass & Aluminum Foundry Co., Milwaukee, formerly with Ampco Metal Co.

[2] Duquesne Smelting Corporation, Pittsburgh.

[3] Federal Mogul Corp., Detroit.

them with a suitable hot top to make a sound structure. In this, I believe, we do practically the same as H. Kramer & Co. In order to insure good results, the method of casting should be as rigidly standardized as possible. A fast smooth pouring of the metal with standardized melt and mold temperatures should give a good set of pins. Many types of mold dressing may be used but the one we choose is a mixture of lard oil and graphite made up to a fairly heavy consistency. This is brushed on and the excess blown off. Sometimes just heavy fuel oil is used which may require a little different technique but the end result is the same. With a little practice most people can pour a sample in which there is no necking at the junction of the pin and hot top or cold shuts in the pin. With a smooth set of pins, free from the defects mentioned above, yet given a good chill casting condition, there is little reason for not making them work satisfactorily.

The source conditions are also very important. The writer believes that this fact has frequently been overlooked or not given sufficient attention. Should the conditions be wrong for the work being done, inaccurate and erratic results are certain to follow.

The types of materials that we are able to analyze are shown in Table A.

Our precision or accuracy for the above is about 2 per cent of the amount present for tin and zinc, the percentage being a little higher for the low amounts of zinc. For lead in the low percentages the precision is about 2 per cent but for the high leads this may, at times, run to 5 per cent. Usually we are able to hold the precision to about 3 per cent or less. This factor is largely dependent on the procedure in making the control samples. If proper care is taken, we could make an average of 2 per cent or less. Our precision is about 5 per cent on the phosphorus. All of the above percentages are, of course, based on the quantity present.

We seem to have trouble in determining the leads on types SAE No. 40 and 66 at present and are endeavoring to find how to get around it. It seems that these alloys are very sensitive to casting conditions for the determination of lead.

TABLE A—Materials Analyzed

Alloy	Element, per cent				
	Copper	Tin	Lead	Zinc	Phosphorus
SAE #40 Leaded Red Brass	85	5	5	5	
SAE #62 Gun Metal	88	10	.3–	2	
SAE #63 Leaded Gun Metal	88	10	1.5	..	
SAE #64 Phosphor Bronze	80	10	10	..	.25–*
SAE #65 Phosphor Gear Bronze	89	11	.5–	.5–	.1–.3
SAE #66 Bronze Bearing	85	5	9	1	
SAE #660 Bronze Bearing	83	7	7	3	

* Not determined in this alloy.

MR. STAAHL *(author's closure):* A word of caution should be given the uninitiated concerning the indiscriminate use of the spectrograph. Care must be taken throughout the entire procedure. As stated in the paper, all conditions affecting the quantitative work must be controlled and maintained as constant as possible. In addition, working curves, plate contrast, source conditions, etc., must be checked regularly. Neglect of detail in this respect has been a source of error on the part of some people who have tried to use the spectrograph in control work.

However, many laboratories are using spectrographic methods successfully and some are claiming a greater accuracy than we do. The range of concentrations covered has been extended by some workers to run constituents up to 20 per cent of the amount present and higher, and consistent results are obtained in these higher ranges. The correct equipment and technique should bring the desired results. These are the problems that the individual laboratory must work out for its own particular needs.

FOUNDRY TRAINING COURSE FOR COLLEGE GRADUATES

Report of the A.F.A. Subcommittee on
Training Graduate Engineers in Industry
Educational Division

INDUSTRIAL FOUNDRY TRAINING of college graduates is receiving increasing attention as more and more foundries recognize the contributions graduates can make to the industry. In addition, college graduates look to the castings industry for a career because they are beginning to recognize the opportunities available and the absence of competition in this field.

During the war years, most engineering graduates were in the armed services. Consequently, it was difficult to secure and train the young technical talent which the castings industry urgently needs now. The technical staffs of foundries which formerly used many college graduates, and the personnel of foundries planning to use graduates for the first time, will have to be built up of young men who are now being graduated. Some of these men had some industrial foundry experience during the war, and many of them are veterans who learned of the importance of the castings industry through its war efforts.

Graduates Need Training

Some of these graduates are interested in foundry work and expect the industry to give them industrial training to fit them to take their place in an important basic industry. It is important that the castings industry recognize, as has many other industries, that college graduates need industrial training, as well as desire it. A college graduate, like a graduate of any other course in formal education, has merely been prepared to absorb information and ideas and correlate them with known information rapidly and thoroughly. If properly educated in college and if he has the proper outlook, a college graduate can contribute much to an industrial enterprise, after he has become acclimated to industrial life and practices.

Opportunities in the castings industry for the college graduate have been discussed previously in publications of the American Foundrymen's Association. The value of a college graduate to a foundry has also been brought out. Both of these subjects were covered particularly well in an article entitled "College Graduates and the Casting Industry" which appeared in the November, 1946, issue of AMERICAN FOUNDRYMAN. Reprints of this article are available to graduates, foundry management, personnel managers and to all who are interested.

Combine Experience and Education

The function of a foundry training course for college graduates is to effect a combination of industrial foundry experience and college education which will make a man more valuable to a plant than if he had only one of these two types of training. The possibility of giving a college education to a practical foundryman should be considered, as well as the reverse procedure of making a foundryman out of a college graduate.

Wisconsin Cooperative Plan

Complete college training of foundry apprentices is being carried on by the University of Wisconsin in cooperation with the Wisconsin Chapter of A.F.A. Under this plan, several foundry apprentices, who are interested and able to meet college entrance requirements, are selected each year from Wisconsin foundries. These apprentices attend the University full-time, take a course in metallurgy or engineering, and earn part of their expenses by serving as laboratory assistants in the foundry and metallurgical laboratories. Such a plan is well worth consideration by other chapters and by the larger foundries.

A.F.A. Educational Division

The foundry training course for college graduates outlined later, and the suggestions regarding its establishment and execution, were prepared by the Training of Graduate Engineers within Industry Subcommittee of the former Committee on Cooperation with Engineering Schools. The functions of these committees have been assumed by others in the newest A.F.A. division, the Educational Division. The membership of the subcommittee included Chairman A. W. Gregg, Executive Engineer, Equipment Division, Whiting Corporation, Harvey Ill.; G. J. Barker, Chairman, Metallurgy Department, University of Wisconsin, Madison; H. Bornstein, Director, Testing & Research Laboratories, Deere & Company, Moline, Ill.; and C. V. Nass, Vice President and Manager, Foundry Division, Pettibone Mulliken Corp., Chicago.

Training Course Varied

A recommended course of training for the college graduate in the foundry must of necessity be quite general because no schedule will be applicable to all conditions and all foundries. Therefore, foundry management planning such a course should have definitely in mind the positions for which these young men are being trained.

The program suggested must be varied somewhat to fit a man's college training and the needs of the foundry. The course should be open to graduates of recognized colleges and universities. It might be preferable to confine the course to engineering school graduates although graduates holding degrees other than engineering or metallurgy might well be considered for certain positions.

In planning the course, it must be kept in mind that the college graduate is not being trained to be a mechanic. Nevertheless, he must secure sufficient knowledge of all branches of foundry work to be able to understand them and correlate them. If he has the ability and patience, a properly trained college graduate may secure a top position such as superintendent, works manager, sales manager or chief metallurgist.

Two-Year Course Needed

Nothing less than a well planned 18 months course, and preferably two years, will provide sufficient time for proper training. Perhaps eighteen months of this time should be spent adhering to a rather definite schedule, and the last six months spent on special assignments under the supervision of the head of the department to which the man will be assigned on completion of his training period.

It is highly important, no matter what course of training is adopted, that the prospective trainee understand exactly what his program will be. If the company has a training supervisor, he is the man to whom the graduate should be responsible. The supervisor should see that the man is moved from department to department according to schedule.

If the company has no training supervisor, someone in authority, such as the works manager or the general superintendent, should assume this responsibility. The supervisor of graduates should be a man of broad experience with a genuine liking and sympathy for young men. He should be a kind of father confessor to whom the young man will feel free to confer concerning any problem or trouble which may be encountered.

Hold Weekly Meetings

A weekly meeting of graduates with the supervisor is a valuable means of checking each man's progress and understanding of the work of his department. Each graduate should be required to submit a written report as he completes his time in a department, describing what he has learned, and making observations and suggestions about the operations in the department.

Graduates should be encouraged to join the American Foundrymen's Association and to attend chapter meetings. Most foundries hold weekly foremen's meetings in which current problems of the foundry are discussed by supervisors and management. During the latter part of his training, it is well to have the graduate attend these meetings.

The wage paid to the college graduate will be variable because conditions vary from one locality to another, and from year to year. However, the wage should be sufficiently attractive to interest the graduate and he should be given an encouraging increase in pay every six months. He should also be given a salary increase upon completion of the training course.

Orientation Period Needed

When the man first reports for duty he should be introduced to foremen and executives of the company, and should be given two or three days (or possibly a week) to observe the various operations throughout the plant. This will enable him to get used to an industrial plant and to get an over-all picture of operations. After this brief period of orientation, the trainee should be started on a prescribed course. An outline of a course for training college graduates in the castings industry follows.

Industrial Training Outline

Pattern Shop—Five Weeks. The graduate cannot be expected to make patterns and will be of little value as a helper. However, a great deal can be learned by watching a skilled pattern maker, and by studying the rigging which is developed in this department. The pattern shop is often the planning department of the modern foundry.

The graduate can be asked to do design work and estimate costs in pattern making. He should spend considerable time checking patterns in and out of pattern storage and to and from the foundry. Patterns are often abused in the foundry and the engineering apprentice should note the condition in which patterns are returned to storage. He should note also how the condition of patterns affects production and the number of scrapped castings.

Molding Department—Fifteen Weeks. The graduate should have experience with floor molding, bench molding, machine molding, molding sands, sand conditioning, sand testing, etc. He should help molders making heavy floor work, and should make some small simple molds on a bench and on a molding machine. If the molding department is mechanized, he should be moved from one job to another in this department. He should also get some experience in pouring.

Core Room—Ten Weeks. This department is very important in some foundries and relatively unimportant in others. The time spent in the core room should be regulated accordingly. The graduate can be trained in a short time to make small cores on a bench. If the foundry uses large and intricate cores, he should spend time helping a core maker on this class of work.

The trainee should study core sands and binders, mixing of core sand, and the composition of various core mixtures. The time and temperature required to bake cores made with different binders should be studied as well as the design and operation of the ovens, core blowers, etc.

Finishing Department—Five Weeks. The trainee should not be used as a chipper, grinder, welder, flame

cutter, etc., but should study these operations sufficiently to become thoroughly familiar with them. He should pay *especial attention* to castings which are scrapped and study thoroughly the reasons for rejection. If the inspection of castings is under a separate department, the apprentice should be assigned to the inspection department for at least five weeks.

Melting Department—Fifteen Weeks. The graduate can be useful in this department in many ways, and it is important that he become familiar with all the operations and the equipment and furnaces. He should make a thorough study of the theory and practice involved and of the metallurgical reactions in the melting units. He can assist in making up and weighing charges and in repairing the refractory linings of the melting units. He should familiarize himself with the various refractories, the results obtained with various fuels and the handling of alloying materials.

Laboratory and Testing Departments—Fifteen Weeks. In these departments, the graduate is generally on familiar ground. Here his time should be divided between the chemical, the mechanical testing, the sand control, the pyrometry, the x-ray and the metallurgical laboratories.

Cost Experience Essential

Cost and Estimating Departments—Ten Weeks. The trainee who is expected to fill an executive position at some future time must have an idea of the cost of operating all departments. In addition, a thorough understanding of the cost system employed is essential. Ten weeks spent in the cost and estimating departments will be of some value in acquainting the graduate with a phase of business generally neglected in engineering curricula.

Specialized Training—Six Months. After spending approximately 18 months in the various foundry departments the graduate should be familiar with all operations of the foundry, and should have a well-founded opinion regarding which part of the business attracts him most. In addition, the plant management should have become acquainted with the man's aptitude and ability.

The final six months should be spent in specializing in the work in the department in which the man is most interested and in which he will be expected to work after training. If he is particularly interested in planning and general operations, the balance of his time should be spent under the works manager.

For a graduate interested in manufacturing, some time should be allotted to the plant maintenance department which represents an important item in the overhead cost of making castings.

In case a graduate is interested in engineering, he should be assigned to the engineering department under the supervision of the plant engineer. This department often has charge of maintenance and is responsible for the purchase of new equipment, building extensions, etc.

Many college graduates will prefer to get into sales work. If so, they should be assigned to the sales manager, who will train them in sales work. A special course in salesmanship would be advisable.

Those who prefer metallurgical and laboratory work should be assigned to the chief metallurgist. In general, however, laboratory work in the last six months should be discouraged as a means for developing foundry engineers.

Typical Training Program

A typical good training program for college graduates in the castings industry is shown in the course of the Falk Corporation of Milwaukee. This program requires eighteen months (3,120 hours), and the schedule is as follows:

	Time	
Assignment	Hr.	Equivalent Weeks
Inspection Department	300	7½
Small Molding—Centrifugal Casting	400	10
Large Molding Floor	600	15
Chipping—Welding—Gas Cutting	200	5
Chemical and Metallurgical Laboratory	300	7½
Large Core Room	200	5
Sand Control Laboratory and Foundry Research	600	15
Small Core Room	200	5
TOTAL	2800	70

The balance of the time is spent in the Standards and the Heat Treating Departments, in Sand Reclamation, Production or any and all of the above mentioned departments.

The larger independent and captive foundries have programs for training graduates of colleges similar to one suggested in this report and the Falk Corporation course. Programs of this type are also used in the smaller plants which hire college graduates at the rate of one a year or less frequently.

Subcommittee Personnel

Since the formation of the Education Division of A.F.A., the task of developing and broadening courses for industrial training of college graduates has been taken up by the Engineering Apprentice Training Subcommittee under the chairmanship of C. V. Nass, Vice-President and Manager, Foundry Division, Pettibone Mulliken Corporation, Chicago. Other members of this Subcommittee are H. Bornstein, Director, Testing and Research Laboratories, Deere & Company, Moline, Ill.; Professor G. J. Barker, Chairman, Department of Metallurgy, University of Wisconsin, Madison, and Richard W. Heine, Instructor, General Motors Institute, Flint, Mich.

DISCUSSION

Chairman: F. C. CECH, Cleveland Trade School, Cleveland

Co-Chairman: B. C. CLAFFEY, General Malleable Corp., Waukesha, Wis.

C. J. BARKER[1]: We have been thinking about this educational program and I feel the universities will help the foundry industry by sending young men to the industry. We are today talking about training men and you have two distinct educational training programs at two different levels. You have your apprentice training program, which I believe is a fine program. The

[1] University of Wisconsin, Madison, Wis.

universities should not enter into that discussion. That is the problem of your apprentice training committee and you are treating it correctly.

Then you have the other program of the training of technical men to go into management. We are not interested in training at the universities technical men who are going to stay as supervisors in the cleaning department. If we thought that is as far as they would advance, we would tell them to enter some other industry. That is not what you want us to do. We feel you want us to train men for management, or higher technical positions, so we have set up our training program along that line. That brings in the question: What are we going to offer the young man at the university?

I know some foundrymen are antagonistic in having a technical man in their organization. I feel they are wrong and that they are going to change their viewpoint some day. Where we run into such management we advise our students never to go with that company. We would tell him the opportunities are not there for him. We would be doing no injustice to that company, because they have set up their own standards. We *do* like to send our men into companies where we know they have a definite training program.

We do not believe we are training the boys at the university for your foundry. We are training them to be in a position to go into your foundry and learn something after they get there. *You* have to train him after you receive him, along that line you desire him to work. He has to become familiar with your organization and that is going to take at least one year, probably two years. You will find, the boys we train, will have no objection to going into a training program of two years if you will definitely tell them ahead of time what that training program is and what it is going to lead to.

That is where the industry, as I see it, is a little weak at the present time. Many of the good companies have a training program set up now and in the past month we have sent four boys into those training programs, because they had something tangible to offer those boys. They knew where they were going. If more of you can definitely set up your training program for the technically-trained man of the university, I feel we can find the men for you if you want them. At the present time we are not sure you want them. We think you do, and we believe we can find the men for you. We shall do all we can to train those men properly.

You know there is a new foundation being set up to assist some of the universities and, personally, I believe it is a fine thing. There is a great deal of doubt in my own mind, at the present time, as to just what kind of a course you desire to give those boys in the university who will be under this program. I believe the trustees of the foundation, when it is set up, must give a great deal of thought to this question. I am sure if you come to us, you will find us very open-minded on the subject. We believe we can improve our foundry instruction. I wish to assure you that the universities, speaking for the University of Wisconsin, at least, are only too glad to cooperate with the foundry industry on their educational program.

MEMBER: As one who is an engineer in the foundry industry, I have a clear picture, first, what the engineer can do for the foundry, second, what the engineering schools must do for the engineer who is going into the foundry and, third, what the foundry must offer the engineer.

In the first instance, what can the engineer do for the foundry? An engineer, whether he is a mechanical, chemical, metallurgical, or an electrical engineer, has a definite background in scientific training and scientific thinking in solving the various foundry problems, whether or not they fit into his past university background. He will eventually solve the problems. This is what I mean: A foundry is a definite series of operations involving the melting of metal, making of a mold, pouring the metal into the mold and getting castings out. In that process there are certain variables, and those variables cause certain difficulties at one time or another. An engineer comes in with the background of clear thinking that the university is supposed to give him, not necessarily with a background of the answer to all the problems, but the background of physics, chemistry, drawing, engineering, to apply or the ability to read up further on the subject, to determine what the variables were that caused the difficulty and then to solve it. Then he can set up a quality control or process control plan for the foundry which will permit all variables to remain constant, or as constant as possible. In other words, he sets up a specification for sand and for metals, he sets up a heat specification, not only for all the heats but for particular castings, etc., until those variables causing the difficulty are eliminated and you get a consistent flow of good castings.

Then you get to the point of what the engineering college must give to the engineer who is going into the foundry industry. The engineer needs the background of chemistry, physics, engineering drawing, mathematics, metallurgy, and the ability to put that education into use in solving the problems as they occur in the foundry. So it is not specific courses that the engineering school must give, because those courses are already given to the university.

What does the engineering graduate expect from the foundry? According to aptitude tests given by Hall & Liles in Chicago, and other firms, we find that certain engineers make good salesmen, certain engineers make good executives, and certain engineers make excellent research men working all by themselves. Those are individual characteristics and when a foundry gets an engineer, it is worth while for him to take those aptitude tests, because if you want an engineer who is eventually going to become a supervisor, you want an entirely different type of personality from one who is going to do purely technical or research work. Or if you want a man who is going into sales engineers, you want an entirely different type of individual. The hit and miss idea of putting a man into a hole and seeing whether he fits is the old way of doing things, and the newer more efficient method is by aptitude testing.

Those are the ways in which an engineer fits into the foundry. He can be of great value to a foundry or he can be a distinct liability, depending upon how much leeway the foundry is going to give him.

It has been mentioned that the engineer is put out in the foundry and given a hard manual job to do. I have seen it happen in the past where I have had graduate engineers working in the same department I worked in, some of them had pretty nasty jobs for a while, and I saw 60 or 70 of them flow through there in less than a year and none of them stuck. They all wanted my job. I did not blame them, as far as that is concerned, and I actually never served an apprenticeship on a molding machine; I never worked on a lathe; I never melted a heat of metal, until I went into the foundry in a supervisory capacity, and eventually as an engineer. I did all those things, not because they were required of me, but because I wanted to although I did not follow a definite program in doing them.

It is not necessary to take an engineer out of engineering school and put him on a molding machine for six weeks, to have him shoveling steel into a furnace, or iron into the cupola, pulling slag out of an electric furnace, or whatever type of melting unit you are using, or to have him make patterns in order to learn what the foundry industry is about. I believe that a technical man should be sent into the foundry. He cannot wear good clothes, because he has to learn the foundry operations, but not to learn by doing them. He has to learn them by observing them and observing the faults as they occur in the foundry and attempting to use his mental faculties to solve those problems. In doing that, he is going to be worth while to the foundry. But no engineer is going to be worth his pay to a foundry on a molding machine for very long.

MR. CLAFFEY: We purposely injected the discussion on this particular subject because many of you already have been contacted and are familiar with the foundation which Professor Barker referred to and the work which the foundation proposes to do with the schools in developing future foundry executives.

At many of our conventions and in our meetings we have often heard the statement made that what the industry needs is more college-trained men and more graduate engineers. This little discussion was purposely provoked to familiarize all of us with what some of the school people may think we should do in handling this fellow who is coming to us, so that we have a clearer understanding on that particular subject.

PATTERN PURCHASE CONSIDERATIONS

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ABSTRACT

Considerable thought should be given to the making and purchasing of good pattern equipment since it can be a significant factor in the control and reduction of foundry costs. The purchase of patterns for another foundry requires a knowledge of its production facilities and techniques. In this case, the assistance of a pattern engineer is valuable. Low first cost of pattern equipment is generally poor economy because maintenance costs may be high, casting quality may suffer and production may be low. Whether patterns should be interchangeable or whether duplicate equipment should be provided depends on the number of changes to be made and the flexibility desired.

ONE OF THE GREATEST PROBLEMS facing the foundry industry today is control and reduction of costs. In this connection, how many foundrymen give due thought and consideration to the making and purchasing of good pattern equipment—pattern equipment which can be put to most efficient use?

For the past five years foundries have enjoyed noncompetitive markets, but as times become normal they will face a buyer's market. The requirement then will be new and better equipment to increase productivity. That situation will also call for pattern equipment which will eliminate the human element, such as the blowing of more intricate shaped cores, which gives more uniformity and lowers costs in the foundry.

The many problems involved in the purchase of patterns can be divided into those arising out of purchases for captive foundries and those connected with purchases for commercial foundries. A purchasing agent's greatest problem, however, is to secure from the foundry detailed information as to the exact kind and type of pattern required.

Pattern Purchasing Procedure

Here is typical procedure. The purchasing agent receives a requisition calling for purchase of a pattern needed to make *x* number of castings per year. The castings are to be in production by a certain date. He must requisition drawings to find out what he is to purchase. It then becomes necessary to contact the foundry to determine the kind of pattern desired, the number on a plate, whether a gang box is needed, how many driers are required, etc.

Usually the foundryman says, "Oh, put four patterns on such a size plate and make 50 driers for the body box." What little information he can obtain the purchasing agent passes on to the pattern shops from which he expects quotations. Obviously, before a pattern shop can give an intelligent quotation it must obtain much more information. This is done usually by letter or telephone; and even then there are many interpretations of a drawing.

The most logical course for the patternmaker is to make the best pattern he can on the basis of the data furnished; naturally he will not wish to offend the customer by pointing out a failure to furnish necessary information.

After the foundry receives the pattern the purchasing agent gets a call from the foundry superintendent, who demands to know how in the world he is to gate the pattern when there is not enough room between patterns for a riser, etc. The purchasing agent calls the pattern shop and reports the superintendent's objections. Now there are three dissatisfied parties, and a pattern which will always be a problem in the foundry. This is happening every day. The situation calls for closer cooperation and understanding between the foundry and the purchasing department.

Purchasing Quality Patterns

A purchasing agent is in daily contact with sales people and frequently makes plant visitations. He is able, therefore, to keep abreast of the latest production methods and new equipment. With this information he can buy quality at the lowest price and get value for money spent.

Purchasing of patterns for a captive foundry is considerably simpler than purchasing for vendors. A purchasing agent has knowledge of his company's foundry and is familiar with its molding and coremaking facilities; in most cases he has purchased the equipment. However, it is becoming imperative that he know his vendor's facilities equally well because they are just as important.

Most commercial foundries do not have their own pattern shops and rely on commercial pattern shops for new patterns. This creates another problem for the

purchasing agent, since he must know the type of equipment required by his vendor.

During the war the purchasing agent learned many things as a result of the migration of his patterns. At that time migration reached a high level due to the inability of many foundries to produce castings in required quantities. This taught the purchasing agent to make his pattern equipment more flexible, to get away from any special equipment required by his suppliers. It also encouraged castings suppliers to standardize on commercial machines and flask equipment and thus to reduce the possibility of endangering their customer's production because of a breakdown or labor trouble which would require a new pattern to be made.

Pattern Flexibility

An example of the aforementioned problem of special equipment is the case of a foundry which changed to large match-plate work, namely 15 x 32-in. plates, and used snap or pop-off flasks under a sand slinger. From a purchasing agent's viewpoint, this equipment was relatively inflexible since there are very few foundries able to handle match-plate work of this size. Although the agent fully realizes this equipment could reduce some foundries' costs, he must give consideration before sanctioning this type of pattern equipment.

A purchasing agent must know how intricate the casting is he has to buy to determine the type of equipment to purchase. Frequently, the cheapest pattern is the most expensive. A pattern can be made cheaply, so as to hold down initial costs. However, in almost every case, when a quantity of castings is required, the cost is excessive if pattern equipment is inadequate. Cheap patterns always tend to raise foundry costs; and, certainly, uniform castings cannot be obtained from them.

Pattern Maintenance

There are also the problems of high maintenance costs. A purchasing agent must know the number of castings required from a pattern to help determine its construction. When castings are of intricate design, it is often cheaper to make metal equipment for only a small quantity. Metal equipment can save money for a foundry and, certainly, for the machine shop which will have more uniform castings to machine. A purchasing agent must keep these factors in mind; he must consider a job from an overall standpoint.

There is always the problem of making patterns interchangeable; and here the cost of interchanging should be the guide. Where frequent changes are necessary, the cost of changing may equal or exceed the cost of separate equipment for each job. Separate equipment increases the flexibility of pattern equipment.

When all factors are considered, the initial cost of good production pattern equipment is certainly a small item compared to losses in castings traceable to a cheap pattern.

There is an increasing need for pattern engineering in foundries. The requirement is for someone who can coordinate pattern and flask equipment and show customers where they can save money.

Pattern Engineering

Pattern engineering in a foundry does not in any sense require elaborate arrangements. The only essential is someone who can read a blueprint and can indicate parting lines, core prints, and the type of core boxes, driers, etc., which are needed. Such information clarifies points for the purchasing agent who, in turn, can pass it on to pattern shops, enabling them to quote more intelligently and on a competitive basis. It also serves as a record for the foundry.

A pattern engineer frequently can be helpful to the designer by straightening out a wall or running a boss down to the parting line to eliminate a core. Such changes reduce pattern and casting costs. A purchasing agent should encourage his vendors to offer suggestions, and should use their suggestions whenever possible.

A purchasing agent must watch the care and use of patterns at his vendors' plants. Many thousands of dollars have been invested by his company to supply good pattern equipment, and much money may be lost in rejected castings if care and maintenance of patterns is below par.

The foundry industry today is challenged by welded and fabricated parts. In the past few years foundries have made the purchasing of castings difficult because they preferred to run the easier types, thus forcing purchasing agents to go to new fields for substitutes. A veteran foundryman once said that "when they start fabricating odd shaped manifolds," he would give up the foundry business. Today you can buy many odd-shaped manifolds for half the cost of castings. However, the threat that other products will replace castings need not become serious if quality is maintained in castings at the consumer's specifications. And good pattern equipment is basic to production of good castings. The old saying—"a casting is only as good the pattern from which it is produced"—remains true.

DISCUSSION

Chairman: V. J. SEDLON, Master Pattern Co., Cleveland

Co-Chairman: A. F. PFEIFFER, Allis-Chalmer Mfg. Co., Milwaukee

F. M. NEMCIK[1]: You referred to the Pattern Engineer. Who is the Pattern Engineer and where does he get his ideas from?

MR. SCHULLER: This reverts to the foundryman himself. He has to bring up someone in his own organization. I do not think it necessarily takes a college graduate to do it. Your Pattern Engineer is a man who is familiar with your practices and knows the type of patterns you require and can make suggestions to his customer. Many times the design can be modified to make a cheaper casting and a better pattern. I think it is purely up to the foundry itself.

A Pattern Engineer is a man who has ability to read a drawing and be able to mark up a blue print so that you will get what you want. Some of the men in the pattern shops making the patterns had to use their own ingenuity in making the pattern. They mostly could carry it a little bit further. Most patternmakers have had to take the initiative on that in many foundries. The patternmaker had to do it because possibly he had a little better knowledge of reading a blue print. I still think it ought to be done right out in the foundry. The patternmaker could do a very good job on it.

MR. NEMCIK: You stated that pattern engineering is practically done by the foundries today. Does that make your patterns flexible enough?

MR. SCHULLER: The buyer takes that into consideration. He

[1] Apex Non-Ferrous Foundry, Cleveland

should also have some knowledge of the facilities you have in your plant and the way you want your patterns made. He might make a suggestion, too, or go ahead and make it in a way that will make it more flexible. Many foundries have special set-ups such as large turntables. They make a lot of match plates, 15 x 32. Try to place one of those in a commercial foundry today. Not many commercial foundries today have that type of equipment and facilities. That is what I meant by reducing the flexibility.

MEMBER: I assume that it would be the buyer's job to furnish the Pattern Engineer or do the engineering.

MR. SCHULLER: Your buyer is purely the mouthpiece, eyes and ears, for the foundryman. That is why he is to do the buying. He is your buyer and you should use him to the best advantage by imparting information to him by a marked print with which he can intelligently do a job of buying. He must be given the proper tools to work with.

F. L. GUYETTE[2]: This discussion is particularly interesting to me because my training up until about a year ago was in tooling for machining operations where tool engineering is of major importance to good production machining.

My first impression upon being transferred to the pattern division of our foundry was the lack of planning before pattern equipment for various productions was started, much of it being left up to the patternmaker as to finish, draft and kind of patterns and core boxes to be furnished.

It would appear that the job of furnishing patterns for foundry use is as important as are the tools to machine the casting made from these patterns. Therefore, why should not a central planning department similar to a tool designing department be of great value to any foundry.

My lack of foundry and pattern experience makes it difficult to decide where the men to man such a department should be drawn from.

I am also perplexed as to why, if this work of planning patterns is as important as it now appears to me, more progress has not been made in developing men for this type of work.

The point I am trying to make is—can patterns be planned and drawn up by a draftsman so the patternmaker can successfully make a workable pattern by following a blue print, and, is there any merit in following such a system as far as savings standardization, economy, etc., are concerned?

MR. SCHULLER: You have to create your own Pattern Engineer.

R. C. DAVIS[3]: You ask how we handle our pattern making. When we receive an engineering drawing of a cast part we make a complete pattern layout showing core prints, drafts, split lines, partings, loose pieces, core clearance, finish allowed, detail core rubbing or grinding jigs, core assemblies and core setting gages, coreboxes and driers, and flasks when required.

These drawings and specifications are given to the Purchasing Department to buy. This eliminates a good deal of trouble and allows us to split the work up and place in different shops and it comes in the way we want it.

In experimental work, which in some cases, the design is somewhat of an engineer's dream; we try to give them what they want because it is in the research stage. But when a part is released for production we simplify it as much as possible.

If we can show the engineers where we can save a little here and there by straightening walls or runnig bosses down to make better partings and eliminate cores or loose pieces, they will modify the design to simplify the molding.

MEMBER: The buyers could do a lot for us as pattern engineers if they would educate management of their respective companies what good pattern equipment costs. We run into cases where we quoted and submitted drawings of the type of equipment we use in our foundry, a production foundry, and in the case of one customer the management of the company we were trying to sell said: Patterns cost as much as a house.

We have pattern equipment that costs a lot more than a good house would cost. I think the buyers could do us a lot of good if they would go in and sell the management on what a good piece of pattern equipment will cost them.

MR. SCHULLER: I agree with you. It starts right with the foundry by imparting information to the buyer. He in turn, has to answer to his superior why he is spending all this money.

Of course, it is up to you to remain competitive. If you are asking for something that might be several thousand dollars in pattern equipment, it is up to you to remain competitive, because we will be buying on a competitive market.

G. J. GEDEON[4]: This dates back about eight years, when I made a remark in New York to a friend that I thought he was going to have to go in the pattern shop to get himself some honest-to-goodness help. He did not agree. I want to ask you if you have ever been able to develop a pattern engineer in your plant without going into the Pattern Dept.

J. KOLB[5]: I will answer that, but first I would like to ask Mr. Davis if it is possible for him to take any of his patterns and let a purchasing agent place those patterns in any foundry that says it can make them? I do not believe he can.

MR. DAVIS: I will admit that our patterns are not that flexible. I will say that they can be run in most any modern foundry. But in some foundries their equipment would be different than the equipment for which the patterns were designed.

We have an idea who is going to make the castings and design our patterns to suit their equipment.

It is sometimes necessary to make our patterns two ways to suit different foundries. Until the foundries standardize their equipment, patterns cannot be made flexible enough to run in every foundry.

I figure today that the ordinary patternmaker lacks foundry experience. The average shop will turn out a good job if it has been engineered and laid out as it should be made. But give them a plain engineering drawing to work from, it is doubtful if the pattern would be practical for production.

MR. SCHULLER: Of course, he is the patternmaker. You cannot expect him to be a foundryman, too.

MR. DAVIS: I think anyone running a pattern shop should have someone with the ability to engineer a complete pattern equipment. A customer does not like it when he runs into trouble and has to make a lot of pattern changes to get it in production. There are some shops that do a good job of pattern engineering, which is a real service to their customer.

MR. KOLB: Until we standardize on equipment in foundries to handle all the different types of patterns that are made in a manufacturing concern similar to the ones you and I represent, never will there be a flexible pattern made that can be placed in any foundry and get production on it. That is my answer to a flexible pattern. One thing the author did not mention was that he won a prize at a patternmaking contest one year in the A.F.A.

I am more or less responsible at Caterpillar Tractor Co. for engineering pattern equipment. We design the equipment. First we find the burden, ask the Planning Department about the burden on the molding machine, what unit they would like to run the equipment on. If they want to run it on a unit where it does not look like the job would be applicable to it, we make our suggestions. But before a pattern is finally laid out, every man in the foundry that has anything to do with the equipment has his signature on the pattern drawing. When the pattern comes into the foundry, he has his name on the pattern layout, and if he has sense of responsibility of helping design the equipment, he will make it work.

At pattern engineer, if he does not take cognizance of the foundry that is going to use the pattern and get their explicit ideas of the unit they are going to run it on, the type of machine, how they will make the cores, in every detail, he is not a pattern engineer.

JOHN GILL[6]: If you want a pattern engineer, go to the pattern shop. Pick out a young man with a little intelligence and ambition and a little bit of "go-between ability," one who can go to the purchasing agent, the engineer, and the foundry, and work out what is wanted. I think that is the primary thing, someone who can intelligently suggest the changes which should be made to help the purchasing agent and the foundry, and still make the pattern a practical pattern to make.

MR. SCHULLER: I agree with you 100 per cent.

MEMBER: I am going to disagree regarding what a pattern engineer is. It takes a person of experience in the foundry, in the core room and in the machine shop as well, because a

[2] The New York Air Brake Co., Watertown, N. Y.
[3] Chrysler Corp., Detroit
[4] Aluminum Company of America, Cleveland
[5] Caterpillar Tractor Co., Peoria, Ill.
[6] Lake Shore Pattern, Erie, Pa.

pattern shop has to cope with all the conditions of the three different departments.

That does not only say that a pattern engineer must be a foundryman, but also has to be experienced in the core room and every department in the foundry. A pattern engineer is one that is brought up in the foundry and then converted to the pattern shop. The foundry is the place that you have to satisfy for a pattern, because there is no equipment in any two foundries that is worked the same. You can take equipment from one particular foundry, and it is not used the same as it is in others. You can go into a core room and you will find your sand conditions differ entirely on various jobs that are from one foundry to another. One may have a definite arrangement whereby the sand is arranged for the best results for their casting. All those come in. They are part of a pattern engineer. And the one thing that I had in mind regarding the remark of pattern buying is that concerns interested in pattern buying are not always interested in the quality of the goods they buy. There are still a lot of them that are interested in the price tags attached to the drawing when they send it out for quotations.

MR. SCHULLER: I will agree with you there, but I mentioned most buyers, and it will be in the future where they will have to be more conscious of better pattern equipment to realize a better price on the end part of their casting.

MEMBER: What kind of patterns do you have to buy when there are only one to ten castings to be made? Can you use patterns like that, or can you cut a few corners?

MR. SCHULLER: In most cases, unless the job is experimental, some sources will ask for ten castings, and two years later the job is still running and the foundry is still using that same pattern.

MEMBER: I found that same condition where I work. We make a pattern for six castings and they run 200.

MR. SCHULLER: I would get after my production department.

MEMBER: Some purchasing agents do that purposely. They pull a pattern from the foundry periodically with that intention. I caught on to them and I do make pretty good patterns.

MR. SCHULLER: You have a job of education with your purchasing agent.

MEMBER: There are foundries that we get work from that do much planning. They send work in to us from all over the country and they expect us to do the work.

We know well that we cannot satisfy these several hundred foundries around the country, and so we have tried to get some uniformity in patternmaking.

We get patterns from a plant where cope and drag is designated by different colored paints. If they want a certain size gate thrown into the casting, it is on there.

CHAIRMAN SEDLON: You have eliminated a good deal of trouble by submitting a plan before you go ahead and make the pattern.

TEMPERATURE DISTRIBUTION IN METAL MOLDS

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ABSTRACT

The substitution of thinner walled molds for centrifugal casting and the use of air or water to cool these molds are sometimes desirable. For design purposes in such a substitution, it is important to know the temperature distribution in molds of various thicknesses under the different possible conditions of cooling. In the present investigation, the following variables were studied by measuring the temperature distribution in a test plate at intervals following the pouring of a casting on that plate:*

1. Mold thickness (2 in. vs. 9 in.)
2. Mold coating thickness (0 to 0.04 in.)
3. Casting thickness (1 to 6 in.)
4. Cooling media (air vs. water)
5. Cooling water rate (3 to 9 fps.)
6. Mold heat conductivity (steel and cast iron vs. copper)
7. Finned vs. plain plates

In general, the conclusions are that thinner molds with air or water cooling could be used to advantage and that, to secure maximum advantage from water cooling and higher conductivity mold material, it would be desirable to incorporate an effective thermal barrier on the hot side of the mold.

NEW PROCEDURES HAVE BEEN DEVELOPED and used for the production of steel castings to meet exacting requirements. Among the methods finding increasing use is that of centrifugal casting in metal molds. One practice in centrifugal casting is to use rather heavy cast iron or steel molds, without external cooling, except for the air surrounding them.

In such instances, the mold weight may be as much as six times the weight of the casting produced. Obviously, the molds for the larger castings are rather massive, and for this reason require complicated and expensive machinery to spin them at the desired speed. Then too, the cost and life of centrifugal casting molds are of considerable importance when the utility of the centrifugal casting method is under consideration.

Heating and cooling of a mold set up cyclic stresses so that hairline fatigue cracks soon develop. The cracks grow larger as more castings are made and they foster scoring, from withdrawal of the castings, which soon leads to complete failure of the mold. When casting in a mold, the inner surface is heated much hotter than the rest of the mold, and because of thermal expansion, a compressive force is set up within the inner mold wall.

The stress is sufficient in a new mold to cause upsetting of the inner mold surface so that, when the mold is cooled, this part is shorter circumferentially than it was originally, thus introducing tension stresses. Since compressive forces cause deformation, it is the tension stresses which cause cracking.

When the mold is used repeatedly, the stress has a cyclic nature such as that which causes fatigue failure. When the inner mold surface is hot, it is in compression and, when cold, in tension. If a constant temperature difference existed across the mold, no failures would occur, because the molds would accommodate themselves to this condition and stresses would gradually decrease to some harmless value.

Accordingly, the stresses which cause failure are not caused by a high temperature difference, but by large changes in the temperature difference. In other words, if two similar molds maintained the same outside temperature, and one were heated on the inner surface to a maximum of 1000 F and cooled to a minimum of 200 F, there would be a change in temperature difference of 800 F, while if the other were heated also to 1000 F but cooled only to 400 F, the change in temperature difference would be 600 F.

In all probability, the latter mold would last longer because the stress range in fatigue, which is caused by the change in temperature difference, would be considerably less than it would be in the first mold. Fully as much decrease in the stress range is caused by keeping the inner surface hot between casts as by keeping it cool during the cast. A knowledge of the temperature distribution in metal molds used in centrifugal casting should, therefore, prove important in seeking to improve mold design, with resultant influence on mold life and the ultimate design of the centrifugal casting machine.

For example, the use of thinner molds cooled by water or air would lead to a saving in mold weight, would shorten the casting cycle, and would permit the use of lighter spinning machines. Information as to

* This paper is based on work done under Contract OEMsr-731 between Battelle Memorial Institute and the Office of Scientific Research and Development, which assumes no responsibility for the accuracy of the statements contained herein.

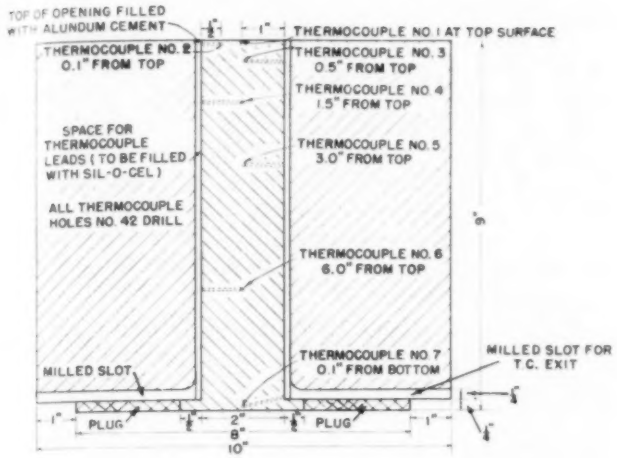


Fig. 1—Sectional view of 9-in. cast block.

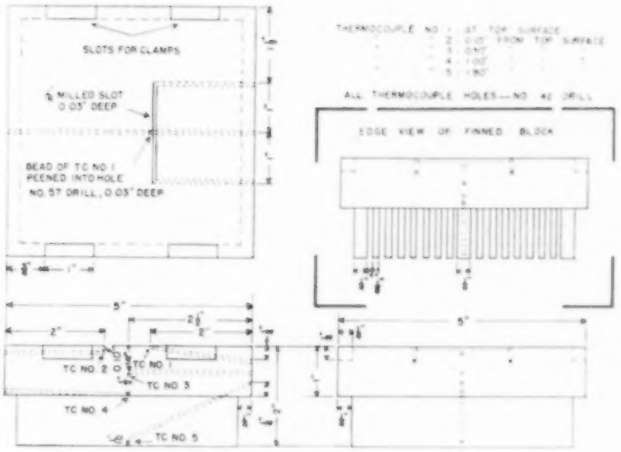


Fig. 2—Construction details of 2-in. blocks.

heat flow in molds made of various materials and cooled in different ways should help in estimating the probable economies which might follow from substituting thin-walled, liquid-cooled molds for heavy-walled, air-cooled molds in centrifugal casting.

To acquire needed information on heat flow in metal molds, temperature-distribution measurements were made on test plates which were intended to simulate sections from the molds. These measurements were made at definite time intervals, beginning immediately after pouring steel on the test plates. The following operational variations were investigated:

1. Nine inch thick, air cooled, cast iron plate.
   a. Mold coating thickness from 0 to 0.04 in.
   b. Casting thickness from 1 to 6 in.
2. Two inch thick, air cooled, cast iron plate.
   a. Mold coating from 0 to 0.10 in.
   b. Casting thickness from 1 to 3 in.
3. Two inch thick, water cooled, cast iron plate.
   a. Mold coating thickness from 0 to 0.10 in.
   b. Casting thickness from 1 to 6 in.
   c. Cooling water velocity from 3 to 9 fps.
4. Two inch thick, air cooled, steel plate.
   a. Casting thickness from 1 to 3 in.
5. Two inch thick, water cooled, steel plate.
   a. Coating thickness of 0.04 in. and casting thickness of 1½ in.
6. Two inch thick, air cooled, finned, cast iron plate.
   a. Coating thickness of 0.04 in. and casting thickness of 1½ in.
7. Two inch thick, water cooled, finned, cast iron plate.
   a. Coating thickness of 0.04 in. and casting thickness of 1½ in.
8. Two inch thick, air cooled, finned, steel plate.
   a. Coating thickness of 0.04 in. and casting thickness of 1½ in.
9. Two inch thick, water cooled, finned, steel plate.
   a. Coating thickness of 0.04 in. and casting thickness of 1½ in.
10. Two inch thick, air cooled, copper plate.
    a. Mold coating thickness from 0 to 0.04 in.
11. Two inch thick, water cooled, copper plate.
    a. Mold coating thickness from 0 to 0.04 in.
    b. Cooling water velocity from 3 to 9 fps.

The pertinent data obtained in the conduct of this investigation are recorded in the following sections of this paper.

Experimental Work

Apparatus. The cast iron test plates used in this study were made from an iron of approximately the following analysis: C—3.30 per cent; Mn—0.70 per cent; Si—2.25 per cent; P—0.17 per cent; S—0.10 per cent.

The nine-inch plate was a cylinder 10 in. in diameter and nine inches thick. Figure 1 is a sectional view of this plate. The plate was designed with a central plug instead of as a solid block for two reasons:

First, because of the limited diameter of the mold, the plug assembly was added insurance that the direction of heat flow was perpendicular to the surface; and

Second, it facilitated accurate location of the thermocouples. The plate was set in a wind box with its bottom surface forming the top of an air chamber having a ¼ x 8 in. cross section.

The plate was insulated around the cylindrical surface with one inch of Sil-o-cel. Sil-o-cel was also used to insulate the central plug from the main block. A motor-driven fan forced air through the air chamber at a rate of 189 cfpm or a linear velocity of 13,600 fpm corresponding to the relative velocity at the outside of a 9-inch-thick, 24-inch-inside-diameter mold spinning at 1300 rpm. The rate of air flow was controlled by a sliding gate valve, and measured by the pressure drop across an orifice.

The thin plates were five inches square and two inches thick. The steel plates were made from forged steel of the following composition: C—0.37 per cent; Mn—0.82 per cent; Si—0.25 per cent; Cr—1.12 per cent; Mo—0.24 per cent; Ni—0.05 per cent (residual). The copper plate was made from forged tough pitch copper.

Figure 2 shows the detailed construction of the thin plates. It was assumed that, in such a thin plate, heat flow would be perpendicular to the surface, at least

in the central section where the thermocouples were located. The air and water boxes for the thin plates were similar in construction, having fluid spaces of ¼ x 4½ in. cross section (the fluid space for the finned plates was increased by the amount of metal removed in machining the fins).

Air rates were 85 cfpm (237 cfpm for the finned plates to keep the linear velocity constant), corresponding to the relative velocity at the outside of a two-inch-thick mold, 22 in. inside diameter, spinning at 1300 rpm. Water rates were 1.4 and 4.2 cfpm (a linear velocity of 3 and 9 fps), except for the finned plates where 3.9 cfpm (corresponding to a linear flow of 3 fps) was used. The water was recirculated from a reservoir. Water and air rates were controlled by gate valves and measured by orifice meters.

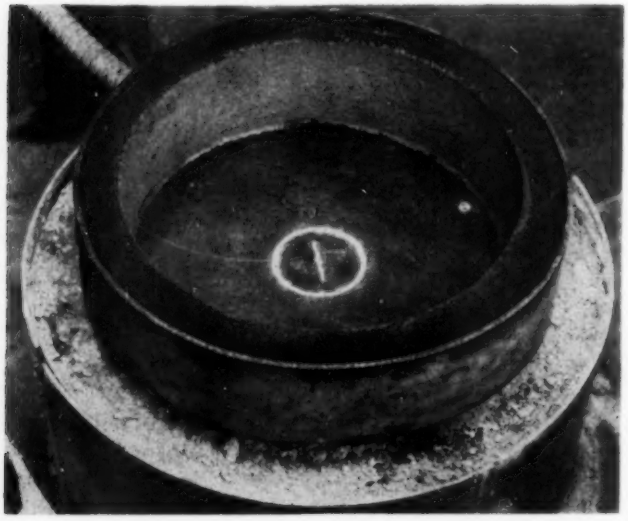
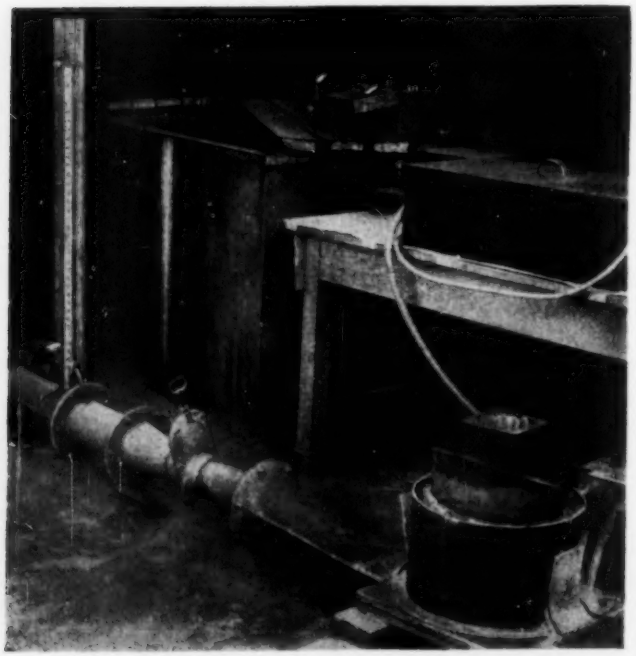


Fig. 3 (above)—Mold for 9-in. plate.

Fig. 4—Set-up for producing test blocks.



The molten steel was confined to the surface of the plates by core-sand rings, one inch thick and three inches high. (In the case of the six inch thick castings, the ring height was 6½ in. and the ring was insulated with one inch of Sil-o-cel. To prevent excessive heat loss by radiation, a one-inch-thick transite plate was laid over the top of the ring after the casting was poured. The steel was poured through a pouring cup having openings concentrically located around the thermocouple positions.

Figure 3 is a picture of the top surface of the nine-inch-thick block with the sand-core ring in place. The ring was made rigid and heavy by an outside shell of ⅛ in. sheet steel. A general view of the apparatus with the nine-inch-thick block is shown in Fig. 4. The air delivery pipe with gate valve and pressure gage is seen at the left and bottom of the photograph.

The pipe was flattened out as it reached the test block in order to accommodate it to the cross section of the air chamber. A pouring cup is seen in place over the sand core above the test block. The shielded thermocouple leads extended from the mold up to the cardiograph and portable potentiometers on the table.

A general view of the setup for the thin plates is shown in Fig. 5. The air box (2) is shown empty, while the water box (1) is shown with a plate in place. The plates were clamped in position. Leakage was prevented by using rubber gaskets. One of the typical castings (3) is shown. Double-throw knife switches (4) were inserted in the thermocouple circuits, so that a test could be made with air cooling immediately after one with water cooling.

The readings of the top four thermocouples were taken with a Sanborn porto-cardiograph which recorded the voltage from these thermocouples and from two standard sources on a strip of photographic paper. The temperatures of the bottom three thermocouples in the nine-inch plate and the bottom thermocouple in the two-inch plates were measured with portable potentiometers.

Locations of the thermocouples in the plates were as follows:

Thermocouple No.	*Distance From Top Surface, In.*	
	9-In. Plate	*2-In. Plate*
1	0	0
2	0.1	0.1
3	0.5	0.5
4	1.5	1.0
5	3.0	1.9
6	6.0	–
7	8.9	–

A string galvanometer in the cardiograph was activated by the thermocouple voltage, and the shadow of the string was recorded on a strip of photographic paper. Also recorded automatically on the paper was a series of lines $\frac{1}{25}$ of a second apart.

To switch from one thermocouple to another, a rotating drum, with cams to actuate individual switches, was used. This drum was rotated at about 60 rpm.

Because the galvanometer of the cardiograph was limited to 6 millivolts, a proportioning shunt register was used to reduce the voltage of the thermocouples and of the standards to approximately ⅙ of their origi-

nal value. The thermocouple leads were shielded to prevent pickup of stray alternating currents.

Test Procedure. With the nine-inch plate, the cardiograph camera was operated continuously for the first two minutes, then for a few seconds at a time at half-minute intervals between two and ten minutes, at minute intervals between ten and 20 min. and at five minute intervals between 20 min. and 60 min. after the steel was poured on the plate.

With the two-inch plates, the camera was operated continuously for the first two minutes, then for a few seconds at a time at half-minute intervals between two minutes and ten minutes, at minute intervals between ten and 15 min., and at five-minute intervals between 15 and 30 min. after the steel was poured on the plate.

Although the spots on the cardiograph record were about 50 F wide, it was possible, by using similar reference points on each spot, to read the temperatures within ±10 F.

Readings on the portable potentiometers connected with the lower thermocouples (3 for the 9-in. plate, 1 for the 2-in. plates) were taken every half minute for the first ten minutes, then at minute intervals to 20 min. on the nine-inch plate and to 15 min. on the two-

Fig. 5—Arrangement for tests on thin plates; water box with plate in position for testing; 2, air box, empty; 3, typical test casting; 4, knife switches for change-over from one box to the other; 5, ice bottles for cold junction; 6, gate valve; 7, thermometers; 8, leads to cardiograph, shielded against current pick-up; thermocouple leads, also shielded. In order to prevent leakage, rubber gaskets were used in clamping plates.

Table I.—Test Conditions for All Plates

Plate			Casting		
Material	Thickness, in.	Coolant	Thickness, in.	Nominal Weight, lb.	Mold Coating, in.
Cast iron	9	Air	1½	27	0.04
Cast iron	9	Air	1½	27	0.01
Cast iron	9	Air	1½	27	None
Cast iron	9	Air	1	18	0.04
Cast iron	9	Air	3	54	0.04
Cast iron	9	Air	6	108	0.04
Cast iron	2	Air	1½	7.5	0.04
Cast iron	2	Air	1½	7.5	0.01
Cast iron	2	Air	1½	7.5	None
Cast iron	2	Air	1	5	0.04
Cast iron	2	Air	3	15	0.04
Cast iron	2	Water, 3 fps	1½	7.5	0.10
Cast iron	2	Water, 3 fps	1½	7.5	0.04
Cast iron	2	Water, 3 fps	1½	7.5	0.01
Cast iron	2	Water, 3 fps	1½	7.5	None
Cast iron	2	Water, 3 fps	1	5	0.04
Cast iron	2	Water, 3 fps	3	15	0.04
Cast iron	2	Water, 3 fps	6	30	0.04
Cast iron	2	Water, 9 fps	1½	7.5	0.04
Steel	2	Air	1	5	0.04
Steel	2	Air	1½	7.5	0.04
Steel	2	Air	3	15	0.04
Steel	2	Water, 3 fps	1½	7.5	0.04
Steel	2	Water, 9 fps	1½	7.5	0.04
Cast iron, finned	2	Air	1½	7.5	0.04
Cast iron, finned	2	Water, 3 fps	1½	7.5	0.04
Steel, finned	2	Air	1½	7.5	0.04
Steel, finned	2	Water, 3 fps	1½	7.5	0.04
Copper	2	Air	1½	7.5	0.04
Copper	2	Air	1½	7.5	0.01
Copper	2	Air	1½	7.5	None
Copper	2	Water, 3 fps	1½	7.5	0.04
Copper	2	Water, 3 fps	1½	7.5	0.01
Copper	2	Water, 3 fps	1½	7.5	None
Copper	2	Water, 9 fps	1½	7.5	0.04

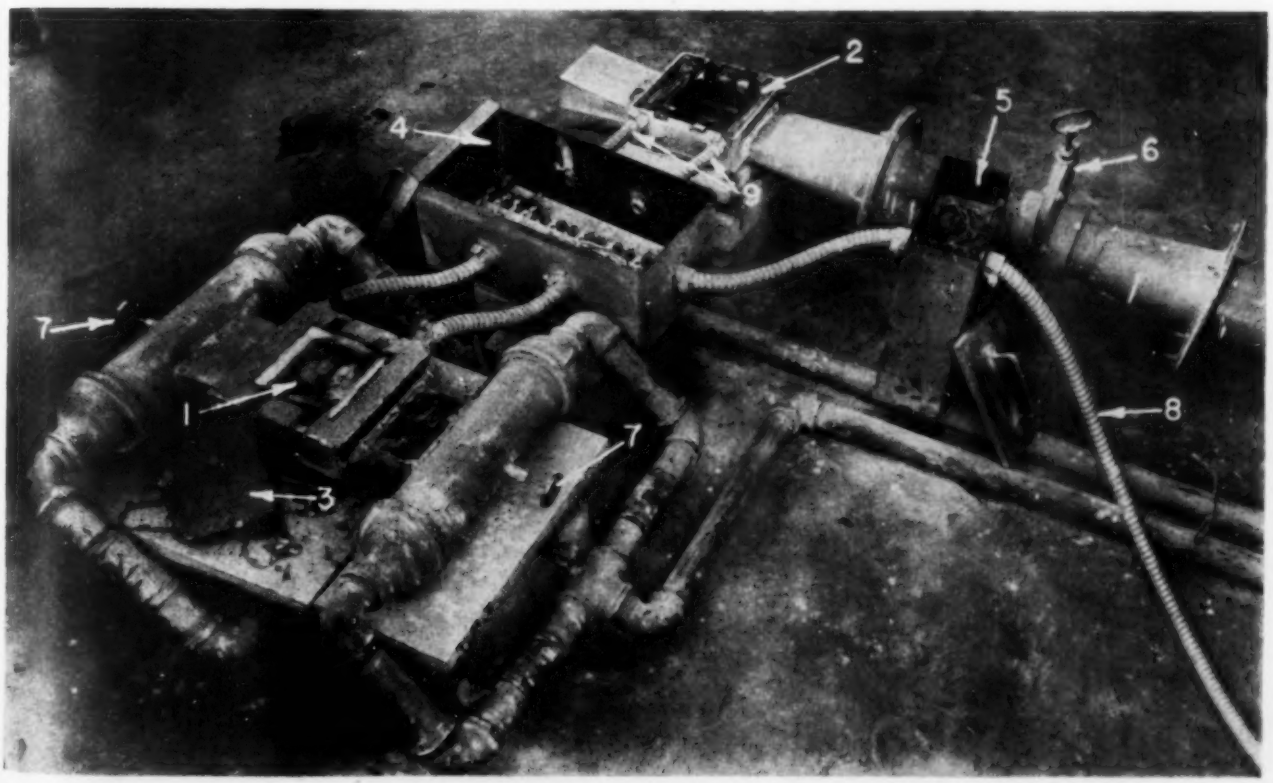


TABLE 2.—TEMPERATURE DISTRIBUTION DATA FOR THE NINE-INCH CAST IRON MOLD, AIR COOLED AT 189 CFM.

Conditions for Tests					Data				
		Casting							
Test No.	Mold Coating Thickness, In.	Intended Thickness, In.	Weight, Lb.	Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp. Difference, Min.	Max. Change in Temp. Difference, °F.
8	0.04	1½	25.5	2965	1084	3	975	3	997
9	0.04	1½	26.0	3010	1044	4	917	4	914
10	0.01	1½	25.0	3015	1359	1	1209	1	1189
11	0.01	1½	27.0	3020	1313	3	1185	3	1177
19	None	1½	26.0	3005	1281	3	1142	3	1109
20	None	1½	26.0	3000	1453	3	1320	3	1312
14	0.04	1	17.0	3000	1001	3	866	3	955
15	0.04	1	16.0	3000	1031	3	903	3	911
16	0.04	3	48.0	3020	1399	8	1229	5	1256
17	0.04	3	48.0	3020	1366	7	1206	6	1236
125	0.04	6	96.0	3000	1573	8	1428	6	1439
126	0.04	6	96.0	2980	1475	10	1305	6	1314

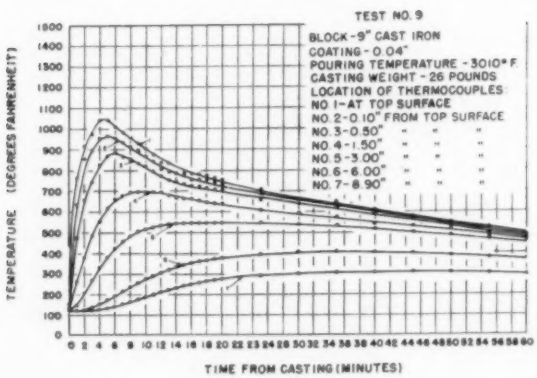


Fig. 6—Time-temperature curves, test No. 9.

inch plates, and at five-minute intervals thereafter.

In all cases, the mold coating was applied by spraying with the plate at about 200 F, and was further torch dried at about 350 F. Thickness of the coating was measured with a depth micrometer. When the bare plate was used, mold wash was applied at positions located under the pouring-cup holes (but not over the thermocouple locations), to prevent damage to the plate by abrasion and erosion from the liquid steel. Composition of the mold wash in parts by volume was 400 parts "Dicalite" (U.F.Grade) *, 24 parts Bentonite, 8 parts 17 per cent waterglass, and 400 parts water.

Data were taken under the specific conditions listed in Table 1.

All the castings on the nine-inch plate and the six-inch castings on the two-inch cast iron plates were poured at 3000 F, all others were poured at 3100 F. Initial plate-temperature variations amounted to but a few degrees in the thin plates and to about 100 F in the nine-inch plate, but these variations seemed to have no significant effect on the data. The analysis of the steel poured was approximately 0.30 per cent C, 0.80 per cent Mn, and 0.40 per cent Si. Other variables were kept as nearly constant as possible.

Duplicate tests were made under each set of conditions. Reproducibility appeared to be good in most instances.

During most of the tests, measurement of the incoming and outgoing coolant temperature was made at intervals. Typical data are as follows:

Nine-Inch Cast Iron Plate, Air Cooled (Test 19)

Time From Casting, Min.	Inlet Air, °F.	Exist Air, °F.
0	114	111
10	121	125
20	121	132
30	122	136
40	120	137
60	121	137

Two-Inch Plate, Air Cooled (Test 38)

Time From Casting, Min.	Inlet Air, °F.	Exist Air, °F.
0	117	117
2	117	147
4	117	160
6	117	156
8	117	156
10	117	149
20	117	144
25	117	142

Two-Inch Plate, Water Cooled (Test 41)

Time From Casting, Min.	Inlet Water, °F.	Exist Water, °F.
0	65	65
2	65	68
6	65	68
8	66	68
10	66	68
15	67	67
20	67	67
25	67	67
30	67	67

* Dicalite is a commercially available product consisting of calcined diatomaceous earth.

The exit coolant reached its maximum temperature at about the same time that the bottom surface of the plate was at its maximum temperature. This is as would be expected. Data for the other tests are similar to those presented and are of the same order of magnitude.

Discussion of Experimental Results *

Nine-Inch-Thick, Air-Cooled, Cast Iron Plate. Figures 6, 7 and 8 are time-temperature curves for this plate under some of the conditions already listed in Table 1. Table 2 gives some of the pertinent data derived from these curves. By "maximum temperature difference" in this and succeeding tables is meant the greatest difference in temperature between the top and bottom thermocouples found during the test run. By "maximum change in temperature difference" is meant the difference between the maximum temperature difference and that temperature difference existing at the start of the test (the latter frequently being zero).

Results fell in the direction expected. Thinner mold coatings gave rise to higher temperatures, and the maximum temperature was reached more quickly. Thicker castings also gave rise to higher temperatures, but these temperatures were maintained for a longer time. The maximum temperature did not increase uniformly with casting thickness but approached a limit with the thicker castings.

Two-Inch-Thick, Air-Cooled, Cast Iron Plate. Figures 9 through 11 are the time- temperature curves for this plate under some of the conditions set forth in Table 1. Table 3 gives some the pertinent information derived from the curves. A mold coating 0.01-in. thick seemed to be rather ineffective in cutting down the maximum temperature, although it did increase slightly the time required to reach that temperature. The 0.04-in. coating was effective in keeping down the maximum temperature.

Although the maximum temperatures increased with increased casting thickness, the maximum temperature difference was a maximum with the 1½-in. thick casting. Since the maximum temperature and the time to reach that maximum temperature increased with casting weight, while the rate of temperature increase on the cold side of the mold during this interval remained practically constant, some intermediate casting size resulted in the lowest maximum temperature difference.

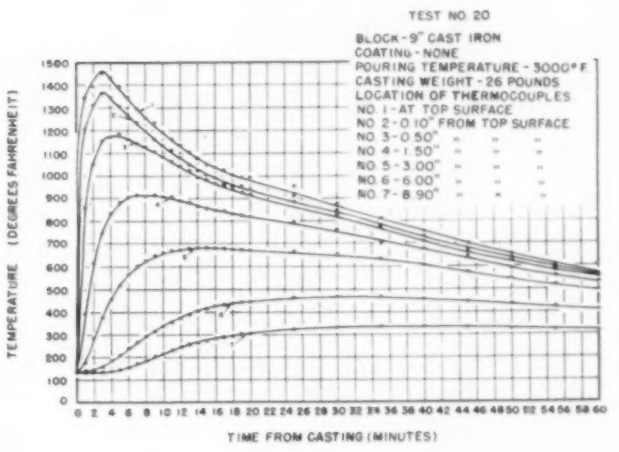
In other words, with light castings, the maximum temperature, though low, was reached before the cold side of the mold was affected, while the heavier castings, the maximum temperature was high and not offset sufficiently by the rise in temperature on the cold side. With castings of intermediate thickness, the maximum temperature was reached at a time when the cold side of the mold had also been heated appreciably.

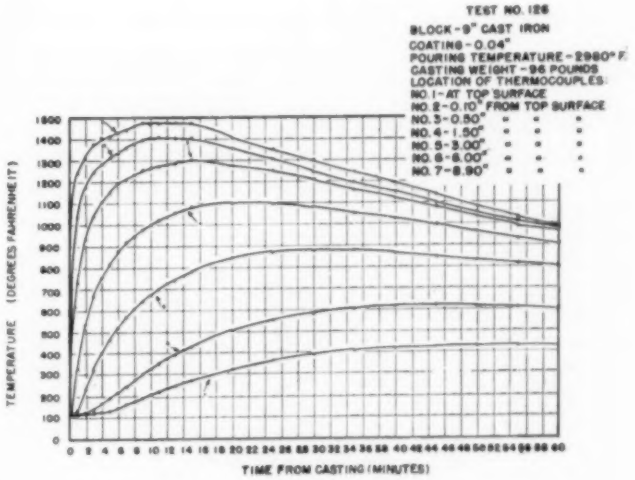
Two-Inch-Thick, Water-Cooled, Cast Iron Plate. Figures 12 through 16 are the time-temperature curves for the tests on this plate under some of the conditions listed in Table 1. Table 4 gives some of the pertinent data derived from the curves.

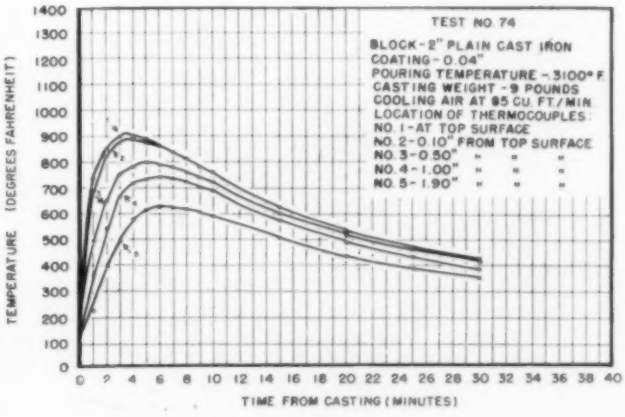
The effect of mold coating was as expected, the maximum temperature decreased and time to maximum temperature increased with increasing coating thickness. As with the air-cooled plate, the 0.01-in. coating was not very effective.

Increasing the casting thickness, increased the maxi-

Figs. 7, 8 and 9 (from top down)—Time-temperature curves for, respectively, tests 20, 126 and 74.



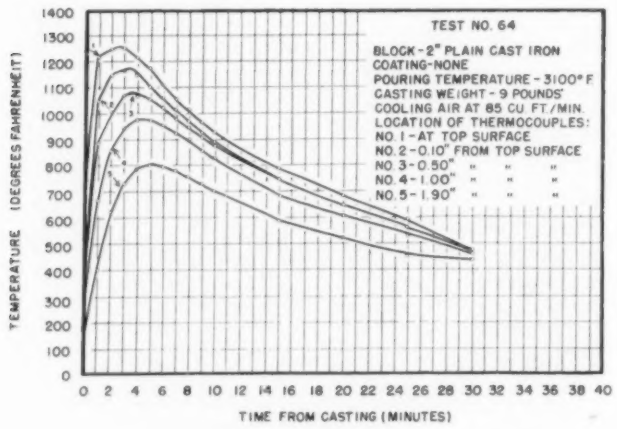


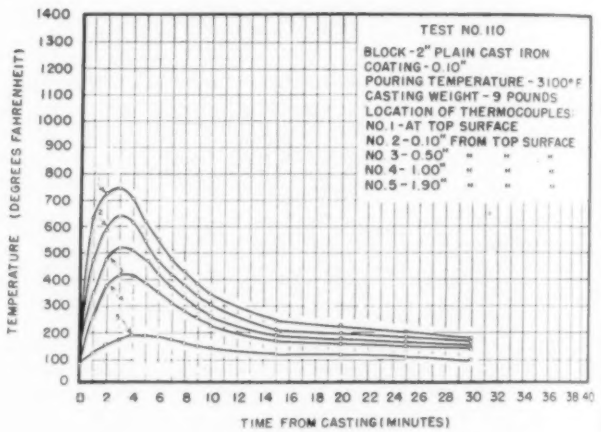


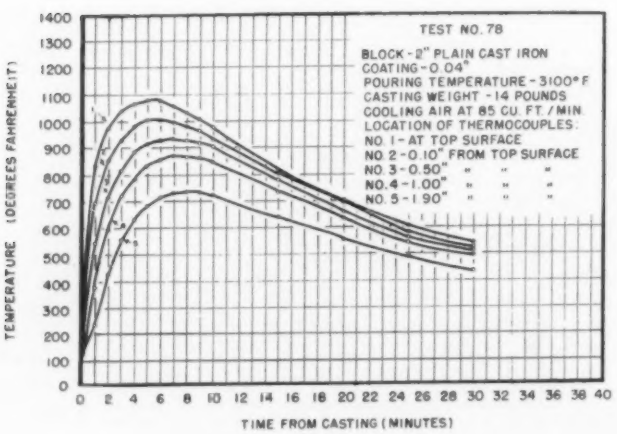
* For a more detailed presentation, see Final Report on "An Analysis of Heat Flow in Metal Molds for Centrifugal Casting of Gun Tubes, Airplane Cylinders, Tank Bogey Wheels, and Other War Materials." National Defense Research Committee of Office of Scientific Research and Development. OSRD No. 1935. Serial No. M-138, October 12, 1943.

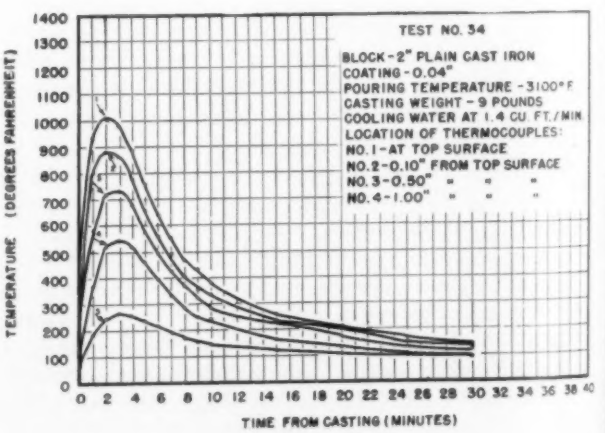
TABLE 3.—TEMPERATURE DISTRIBUTION DATA FOR THE PLAIN CAST IRON TWO-INCH PLATE, AIR COOLED AT 85 CFM.

Conditions for Tests					Data			
		Casting						
Test No.	Mold Coating In.	Intended Thickness, In.	Weight, Lb.	Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp. Difference, Min.
					EFFECT OF MOLD COATING THICKNESS			
72	0.04	1½	9.0	3100	980	3¾	500	1
74	0.04	1½	9.0	3100	917	3½	500	1
80	0.01	1½	9.0	3110	1160	2½	800	1
124	0.01	1½	9.0	3110	1067	3	720	1
64	None	1½	9.0	3100	1257	2½	800	1
68	None	1½	9.0	3100	1200	2½	705	1
					EFFECT OF CASTING WEIGHT			
95	0.04	1	5.0	3100	867	1½	585	1¼
123	0.04	1	5.0	3100	888	1½	540	1
72	0.04	1½	9.0	3100	980	3¾	500	1
74	0.04	1½	9.0	3100	917	3½	500	1
78	0.04	3	14.0	3100	1080	5½	610	1¼









Figs. 10 (top) and 11—Time-temperature curves for, respectively, tests 64 and 78.

Figs. 12 (top) and 13—Time-temperature curves for, respectively tests 110 and 34.

mum temperature and also the time to reach that maximum; however, the maximum temperature difference was observed for the 1-in. casting instead of for the 1½ in. casting, as was the case with the same plate air cooled.

With the six-inch casting, the mold surface began to cool earlier and at a faster rate than it did after pouring the three-inch casting. Also, the cooling curve remained at a fairly constant temperature during the six-to ten-minute interval after pouring. This observation was consistent in three tests with the six-inch casting. It is believed that this behavior of rapid cool-

TABLE 4.—TEMPERATURE DISTRIBUTION DATA FOR PLAIN CAST IRON TWO-INCH PLATE, WATER COOLED AT 1.4 CFM.

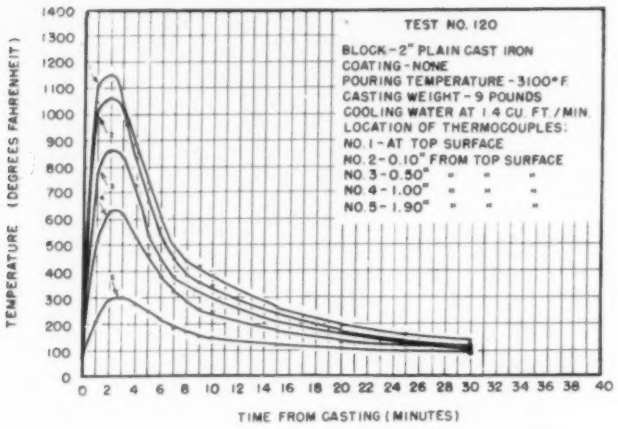
Conditions for Tests					Data			
		Casting						
Test No.	Mold Coating In.	Intended Thickness, In.	Weight, Lb.	Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp. Difference, Min.
					EFFECT OF MOLD COATING THICKNESS			
110	0.10	1½	9.0	3100	725	3	575	2¼
116	0.10	1½	9.0	3130	673	2¾	465	2½
32	0.04	1½	8.5	3100	846	2	670	1½
34	0.04	1½	9.0	3100	1017	2	770	1½
35	0.01	1½	9.0	3100	1160	1¼	925	1
41	0.01	1½	9.0	3100	1027	1¼	820	1
118	None	1½	9.0	3100	1090	1¾	815	1
120	None	1½	9.0	3100	1147	2	900	1
					EFFECT OF CASTING WEIGHT			
39	0.04	1	5.0	3100	716	1¾	525	1½
43	0.04	1	5.0	3100	702	2	515	1½
32	0.04	1½	8.5	3100	846	2	670	1½
34	0.04	1½	9.0	3100	1017	2	770	1½
51	0.04	3	13.5	3100	875	2¾	620	1½
53	0.04	3	13.0	3100	924	2¾	650	1½
129	0.04	6	30.0	2980	995	1½	885	1
128	0.04	6	30.0	3005	1060	1½	855	1

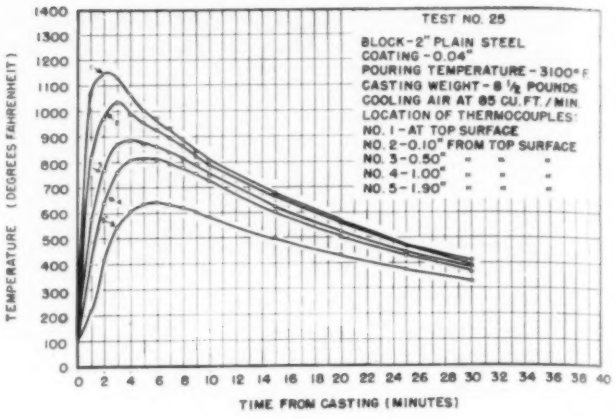
TABLE 5.—EFFECT OF COOLING WATER RATE ON THE TEMPERATURE DISTRIBUTION DATA FOR THE PLAIN CAST IRON, THE PLAIN STEEL, AND THE COPPER TWO-INCH PLATES.

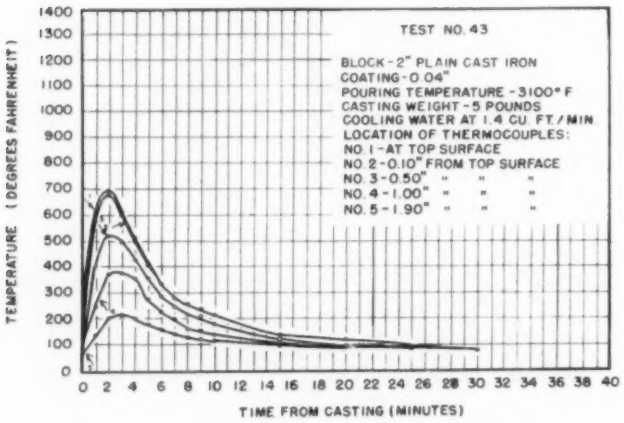
Conditions for Tests					Data			
			Casting					
Test No.	Cooling Water Rate Fps	Mold Coating In.	Weight Lbs.	Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp Difference,. Min.
					PLAIN CAST IRON TWO-INCH PLATE			
32	3	0.04	8.5	3100	846	2	670	1½
34	3	0.04	9.0	3100	1017	2	770	1½
28	9	0.04	8.5	3100	931	2	765	1½
30	9	0.04	9.0	3100	898	2	735	1¼
					PLAIN STEEL TWO-INCH PLATE			
57	3	0.04	9.0	3120	898	2	700	1¼
58	3	0.04	9.0	3120	830	2	610	1¾
67	9	0.04	9.0	3140	780	2	615	1¼
69	9	0.04	9.0	3120	738	2	565	1¾
					COPPER TWO-INCH PLATE			
98	3	0.04	9.0	3180	264	1	110	¾
100	3	0.04	9.0	3080	310	1	125	¾
105	9	0.04	9.0	3100	264	1	129	1
106	9	0.04	9.0	3100	260	1	123	1

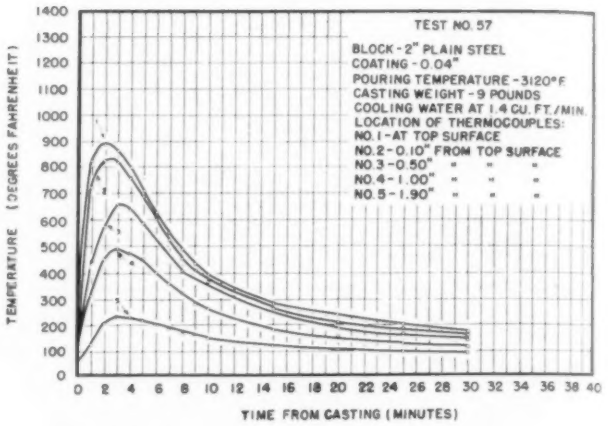
TABLE 6.—TEMPERATURE DISTRIBUTION DATA FOR THE PLAIN STEEL TWO INCH PLATE, AIR COOLED AT 85 CFM.

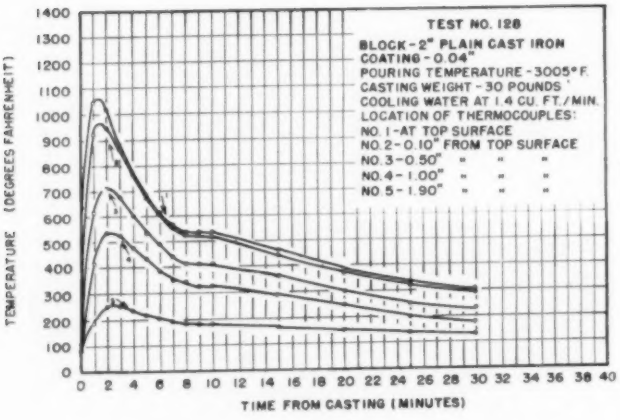
Conditions for Tests					Data			
		Casting						
Test No.	Mold Coating In.	Intended Thickness, In.	Weight, Lb.	Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp. Difference, Min.
					EFFECT OF CASTING WEIGHT			
36	0.04	1	5.0	3100	843	1½	575	1
38	0.04	1	5.0	3100	912	2	570	1
25	0.04	1½	8.5	3100	1151	2¼	850	1
102	0.04	3	14.0	3100	1189	4½	725	1¼

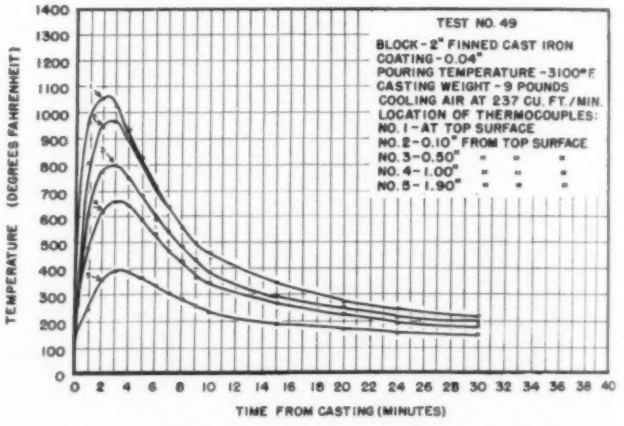












Figs. 14, 15 and 16 (from top down)—Time-temperature curves for, respectively, tests 120, 43 and 128.

Figs. 17, 18 and 19 (from top down)—Time-temperature for tests 25, 57 and 49, respectively.

ing after reaching the top temperature resulted from cooling of the metal while the bulk was still in the molten state,

The interruption in cooling after six minutes was thought to be caused by release of heat during freezing of the unsolidified steel. The more gradual cooling after pouring the three-inch casting can probably be explained by the release of heat on solidification at the same time the maximum mold temperature was being reached.

In Table 5, the effect of water velocity is shown for this plate and also for the steel and copper plates. For the cast iron plate, changes in water rate, in the range investigated, had little effect.

Two-Inch, Air-Cooled, Steel Plate. Figure 17 is a time-temperature curve for one of the tests on the air-cooled steel plate (see Table 1). Table 6 contains the pertinent data taken from these tests.

The effects of increasing casting weight were to increase the maximum temperature reached, to increase the time to reach that temperature, and to reduce the cooling rate.

Two-Inch, Water-Cooled, Steel Plate. Figure 18 is a time-temperature curve for one of the tests on the

Table 7.—Temperature Distribution Data for Plain Copper Two-Inch Plate, Air Cooled at 85 CFM. and Water Cooled at 1.4 CFM.

Conditions for Tests				Data			
Test No.	Mold Coating, In.	Casting Weight, Lb.	Casting Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Temp. Difference, °F.	Time to Max. Temp. Difference, Min.
				Air Cooled—Effect of Mold Coating Thickness			
108	0.04	8.5	3050	691	5	111	1
109	0.04	9.0	3100	774	4½	122	½
111	0.01	9.0	3120	935	4	169	½
115	0.01	9.0	3100	960	2½	187	1
117	None	9.0	3100	925	3	244	½
119	None	9.0	3100	908	3	236	½
				Water Cooled—Effect of Mold Coating Thickness			
98	0.04	9.0	3180	264	1	110	¾
100	0.04	9.0	3080	310	1	125	¾
101	0.01	9.0	3080	479	½	250	¼
104	0.01	9.0	3100	453	½	225	¼
121	None	9.0	3100	1066	about 3 sec.	1066	about 3 sec.
122	None	9.0	3100	868	about 2 sec.	868	about 2 sec.

Table 8.—Comparison of Temperature Distribution Data for the Various Plates (0.04 In. Dicalite Coating, 1½ In. Casting)

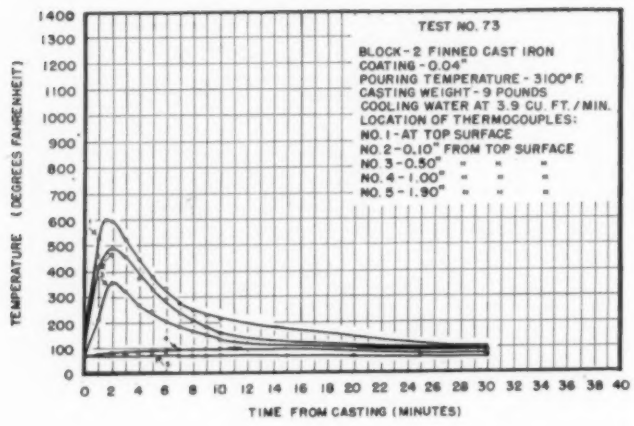
Plate	Test No.	Cooling	Pouring Temp., °F.	Max. Temp., °F.	Time to Max. Temp., Min.	Max. Change in Temperature Difference, °F.	Time to Max. Change, Min.
9 in. Cast iron	8	Air[1]	2965	1084	3	997	3
	9		3010	1044	4	914	4
2 in. Cast iron	72	Air[3]	3100	980	3¾	500	1
	74		3100	917	3½	500	1
	32	Water[2]	3100	846	2	670	1½
	34		3100	1017	2	770	1½
2 in. Finned cast iron	46	Air[4]	3100	865	2¼	625	1¾
	49		3100	1059	2¼	720	1
	73	Water[2]	3100	601	1½	525	1½
	77		3100	575	1½	495	1½
2 in. Steel	25	Air[3]	3100	1151	2¼	850	1
	57	Water[2]	3120	898	2	700	1¼
	58		3120	830	2	610	1¾
2 in. Finned steel	52	Air[4]	3100	998	2½	670	2
	54		3100	1065	3	730	1½
	79	Water[5]	3100	820	1¼	750	1¼
	81		3100	630	2	550	2
2 in. Copper	108	Air[3]	3050	691	5	111	1
	109		3100	774	4½	122	½
	98	Water[2]	3180	264	1	110	¾
	100		3080	310	1	125	¾

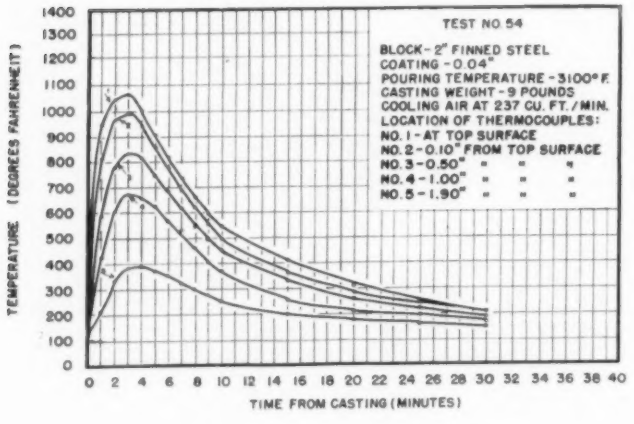
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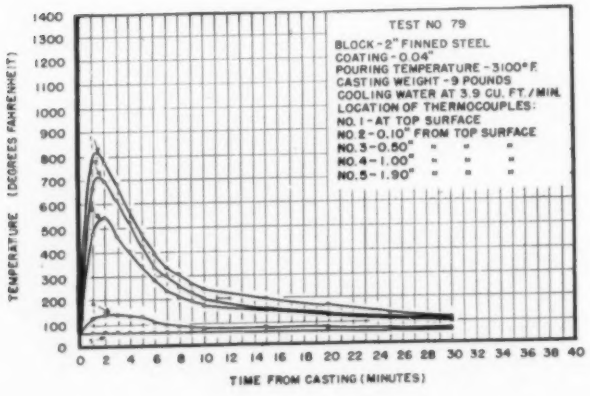
water-cooled steel plate (see Table 1). Pertinent data are given in Table 5. An increase in rate of water flow seemed to be effective to a small degree in reducing maximum temperatures.

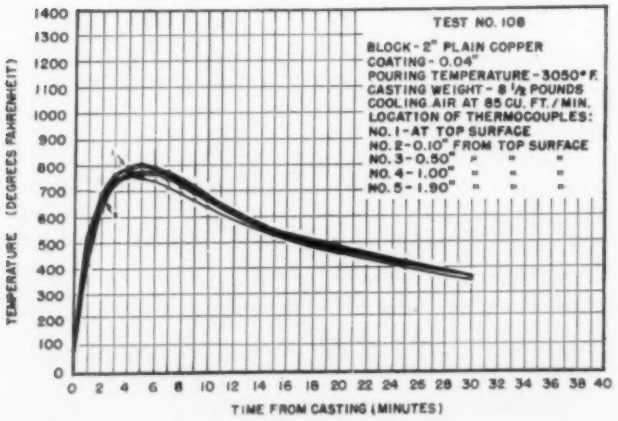
Two-Inch-Thick, Finned Plates, Cast Iron and Steel, Air and Water Cooled. Figures 19 through 22 are time-temperature curves for the two-inch, finned plates (see Table 1).

The temperatures within the air-cooled, finned plates of both cast iron and steel differed very little from those within the corresponding solid plates under similar conditions. On the other hand, the finned

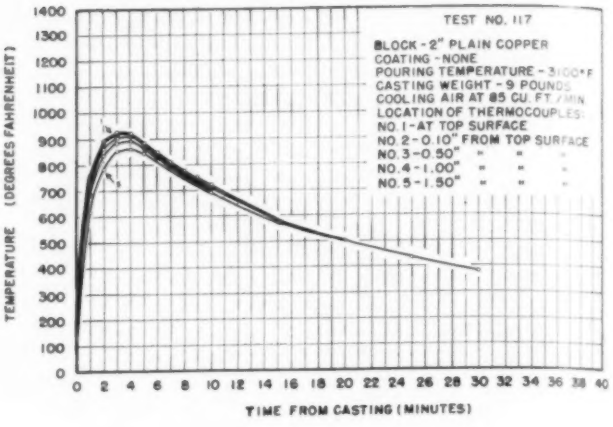


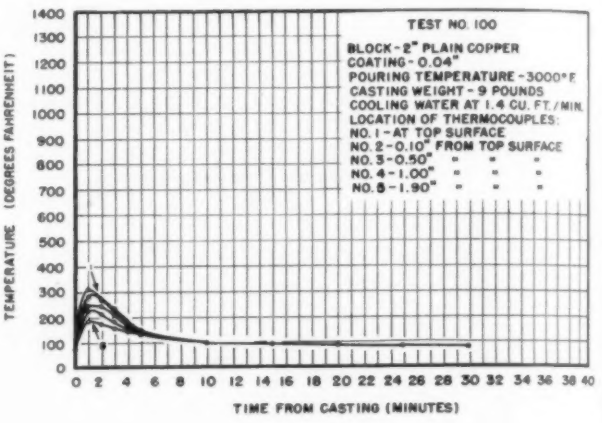


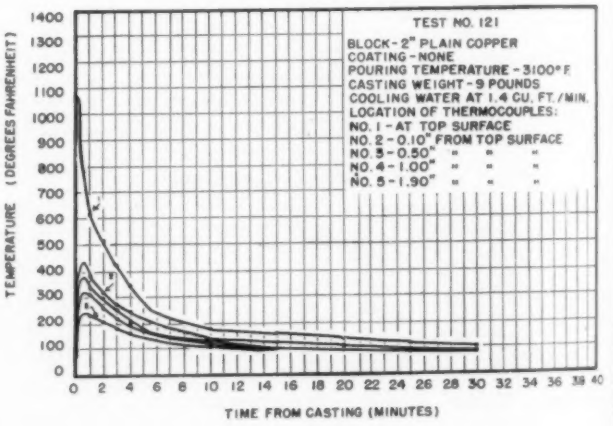




Figs. 20, 21, 22 and 23 (from top down) —Time-temperature curves for tests 73, 54, 79 and 108, respectively.







Figs. 24, 25 and 26 (from top down) —Time-temperature curves for, respectively, tests 117, 100 and 121.

plates, when water cooled, tended to be at lower temperatures than the water-cooled solid plates under corresponding conditions.

Two-Inch Copper Plate, Air and Water Cooled. Figures 23 through 26 are time-temperature curves for some of the tests on the copper plates with air and with water cooling (see Table 1). Table 7 contains pertinent data from these curves.

An important change resulting from the use of a copper plate was the accentuation of the effects of the mold coating. In the case of the copper plate, the maximum temperature was reduced much more by

the 0.04 in. of mold coating than was the case with either the cast iron or the steel plates. This effect was particularly noticeable when the copper plate was water cooled. The 0.01 in. of coating on the air-cooled copper plate was relatively ineffective in reducing the maximum temperature, but on the water-cooled copper plate this thickness of coating lowered the maximum temperature considerably.

The maximum temperature differences across the copper plates were never very large in any test.

An increase in the rate of water flow increased slightly the maximum temperature difference (see Table 5).

Other Comparisons. Temperature distribution data for the various plates, both air and water cooled, and coated with a layer of refractory wash 0.04 in. thick, upon which steel to a thickness of 1½ in. was cast, are compared in Table 8.

Temperatures measured on steel plates tended to be slightly higher than those measured on cast iron plates under corresponding conditions. Temperatures on both steel and cast iron plates were much higher than those measured on copper plates under corresponding conditions.

Water cooling as compared to air cooling was detrimental (in respect to maximum change in temperature differences) in all cases where an effective thermal barrier on the hot side of the mold was used. A 0.04-in. thick coating was effective as a thermal barrier in almost all cases.

The two-inch cast iron plate was the only one that could be compared logically with the nine-inch cast iron plate (see Table 2 and 3). Although the maximum temperatures reached were not far different in the two cases, the maximum temperature difference was much greater for the thick plate. Counteracting this to a large extent was the longer time to reach maximum temperature for the thick plate. A disadvantage of the thick plate was the longer cooling time.

Conclusions

The results of the investigation would indicate the following:

1. A thin plate offers the following advantages over the thick plate:

a. Less metal (and the operating advantages resulting from this).

b. Lower temperature and smaller temperature-difference changes (probably resulting in longer life).

c. Faster cooling time.

The chief disadvantage of the thin plate as compared to the thick plate is the faster realization of the maximum temperature difference giving less time for stress relief.

2. The finned plates tested offer no particular advantage over the solid plates, although in the case of water cooling, the maximum temperature was lowered slightly.

3. A mold coating of approximately 0.04 in. thickness is effective in reducing mold temperature, and also in increasing the time to maximum temperature. A coating 0.01 in. thick is only slightly effective in most cases.

4. An increase in casting weight increases the maximum temperature, the maximum temperature difference, and the time to maximum temperature. There appears to be a critical thickness of casting above which a further increase in thickness does not affect the temperature difference in the mold. This observation may not be of general application, but may apply only to the experimental conditions that were investigated, where the increased casting thickness increases the quantity of heat lost through parts other than the mold wall.

5. The temperature rise in a steel plate seems to be somewhat higher than in a cast iron plate for corresponding conditions.

6. An increase in thermal conductivity of the plate (copper as compared to cast iron or steel) is beneficial with air cooling. This benefit is brought about by higher temperatures on the cold side which reduce the temperature differences within the plate. In the case of water cooling, increased conductivity is definitely detrimental, on the basis of temperature differences, unless accompanied by a good thermal barrier on the hot side of the plate. The temperature differences are then reduced by a reduction in temperature on the hot side.

7. An increase in the rate of water flow within the range employed in the investigation is of little consequence. If anything, on the basis of temperature differences, an increase in water flow appears to be slightly detrimental from the fact that it lowers the cold-side temperatures without affecting the hot-side temperatures.

8. Water cooling tends to hold the cold-side temperature constant, thereby effecting an increase in the maximum temperature difference. This is particularly evident in the case of the copper plate. It can be offset by coating the hot side of the plate with a thermal barrier which reduces the maximum temperature.

9. Because of relatively large heat losses through parts other than the mold wall, the experimental results obtained are relative only. In a mold, the heat loss through the mold wall should be larger. In centrifugal casting practice, for example, where the inside surface of the casting is radiating heat against itself, and the end areas are relatively small compared with the surface in contact with the mold, most of the heat is dissipated through the mold wall. In the experimental arrangement, an appreciable quantity of heat was dissipated to the air and to the sand-core ring surrounding the casting.

Acknowledgment

This paper is based on a research program sponsored by the National Defense Research Committee of the Office of Scientific Research and Development as NDRC Research Project NRC-33. Grateful acknowledgment is made to the Office of Scientific Research and Development for their support and for their permission to publish this work, to the War Metallurgy Division of NDRC, and to the War Metallurgy Committee for supervising and directing the work, and to various members of the Battelle Staff, particularly C. H. Lorig and C. T. Greenidge, for their helpful contributions.

DISCUSSION

Chairman: J. B. CAINE, Sawbrook Steel Castings Co., Cincinnati.

Co-Chairman: C. K. DONOHO, American Cast Iron Pipe Co., Birmingham.

G. VENNERHOLM:[1] The subject is of vital importance for anybody that uses metal molds for centrifugal casting. We did a lot of work during the war in casting cylinder barrels for our Pratt & Whitney engines, and in order to control the uniformity and the rate of cooling, we found it necessary to cool the molds before each cast.

In other words, we had a continuous casting machine consisting of individual carriages coupled together, each fitted with a centrifugal spinning arrangement. Each spinner consisted of a set of rollers driven by a motor mounted on the individual carriages and the mold was placed on these rollers and rotated while the metal was poured in and during solidification. At the shakeout station the mold was actually lifted off, the casting removed and the mold passed through a water spray and cooled to below 300° F. and then reheated to 350° F. before being returned to the conveyor.

Possibly a lot of this could have been eliminated if time had been available for the necessary research. As the problem, however, was one of getting the castings out in a hurry we did the best we could and it worked very satisfactorily. We actually made three castings per minute per machine; two of which were in operation.

It may be of interest here to mention the approximate analysis of the molds used as we spent a considerable amount of time in trying various materials. The best results were obtained with the material containing: carbon 0.18 to 0.23, manganese 0.40 to 0.60, chrome 1.50 and molybdenum .50; in other words, a steel mold which had excellent resistance to heat checking.

CO-CHAIRMAN DONOHO: How thick is this mold in relation to the casting?

MR. VENNERHOLM: The casting wall thickness was approximately ½ in. and the mold about 1½ in.

CO-CHAIRMAN DONOHO: I might add along that line that we did much the same sort of casting, using a cast iron mold about four times as thick as the casting, and with an air cooling only between casts. With our set-up the experience was that the cast iron was less prone to cracking by heat shock than the steel molds we tried. Probably the control of mold temperature makes all the difference as to which mold material works best.

MR. VENNERHOLM: It would have been impossible with our molds to run it continuously. The success was in the cooling operation between casts, which this material permits.

CHAIRMAN CAINE: One thing that struck me is the possibility that this paper contains a great deal of basic information on heat extraction through chills, and I would like to ask whether there is any reason why we could not use some of this information for studies of heat extraction into chills.

MR. UDY: As I tried to point out in the paper, our actual figures were relative only, but I think the same relation would be applicable to chills as well as to metal molds.

CO-CHAIRMAN DONOHO: Did you make any observations on the condition of the steel, the surface, etc., for these different conditions and with the different coating thicknesses? The mold is only half the story; we have to consider the casting, too.

MR. UDY: Our work was done with 0.30 carbon, 0.60 manganese steel, aluminum killed. We discarded considerable data because bubbles were formed immediately over the thermocouple's location. We overcame that difficulty by being more careful in drying our mold coating and by melting down under slightly oxidizing conditions.

CO-CHAIRMAN DONOHO: Did thicker mold coatings show more tendency to blow or form pinholes.

MR. UDY: No, the tendency was about equally evident for all thicknesses used.

A. K. BEEDLE:[2] Why did you pick a 0.040-in. wall thickness? Generally when we get a mold, we go down to 0.002 in., to maybe 0.010 in. maximum.

MR. UDY: We wanted enough variation to establish a trend. We actually used three thicknesses, the bare mold, 0.01 and 0.04 in. We found it hard to put a coating on under 0.01 in. and still be able to accurately reproduce the thickness of that coating.

CO-CHAIRMAN DONOHO: There is data in the paper on coatings of 0.01 in.

MR. VENNERHOLM: Was the coating sprayed on and the mold dried?

MR. UDY: It was sprayed on with the mold around 200° F., so it would not bubble, and then further torch dried at around 350° F.

MR. VENNERHOLM: It occurs to me, that the thickness seems to be greater than we would normally use. We like a rather thin wash, because of the danger of a heavier wash peeling off and interfering.

MR. UDY: I have heard of applications where instead of using a wash, dry silica sand is put into the mold and that serves as a refractory coating. The thickness of that would be about five millimeters.

R. E. KERR:[3] Would a comparison between this data and chills apply? I believe in this data you have a metal mold conducting heat from the molten mass to the air, so conduction would be a major factor, and, of course, radiation from the metal into the air, whereas with the chill, you would have a small metal mass completely surrounded by sand. Usually the metal mass would heat up rather rapidly, with smaller gradation of temperature across the surfaces. In my estimation, the chill would act more as a heat condenser, whereas the metal mold more as a heat conductor.

[1] Ford Motor Co., Dearborn, Mich.

[2] Aluminum Company of America, Cleveland.

[3] Pettibone Mulliken Corp., Chicago.

ESTABLISHMENT AND USE OF STANDARD DATA

TIMESTUDY TECHNIQUES

By

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Superintendent of Standards
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ABSTRACT

This paper presents some fundamentals for taking timestudies in molding for correct interpretation of timestudy data. The various timestudy elements are defined and discussed, actual test data being used in the examples.

The logical method for setting standards is by formula derived from analysis of enough timestudies on any one operation to cover all of the variations on elements normally encountered.

THE PURPOSE OF THIS PRESENTATION is not to go into a comprehensive study of all the details of timestudy, but rather to present a few fundamentals which experience has shown us to be necessary for taking good timestudies, for the correct interpretation of the timestudy data, and for its consistent and logical application in setting incentives.

The elements to be considered in the discussion are,

1. Pace Rating (or leveling) in a timestudy and the establishment and maintenance of consistent "Bench Marks" by which to measure pace rating.
2. The proper use of Fatigue Factors.
3. The timestudy sheet and the comparison sheet.
4. Deriving a formula from the timestudy data.
5. Setting standards from the formula.

Pace Rating

Pace rating (or leveling) is an attempt on the part of the timestudy observer to measure the effort and performance of the worker being studied so that all observations can be reduced to terms of normal, or standard work time. Leveling is a fundamental step in setting up consistent incentives. However, without a yard stick to measure by, the pace rating of the observer is too much a matter of personal judgment and is subject to too many variations due to the human element and to the abstract nature of the problem.

To get one step closer to consistent pace rating and ultimately to better incentives, it is necessary to establish and maintain certain standards of performance for elementary movements of the body which are considered normal or standard. These are termed Bench Marks of performance. Then, by repeated observations of these clocked movements, the observer learns to judge first, a normal pace, and secondly, to what degree or percentage any faster or slower movement varies from the normal.

After the initial training of the timestudy observer in this technique, his sense of values or ability to judge normal performance may change or grow stale. Such a change may take place over a long period of time and be almost imperceptible, and will, in time, result in consistently loose timestudy data. Hence it is important to go through the routine of measuring and adjusting the observers' sense of judging standard performance periodically by the Bench Marks originally set up.

Basic Normal Paces

The following are the basic normal paces for the various elementary movements of the body most often encountered in timestudy work. These have been used repeatedly in timestudy as a basis for normal performance with uniformly good results. From these basic normal paces a Bench Mark performance routine may be set up. Then, with a clear picture in mind of a normal pace for various single movements of the body, the observer can logically rate more effectively of two or more movements at a time.

1. WALKING at a normal pace is 4½ feet per second or 45 feet in 10 seconds. When rating and judging this pace it must be remembered that it is a normal pace for an entire day and not just for a few minutes, and is such that an average person in good physical condition can easily keep up the pace for an entire day without undue fatigue.

2. WRIST MOVEMENT. While sitting in a comfortable position at a table, take a pack of 26 cards in one hand. From that position, if all of the cards are dealt into four piles on the table, reasonably close together, in 14 seconds, the dealing hand has moved at a normal pace.

3. FOREARM MOVEMENT. Swing the forearm in a 90° arc, either horizontally or vertically, keeping it straight at the wrist and without moving the upper part of the arm. The pace is normal for this movement when 10 complete cycles (round trips) are made in 8.3 seconds.

4. WHOLE ARM MOVEMENT. Swing the arm from the shoulder through a 90° arc, either horizontally or vertically, keeping it straight at the elbow. The pace is normal when ten complete cycles are completed in 14.3 seconds.

TABLE 1.—TABLE FOR RATING WITH DECIMAL STOP-WATCH PACES OTHER THAN NORMAL

Walking 52.8 ft.		Forearm		Card Dealing		Whole Arm		Torso	
Rating	Time	Rating	Time	Rating	Time	Rating	Time	Rating	Time
—	—	.50	.280	.50	.460	.50	.480	.50	.900
—	—	.60	.233	.60	.384	.60	.400	.60	.750
—	—	.70	.200	.70	.328	.70	.343	.70	.642
.75	.267	.75	.187	.75	.306	.75	.320	.75	.600
.80	.250	.80	.175	.80	.287	.80	.300	.80	.562
.85	.236	.85	.165	.85	.270	.85	.282	.85	.530
.90	.222	.90	.156	.90	.255	.90	.266	.90	.500
.95	.211	.95	.148	.95	.242	.95	.252	.95	.475
1.00 Std.	.200	1.00 Std.	.140	1.00 Std.	.230	1.00 Std.	.240	1.00 Std.	.450
1.05	.190	1.05	.133	1.05	.219	1.05	.228	1.05	.428
1.10	.182	1.10	.127	1.10	.209	1.10	.218	1.10	.408
1.15	.174	1.15	.122	1.15	.200	1.15	.208	1.15	.391
1.20	.167	1.20	.117	1.20	.191	1.20	.200	1.20	.375
1.25	.160	1.25	.112	1.25	.184	1.25	.192	1.25	.360
1.30	.154	1.30	.107	1.30	.176	1.30	.185	1.30	.346
1.35	.148	1.35	.103	1.35	.170	1.35	.178	1.35	.333
1.40	.143	1.40	.100	1.40	.164	1.40	.171	1.40	.321
1.45	.138	1.45	.097	1.45	.158	1.45	.165	1.45	.310
1.50	.133	1.50	.093	1.50	.153	1.50	.160	1.50	.300
1.60	.125	1.60	.087	1.60	.144	1.60	.150	1.60	.281
1.70	.118	1.70	.082	1.70	.135	—	—	—	—
1.80	.111	1.80	.078	1.80	.128	1.80	.133	1.80	.250
1.90	.105	1.90	.074	1.90	.121	—	—	—	—
2.00	.100	2.00	.070	2.00	.115	2.00	.120	2.00	.225

5. TORSO MOVEMENT. Bend the upper part of of the body forward at the hips from a vertical to a horizontal position through an arc of 90° and return to a vertical position. The upper part of the body is moving at a normal pace when ten complete cycles are made in 27 seconds.

From these normal paces Table 1 has been made up by which paces other than normal can be rated with a decimal stop watch. In the tables, ratings are expressed in decimals. They can also be expressed as a percentage of normal. That is a rating of 1.10 equals 110 per cent of normal, and a rating of 0.80 equals 80 per cent of normal.

Figure 1 is a sample score sheet which can be used for testing and adjusting the observers' ability to pace rate correctly.

It is fairly easy to judge a pace that is near normal. Paces slower than 75 per cent of normal or faster than 125 per cent of normal are more difficult to judge. Therefore in scoring the score sheet an error of 5 per cent in the rating is considered correct when the actual pace is between 75 per cent and 125 per cent of normal. On the other hand, when the actual pace is slower or faster than the 75 to 125 per cent limits, an error of 10 per cent in the rating should be considered correct.

An average score of more than 25 per cent wrong means that more practice at rating is needed.

Fatigue Allowances

One of the greatest causes of erratic and inconsistent incentives in the past has been the irrational application of certain allowances in excess of the normal time for doing a job.

It is generally accepted practice to make a 5 per cent allowance over normal for the personal needs of the worker. However, the indiscriminate use of other blanket allowances, or the improper application of any allowances will result in inconsistent incentives and give rise to the complaint on the part of certain workers that their jobs are too "tight" in comparison with some other jobs.

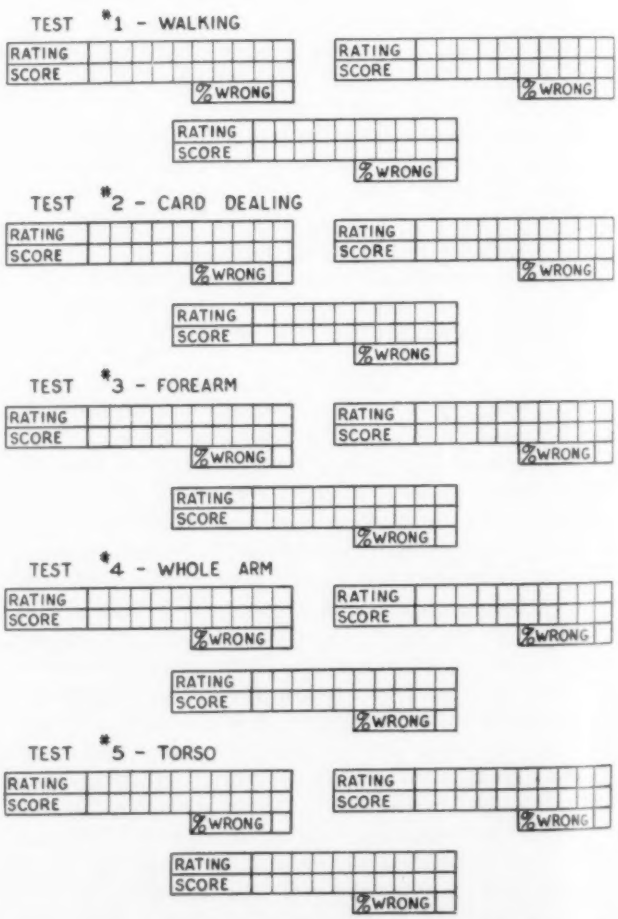


Fig. 1—Score sheet for adjusting observers' ability to pace rate correctly.

If they occur, allowances should be made for such factors as inherent delays, supplemental operations not covered by the operation studied, flow time where two or more men or groups must coordinate their work, or preparation and cleanup. However, such allowances should never be made without adequate study and serious consideration of all the elements involved. In making any allowances over the normal time for doing a job, remember that once a precedent is set it is difficult and sometimes impossible to eliminate or change it.

Experience has shown that a blanket allowance for Fatigue or Relaxation, which has been common practice in the past, has contributed much to inconsistent incentives.

If all jobs were equally fatiguing, the allowance might be disregarded completely and an overall adjustment made in the rate of pay for the time allowed. (This would be a factor in Job Evaluation). Obviously, some jobs are more fatiguing than others, and in any one job some elements will require a high amount of exertion while others have little or none. The operator who shovels sand from a heap needs a much higher fatigue allowance than one who uses a sand slinger or has the sand delivered by an overhead conveyor. Similarly the operator who carries out his molds from the bench puts forth more effort than one who merely slides his mold onto a conveyor.

The proper method of applying fatigue allowances is not on an overall basis, but rather by adapting it to the various types of movement involved and varying it with the force exerted or the weight handled by the operator. Since the timestudy breaks each job down into its elements, and since the standard data from which the formula is derived is built up in a like manner, it is not difficult to apply fatigue factors in this way.

Table 2 is a general table of fatigue factors including a 5 per cent personal allowance. This table can be used as a guide in making elemental allowances in the standard data. The marked differences shown in the table between the smallest and the greatest fatigue allowance emphasizes the inconsistencies that can result from the use of blanket allowances for fatigue in all operations and on all classes of work. The writer does not claim that this table is scientifically correct. Because of the complicity of factors involved it is doubted whether any similar data will ever be proved scientifically. In the writer's opinion, however, if used with reason and judgment, the values in the table will give satisfactory results, and refinement of data must eventually come through use and experience.

Table 2 is carried into weights not normally handled to any great extent, thus insuring that all factors are adequately covered. Consistent necessity for using the very high factors will indicate that labor saving equipment might be installed to good advantage.

Use of the factors in Table 2 will be demonstrated later in the discussion.

TABLE 2.—TABLE OF FATIGUE VALUES

Part of the Body Used and Type of Movement	Force Exerted or Weight Handled in Lb. 0–10	11–30	31–50	51–75	76–100	101–125	125 up
Hands and Fullarm	1.06	1.08	1.14	1.24	1.39	—	—
Hands, Fullarm and Shoulders	1.07	1.09	1.17	1.30	1.50	—	—
Hands, Fullarm, Shoulders and Back partly bent (less than 45°)	1.08	1.10	1.19	1.33	1.54	1.80	2.10
Hands, Fullarm, Shoulders and Back partly bent (more than 45°)	1.09	1.12	1.22	1.40	1.65	1.97	2.35
Carryout and Close	1.09	1.12	1.22	1.40	1.65	1.37	2.35
Carrying an Object Walking	1.07	1.09	1.18	1.32	1.52	1.75	2.00

Delay Allowances

Table 2 gives the "Personal" and "Fatigue" allowances that can be used in the incentives derived from timestudy data. As stated before, other allowances, if they occur, should be made. In this discussion other allowances such as inherent delays and supplemental operations will be grouped under the heading Delay, and the total of these three allowances is then labeled PFD for brevity.

Delay allowances should be determined on a percentage basis by all day studies on the particular operation under consideration. From such time studies, such elements of delay as can be, are eliminated by changes in shop methods, and whatever delays or supplemental operations cannot be so eliminated are allowed in the final standard as a percentage of the total work day. Such allowances must be carefully considered from all angles before being made.

The Timestudy Form

Figure 2 is an example of one type of timestudy form. This can easily be duplicated on any ditto machine or mimeograph.

The headings at the top of the form are self-explanatory. It is important, when the observer takes a study on any job, to write down all the specifications of the job. The specifications should include such items of information as pattern number, flask size, type of sand, depth of pattern draw, depth of cope lift or any other data effecting the timestudy time that is not normally noted during the course of the study. If necessary, rough sketches should be made on the back of the timestudy sheet. More than enough of this type of information is much better than too little. Later, when the data is to be analyzed, lack of a vital bit of information may invalidate the use of a part or all of the study.

In taking the study, the time, in hundredths of a minute, is entered opposite the element on the line marked T (time) in each of the ten numbered columns, Fig. 2. The line marked No. (number) is for recording the number of items involved in the time where such information may later prove useful, such as, shovels of sand, number of vents, number of gates cut, gaggers set, etc.

It is not necessary to record ten complete cycles to get a good study. However, the more cycles taken the better the results will be.

The Total column is for convenience in making the calculations when the study is worked up. The

TIMESTUDY DATA SHEET

DEPT. _____ OPERATION _____ STUDY NO. _____

SPECIFICATIONS _____ OPERATOR _____ SHEET NO. ___ OF ___

_____ CLOCK NO. _____ DATE _____

_____ OBSERVER _____ START TIME _____

_____ APPROVED BY _____ STOP TIME _____

ELEMENT DESCRIPTION		1	2	3	4	5	6	7	8	9	10	TOTAL	AVE.	L. F.	LEVELED TIME	P.F.D.	STAND.
	T																
	NO																
	T																
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REMARKS _____

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Fig. 2—Sample of Timestudy Data Sheet.

Average is obtained by dividing the Total by the number of cycles.

The Column marked L. F. (leveling factor) should be filled in by the observer when the study is being taken or immediately thereafter while still watching the operator at work. The leveling factor is a composite or average rating of the operator over all of the times recorded for each element. Another method of leveling called "Speed Rating" is a valuable check on the overall method usually employed. In Speed Rating the observer will note on any single elemental time in the study the leveling factor for that time alone. The nearer Normal the pace of the operator is the more accurate the Speed Rating will be. Later, when the study is worked up, the speed rated single time multiplied by its LF should equal the average of all the times multiplied by the overall LF.

The column marked Leveled Time is the product of the Average times the leveling factor used for each element. This Leveled Time, also known as Normal Time, is the figure entered on the Comparison Sheet which is used in analyzing the timestudy data and setting up the final standard formula.

If it is necessary to set a standard directly from the timestudy, the column marked PFD is provided to enter the combined Personal, Fatigue and Delay factor, which, when multiplied by the Leveled Time, results in the Standard for the element. This is entered in the last column on the timestudy form. The total of the elemental standards will then give the standard in minutes for the job. This can then be converted into whatever terms it is most desirable to use. For example:

1. To get Standard Hours for the job, divide the standard in minutes by 60.
2. To get pieces per hour for the job, divide 60 by the standard minutes.
3. To get a PW Price multiply the standard minutes by the base hourly rate divided by 60 (i.e. Base Rate per minute).

TABLE 3.—COMPARISON SHEET—SNAP FLASK, MATCH PLATE MOLDING

Time Study Number	1	2	3	4	5	6	7	8	9	10
Pattern Number	C7763	115Q	57-1-2½	B69304C	T1011	C7765	1504A	1092	0	P
Flask Size, in.	10x14x4/4	11x18x5/5	11x18x5/5	14x14x5/6	14x14x5/5	13½x18x5/5	13½x18x5/5	12x20x6/7	16x16x7/5	16x16x7/5
Assemble Flask and Match	.206	.274	.296	.147	.275	.243	.250	.370	.289	.145
Band In (Drag)	—	.137	.053	.116	.104	.089	.053	.105	.109	.118
Riddle Facing "	.233	.301	.340	.224	.143	.186	.304	.270	.290	.343
Heap "	.213	.260	.135	.216	.319	.379	.195	.340	.272	.254
Peen "	.128	.133	.193	.180	.115	.124	.154	.215	.192	.234
Level Sand and Board On "	.088	.115	.099	.136	.268	.232	.234	.266	.168	.173
Jolt "	.028	.033	.078	.058	.063	—	.065	.150	.113	.063
Rollover "	.051	.104	.116	.165	.104	.101	.097	.135	.092	.124
Band In (Cope)	—	.079	.055	.070	.097	.085	.095	.115	.069	.069
Riddle Facing "	.186	.310	.257	.206	.231	.224	.223	.240	.292	.351
Heap "	.198	.206	.178	.143	.240	.270	.173	.395	.358	.326
Peen "	.112	.130	.121	.134	.154	.157	.227	.265	.323	.378
Level Sand and Board On "	.189	.125	.138	.245	.310	.155	.165	.330	.224	.192
Hand Pack Facing	.040	.058	.083	.042	—	—	—	.355	.045	.061
Squeeze "	.075	.070	.119	.090	.100	.082	.110	.110	.097	.115
Board Off "	.094	—	.075	.055	.060	.059	.062	.085	.063	.046
Cut Sprues	(2).218	(1).233	(1).188	(1).190	(1).133	(2).156	(3).614	(1).160	(2).249	(5).626
Vents	(15).084	(10).063	(20).099	(30).136	(25).120	(13).092	(30).193	(20).120	(25).192	(25).163
Blow Off Cope	.130	.115	.108	.212	.099	—	.300	.185	.314	.161
Cope Off and On Table	.164	.077	.080	.118	.253	.093	.209	.320	.138	.176
Clean Sprues	(2).220	(1).117	(1).077	(1).099	(1).200	(2).152	(3).234	(1).180	(2).227	(2).322
Blow Off Match	.064	.071	.069	.076	.088	—	.144	.170	.121	.066
Thru Vent	—	—	—	(2).134	(13).561	—	—	(3).285	—	(2).154
Draw Pattern	.117	.095	.168	.143	.132	.187	.121	.150	.171	.168
Patch and Finish Mold	.080	—	.340	.052	.110	.123	.083	.289	.157	.505
Set Small Core	(2).336	(1).229	—	(1).223	—	(2).246	(1).180	(1).180	(1).181	—
Set Nails	(5).211	—	—	—	—	(1).053	—	(7).440	(2).173	—
Mold Out, Drag and Cope	.138	—	.168	—	.116	—	.174	—	—	—
Drag Out and Position	—	.157	—	.134	—	.176	—	.185	.106	.174
Return and Get Bands	—	.084	.116	.162	.231	.166	.121	.080	.095	.066
Cope Out and Close	.088*	.176	.165*	.213	.190*	.177	.237*	.352	.222	.275
Remove Snap Flask	.136	.056	.097	.082	.090	.077	.086	.080	.077	.099
Set Small Ramup Core	—	(2).108	—	—	—	—	—	—	—	—
Trim Mold	.070	.073	.070	.142	.060	.124	.063	—	.135	.088
Return	.062	.088	—	.095	—	.092	—	.100	.081	.086
Partine Pattern	—	.131	—	—	.208	.127	—	.130	.188	.179
Hammer Bottom Board	.058	.064	.068	.072	—	—	—	—	.059	.100
Clean Pattern	.100	—	.070	—	—	—	.280	—	—	—
Set Chills	—	—	(16)1.600	(1).229	—	—	—	—	—	—
Blow Out Drag	—	—	.288	.096	—	—	—	—	.175	.098
Flask Volume	1120	1980	1980	2156	1960	2430	2430	3120	3072	3072
Joint Area	140	198	198	196	196	243	243	240	256	256

* Cope Closed at Bench.

TIMESTUDY DATA SHEET

DEPT. 5 OPERATION SQUEEZER
SPECIFICATIONS PATTERN B 69304 C
14 X 14 X 5/16
OPERATOR JOHN DOE
CLOCK NO. 462
OBSERVER W.I.S.
APPROVED BY

STUDY NO. 4
SHEET NO. 1 OF 2
DATE JUNE 3, 1939
START TIME 8.46
STOP TIME 9.27

ELEMENT DESCRIPTION		1	2	3	4	5	6	7	8	9	10	TOTAL	AVE.	L. F.	LEVELED TIME	P.F.D.	STAND.
ASSEMBLE FLASK	T	12	14	11	12	18						.67	.134	110	.147		
	NO																
BAND IN	T	14	12	15	9	8						.58	.116	100	.116		
	NO																
SET CHILL	T	14	25	20	23	27						1.09	.218	105	.229		
	NO																
RIDDLE FACING	T	23	16	17	20	18						.94	.188	120	.224		
	NO																
TUCK FACING	T	5	3	3	3	3						.17	.034	125	.042		
	NO																
HEAP	T	23	13	26	19	17						.98	.196	110	.216		
	NO	4	3	4	3	3											
JOLT	T	4	5	6	4	6						.25	.050	115	.058		
	NO																
PEEN	T	17	14	15	16	20						.82	.164	110	.180		
	NO																
LEVEL & BOARD ON	T	11	12	17	10	12						.62	.124	110	.136		
	NO																
HAMMER BOARD	T	6	9	8	6	7						.36	.072	100	.072		
	NO																
ROLLOVER	T	13	15	15	17	15						.75	.150	110	.165		
	NO																
BAND IN	T	7	5	7	10	6						.35	.070	100	.070		
	NO																
RIDDLE FACING	T	17	16	17	20	16						.86	.172	120	.206		
	NO																
HEAP	T	12	13	12	15	13						.65	.130	110	.143		
	NO	3	3	3	3	3											
PEEN	T	11	12	14	12	12						.61	.122	110	.134		
	NO																
LEVEL & BOARD ON	T	25	26	27	23	28						.129	.258	95	.245		
	NO																
SQUEEZE	T	6	8	8	7	7						.36	.072	125	.090		
	NO																
BOARD OFF	T	5	5	4	4	5						.23	.046	120	.055		
	NO																

REMARKS OVERALL TIMES 4.81 - 4.68 - 4.31

OIL PATTERN 1.21
PERSONAL .08

Fig. 3—Sample of actual timestudy data on Squeezer Operation.

TIMESTUDY DATA SHEET

DEPT. _____ OPERATION _____ STUDY NO. 4

SPECIFICATIONS B 69304 C OPERATOR _____ SHEET NO. 2 OF 2

CLOCK NO. _____ DATE _____

OBSERVER _____ START TIME _____

APPROVED BY _____ STOP TIME _____

ELEMENT DESCRIPTION		1	2	3	4	5	6	7	8	9	10	TOTAL	AVE.	L. F.	LEVELED TIME	P.F.D.	STAND.
CUT SPRUE (1)	T	17	16	17	21	24						.95	.190	100	.190		
	NO																
VENT	T	15	11	13	13	10						.62	.124	110	.136		
	NO																
BLOWOFF COPE FINISH BASIN	T	22	22	20	24	18						1.06	.212	100	.212		
	NO																
COPE OFF & ON TABLE	T	10	14	12	11	9						.56	.112	105	.118		
	NO																
CLEAN SPRUE	T	7	8	10	11	9						.45	.090	110	.099		
	NO																
THRU VENTS - COPE	T	13	14	10	12	12						.61	.122	110	.134		
	NO	2	2	2	2	2											
BLOWOFF MATCH	T	8	7	8	7	8						.38	.076	100	.076		
	NO																
DRAW	T	13	14	15	11	15						.68	.136	105	.143		
	NO																
BLOWOUT DRAG	T	9	9	10	11	9						.48	.096	100	.096		
	NO																
SET SMALL CORE	T	18	20	22	22	24						1.06	.212	105	.223		
	NO																
PATCH & FINISH	T	10	10	6	0	0						.26	.052	100	.052		
	NO																
DRAG OUT & POSITION	T	12	15	11	12	11						.61	.122	110	.134		
	NO																
RETURN - GET BANDS	T	14	12	17	16	18						.77	.154	105	.162		
	NO																
COPE OUT & CLOSE	T	20	20	16	22	19						.97	.194	110	.213		
	NO																
REMOVE SNAP	T	10	7	7	9	8						.41	.082	100	.082		
	NO																
TRIM MOLD	T	16	13	13	16	13						.71	.142	100	.142		
	NO																
RETURN	T	9	10	8	7	9						.43	.086	110	.095		
	NO																
	T																
	NO																

REMARKS _____

(Fig. 3 Cont.)

The writer wishes to stress the fact that *as few standards as possible* should ever be set directly from timestudy. Even with the prescribed technique for keeping the observer's pace rating within reasonable limits by the use of the Bench Mark routine discussed previously, there is still the chance that timestudy observations will vary as much as 15 per cent above or below the normal time. This will give inconsistent standards when they are set up directly from timestudy.

The only logical method for setting standards is by formula derived from analysis of enough timestudies on any one operation to cover all of the variations on elements normally encountered. When the formula is completed, standards should be set from it only, and further studies taken if and when exceptions to the formula become apparent, due to elements or variations not picked up in the original data.

The Timestudy

Figure 3 shows a typical study of a snap flask job. At the bottom at the left (Fig. 3) of the timestudy under remarks the overall times shown are clocked times for complete cycles of the job. The observer does this while he is recording the various elements of the cycle preparatory to taking the elemental times which are recorded in the numbered columns. He may also clock one or two complete cycles in the middle of the study for relaxation, or near the end of the study while he is noting the leveling factors for each element.

The start and stop time for the study at the top of the sheet show a total elapsed time for the study of 41 minutes. Counting the overall timed cycles and the elementally timed cycles, there are a total of eight cycles to the study. From the 41 minutes elapsed time is deducted 1.21 minutes for oiling the pattern (note at bottom of Fig. 3 left) which can be considered supplemental operations, and 0.08 minutes for personal (also noted under remarks). This gives a total working time of 39.71 minutes for the study which, divided by the eight cycles gives an average time per cycle of 4.97 minutes. This is a good figure to use in checking the standard derived from the formula when it is finally set up.

The study here shown is incorporated in the comparison sheet from which the formula is subsequently derived.

The Comparison Sheet

The Comparison Sheet is a summary on one large sheet of the Leveled Times taken from the various timestudies of the operation which are to be pulled together into a formula. On it also is noted other pertinent data for the derivation of the formula such as flask size, flask volume, pattern number, etc.

Comparison sheet used in this discussion is shown in Table 3. Limitations of space have made it necessary to cut down on the number of elements involved in all of the timestudies. Only those elements recurring frequently are shown. In any operation ten to fifteen studies should be enough to derive a formula for all the regular elements. That is, those elements which occur in every job in that operation.

More studies may be necessary to obtain data on the extra, or non-recurring elements which may be encountered in the various jobs of the operation, but it is necessary to record these on the original Comparison Sheet.

Analyzing the Timestudy Data

In setting up Standard data the elements involved are divided into two classes, (1) regular elements, and (2) extra elements. The regular elements are those that always occur in each cycle of the operation, and the extra elements are those that may occur in varying amounts or may not occur at all in some jobs of the operation.

Assemble Flask and Match, Band In, Riddle Facing, Heap, Jolt and Rollover are examples of regular elements. Set Cores, Chills, or Nails, Cut Sprues, and Thru Vents are examples of extra elements. In the final application of the formula all of the regular elements can be handled in total for each flask size. However, to arrive at the values to be used in these totals the timestudy data must be considered one element at a time.

The extra elements must be set up in the final formula as separate items to be applied or omitted as the job in question calls for.

The Element Assemble Flask and Match

The first regular element encountered on the Comparison Sheet is Assemble Flask and Match.

Visual inspection of the Comparison Sheet shows that the values for this element are higher for the heavier weight flasks. Logically then, the first step would be to plot the normal times from the comparison sheet against the weight of the flask — or substituting the perimeter of the flask (P) times the depth (D) for weight, since PxD is directly proportional to weight, values in Table 4 are plotted in Fig. 4.

Figure 4 shows that two points are out of line so

TABLE 4.—ASSEMBLE FLASK AND MATCH ELEMENT VALUES FOR FLASKS OF INCREASING WEIGHT

Study No.	Normal Times, Min.	P x D, Sq. In.
1	.206	384
2	.274	580
3	.296	580
4	.147	616
5	.275	560
6	.243	630
7	.250	630
8	.370	832
9	.289	768
10	.145	768

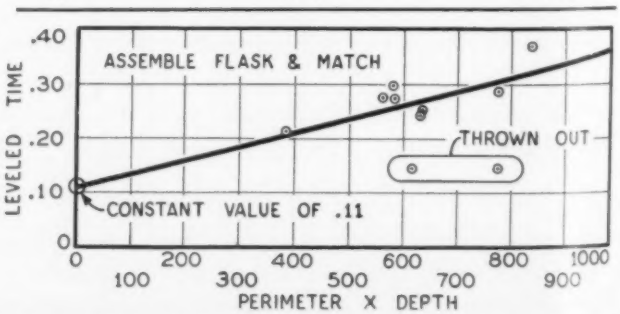


Fig. 4—Plot of timestudy data in Table 4 for element Assemble Flask and Match.

they are disregarded. The curve drawn through the other points indicates that regardless of the flask weight there is a constant value of time that occurs in the element. This constant value is .11 minutes.

If, in the opinion of the person analyzing the data, the points are near enough in line in Fig. 4, the formula may be derived directly from this graph for the element Assemble Flask and Match. To do this the following steps are taken:

1. Select a value of leveled time where the line crosses a given value of PxD. The value of PxD should be as great as possible, and should be an even number for simplicity of calculation. The value of Leveled time where the line crosses the 1000 PxD line is 0.363.

2. From this value (0.363) subtract the constant value of 0.11 where the line crosses zero PxD line. The remainder 0.253 is the variable time for 1000 PxD.

3. For each 100 PxD then, the variable time would be 0.0253.

4. The formula for normal time for any given flask would then be 0.11 plus 0.0253 per 100 sq. in. PxD.

Caution: In plotting graphs and deriving formulas from them, always lay off the time element on the vertical axis of the graph and the variable factor on the horizontal axis, and *always start the variable factor at zero.* Otherwise mathematical complications will occur with this method which will cause bad errors in the formula derived. It is always best to check the formula derived with some other value of the variable factor than that selected to derive the formula. If the check point found using the other value of the variable falls on the line, then the formula is correct.

5. The formula derived above is for normal time. To this must be added allowances for Personal, Fatigue and Delay (or supplemental operations). Since the element of Assemble Flask and Match involves the use of Hands, Fullarm and Shoulders, and the weight of each part handled is between 11 lbs. and 30 lbs., the Personal and Fatigue Factor from Table 2 is 1.09. Assuming a 0.02 factor (2 per cent) for inherent delays and supplemental operations, which would be determined by all-day studies on this operation, the total PFD (Personal, Fatigue and Delay) Factor would be 1.11.

6. Multiplying the formula values for normal time found in No. 4 above by the PFD Factor 1.11 we get the formula for the Standard allowance for this element, which is:

0.122 plus .0281 per 100 sq. in. PxD.

If however, in the opinion of the person analyzing the data, the points in Fig. 4 are too far out of line, or if there is reason to feel that the leveling on some of the studies is wrong, then the procedure outlined below is followed, using from Fig. 4 only the constant 0.11 minutes.

TABLE 5.—DATA FOR DETERMINING APPROXIMATE CONSTANT FOR AN ELEMENT

Time Study	Actual Time	Less Const.	Var. Time	Var. Time per 1000 P x D	Plus Const.	Adj. Time	Operations per Hour	Leveling Factor
1	.206	— .11	.096	.250	+ .11	.260	167	100
2	.274	— .11	.164	.283	+ .11	.393	153	100
3	.312	— .11	.202	.348	+ .11	.458	131	95
4	.134	— .11	.024	.039	+ .11	.149	403	110
5	.246	— .11	.136	.243	+ .11	.353	170	110
6	.243	— .11	.133	.211	+ .11	.321	187	100
7	.218	— .11	.108	.172	+ .11	.282	213	115
8	.370	— .11	.260	.312	+ .11	.422	142	100
9	.263	— .11	.153	.199	+ .11	.309	195	110
10	.132	— .11	.022	.029	+ .11	.139	431	110

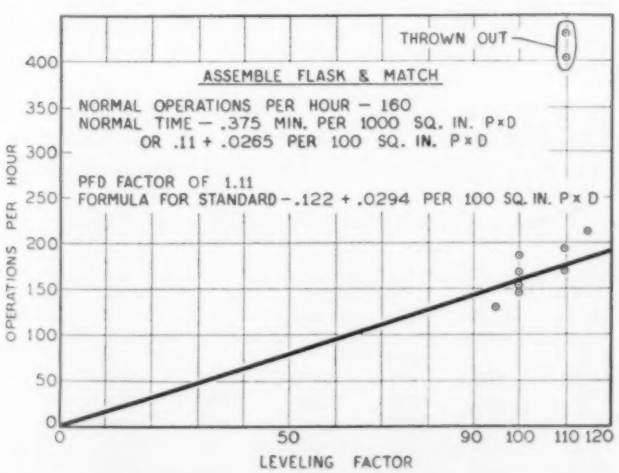


Fig. 5—Plot of data for element Assemble Flask and Match for determining standard formula from it for this element.

Significance of the Constant

In plotting graphs and deriving formulas from them it is well to understand the significance of the constant. It may be thought of as the normal time for the element, if the variable factor were zero. In this case on the element Assemble Flask and Match, the constant 0.11 may be thought of as the time to go through all the motions of the element with a flask of definite size and shape, but weighing nothing since the factor PxD is proportional to weight. In an element such as shoveling sand where the molder must reach for the shovel regardless of the amount he is going to shovel, then throw the shovel into the heap when he is through shoveling, the constant is the time value for doing these parts of the element that are not dependent on the factor, which would be volume in case of shoveling sand.

With these ideas of the reason for the constant in mind, it is not difficult, in plotting a graph, even though the points may not "line up" to determine the approximate constant for use in the following method of plotting Fig. 5 and determining from it the standard formula for the element.

1. List the actual average times taken from the time-studies. (Not the leveled times from the comparison sheet, Table 3.)

2. From the actual average times deduct the 0.11 minutes found to be constant for all flasks from Fig. 5.

3. The remaining time is variable depending upon the total PxD (equivalent to weight). Adjust this time in each study to a value equivalent to a PxD of 1000 sq. in.

4. Add again the constant 0.11.

5. Convert these times to operations per hour by dividing into 60 minutes. In other words, for the actual time the operator spent doing the operation in each case, how many times per hour could he do it if the flask had a PxD of 1000 sq. in.

TABLE 6.—FORMULA SUMMARY SHEET—REGULAR ELEMENTS, SNAP FLASK, MATCH PLATE

Flask Size, in.	10x14x5/5	11x18x5/5	11x18x6/6	13½x18x5/5	13½x18x6/6	12x20x5/5	12x20x6/6	16x16x5/5	16x16x6/6	14x20x5/5	14x20x6/6
Assemble Flask	.263	.292	.326	.308	.346	.311	.349	.311	.349	.322	.362
Bands In (2)	—	.218	.218	.218	.218	.218	.218	.218	.218	.218	.218
Riddle Facing—Cope and Drag	.478	.564	.565	.630	.630	.628	.628	.653	.653	.689	.689
Sand In—Cope and Drag	.440	.527	.587	.595	.668	.590	.663	.613	.691	.651	.735
Peen—Cope and Drag	.260	.332	.332	.386	.386	.385	.385	.405	.405	.433	.433
Level Sand and Board On	.418	.418	.418	.418	.418	.418	.418	.418	.418	.418	.418
Jolt	.065	.065	.078	.065	.078	.065	.078	.055	.078	.065	.078
Rollover	.117	.146	.155	.155	.166	.157	.169	.161	.173	.192	.209
Hand Pack Facing	.064	.064	.054	.064	.054	.064	.064	.064	.064	.064	.064
Squeeze	.107	.107	.107	.107	.107	.107	.107	.107	.107	.107	.107
Board Off	.076	.076	.076	.076	.076	.076	.076	.076	.076	.076	.076
Normal Vent	.090	.127	.127	.156	.156	.154	.154	.164	.164	.179	.179
Blow Off Cope	.130	.157	.157	.180	.180	.176	.176	.184	.184	.195	.195
Blow Off Match	.074	.074	.074	.074	.074	.074	.074	.074	.074	.074	.074
Finish Mold	.086	.086	.086	.086	.086	.086	.086	.086	.086	.086	.086
Remove Snap	.094	.101	.101	.101	.101	.101	.101	.101	.101	.101	.113
Trim Mold	.076	.076	.076	.078	.078	.078	.078	.078	.078	.078	.078
Clean or Partine Pattern	.166	.166	.166	.166	.166	.166	.166	.166	.166	.166	.166
Hammer Bottom Board	.068	.068	.068	.068	.068	.068	.068	.068	.068	.068	.068
Close and Carryout	.493	.740	.891	.905	1.210	.897	1.200	1.110	1.260	1.180	1.580
TOTAL STANDARD FOR REGULAR ELEMENTS	3.565	4.405	4.672	4.836	5.276	4.829	5.258	5.122	5.413	5.362	5.928

6. These operations per hour for each study should be directly proportional to the pace rating given the operator by the observer on this element.

7. Plot the operations per hour against the pace rating for the element, Fig. 5.

8. Figure 5 confirms the fact that points disregarded in Fig. 4 were out of line due to poor leveling. All the other points are fairly well in line. In this type of graph the line is *always* drawn through zero, and the average of the points not thrown out.

9. The normal operations per hour is found where the 100 leveling factor line and the average line cross. Calculations and values used in Fig. 5 are shown in Table 5.

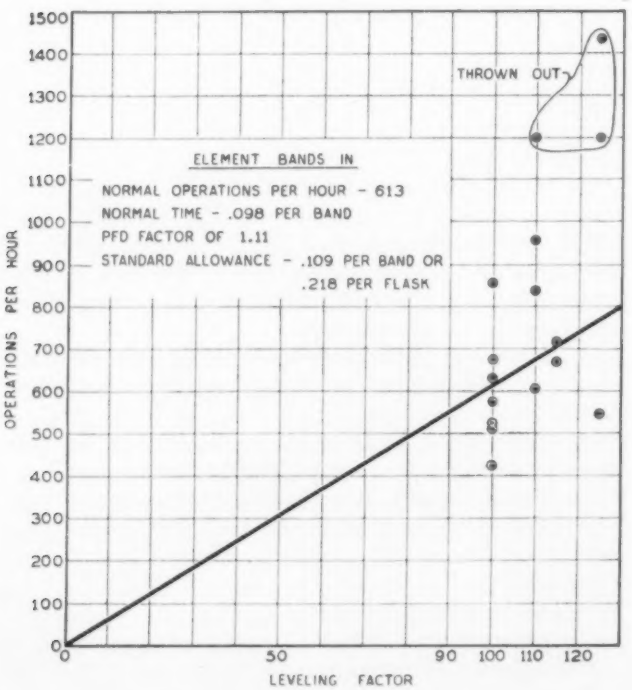


Fig. 6—Plot of data from Table 7 for element Bands In.

10. The normal operations per hour is divided into 60 minutes to find the normal time per operation in minutes.

11. From the graph this normal time is found to be 0.375, which is made up of the constant 0.11 and the variable of 0.265 for a flask with a PxD of 1000 sq. in. or 0.0265 per 100 sq. in.

12. For this element the PFD factor, as previously explained is 1.11. Therefore the formula for the standard allowance for this element will be

0.122 plus 0.0294 per 100 sq. in. PxD.

It will be noted that the variable derived from Fig. 5 differs somewhat from that derived in Fig. 4. This is due to the fact that the leveling in Fig. 5 is weighted as to its accuracy, while in Fig. 4 the leveling is assumed to be accurate in all cases.

13. List on the formula summary sheet eight or ten flask sizes most commonly in use, being sure that such sizes cover the largest and smallest flasks that will possibly be used in this operation. Calculate from the formula in No. 12 above, the value of this element for each flask size listed on the summary sheet and enter these calculated values. Summary sheet is shown in Table 6.

The Element Bands In

Assuming that the element Bands In for both cope and drag should be constant for all sizes, since size or

TABLE 7.—TIMESTUDY DATA ON ELEMENT BANDS IN

Time Study	DRAG L. F.	DRAG Actual Time	DRAG Oper. per Hour	COPE L. F.	COPE Actual Time	COPE Oper. per Hour
1	No bands used			No bands used		
2	125	.110	545	110	.072	834
3	125	.050	1200	110	.050	1200
4	100	.116	517	100	.070	857
5	115	.090	667	115	.084	715
6	100	.089	675	110	.077	779
7	125	.042	1430	100	.095	632
8	100	.105	572	100	.115	522
9	110	.099	606	110	.063	953
10	100	.118	424	110	.063	953

TABLE 9.—CALCULATIONS AND TIMESTUDY VALUES FOR ELEMENT RIDDLE FACING OF DRAG AND COPE

Study No.	Actual Time	Less Constant	Variable Time	Variable per 1000 sq. in.	Plus Constant	Total per 1000 sq. in.	Operations per hr.	Leveling Factor
				DRAG				
1	.212	— .12	.092	.656	+ .12	.776	78	110
2	.274	— .12	.154	.777	+ .12	.897	67	110
3	.296	— .12	.176	.888	+ .12	1.008	60	115
4	.186	— .12	.066	.337	+ .12	.457	131	120
5	.130	— .12	.010	.051	+ .12	.171	351	110
6	.162	— .12	.012	.173	+ .12	.293	205	115
7	.276	— .12	.156	.642	+ .12	.762	79	110
8	.246	— .12	.126	.524	+ .12	.644	93	110
9	.264	— .12	.146	.570	+ .12	.690	67	110
10	.312	— .12	.192	.750	+ .12	.870	69	110
				COPE				
1	.169	— .12	.049	.350	+ .12	.470	128	110
2	.282	— .12	.162	.818	+ .12	.938	64	110
3	.223	— .12	.103	.520	+ .12	.640	94	115
4	.172	— .12	.052	.265	+ .12	.385	156	120
5	.210	— .12	.090	.459	+ .12	.579	104	110
6	.204	— .12	.084	.345	+ .12	.465	129	110
7	.203	— .12	.083	.342	+ .12	.462	130	110
8	.240	— .12	.120	.500	+ .12	.620	97	100
9	.265	— .12	.145	.567	+ .12	.687	87	110
10	.334	— .12	.214	.836	+ .12	.956	63	105

weight of the bands do not have an appreciable range, then the only problem is to eliminate poor leveling on the part of the observer. To do this the following steps are taken:

1. List the actual times from the timestudies (not the normal times from the comparison sheet as in Table 3) and the leveling factors used for this element in the timestudies.

2. Convert the actual average times to operations per hour by dividing into 60 minutes. Plot these values against the leveling factors. The values plotted in Fig. 6 are shown in Table 7.

3. Normal Operations per Hour is found where the curve crosses the 100 leveling factor, or 613. This is then converted to normal time per operation by dividing into 60 minutes, or 0.098 per band for Band In.

4. Since the weight of the bands is more than 10 lbs. and less than 30 lbs., and since Hands, Fullarm, and Shoulders are used in this operation, the P.F. Factor, from Table 2, is 1.09 plus an assumed inherent delay of 0.02 gives a total P.F.D. Factor of 1.11.

5. The value 0.098, with a P.F.D. Factor of 1.11 then becomes 0.109 as the standard time allowed for one Band In, or 0.218 for two bands. Enter this regular value on the formula summary sheet.

TABLE 8.—TIMESTUDY DATA ON ELEMENT RIDDLE FACING

Time Study	Leveling Time		Flask Area
	Drag	Cope	
1	.233	.186	140
2	.301	.310	198
3	.340	.257	198
4	.224	.206	196
5	.143	.231	196
6	.186	.224	243
7	.304	.223	243
8	.270	.240	240
9	.290	.292	256
10	.343	.351	256

The Element Riddle Facing

The element Riddle Facing, being inherent in all molding operations, can be included as a regular element.

Experience dictates flask area to be the most likely factor determining the variations in time encountered in this element.

Plot the leveled times, (Table 8) from the comparison sheet against the flask area for both Cope and Drag (Fig. 7).

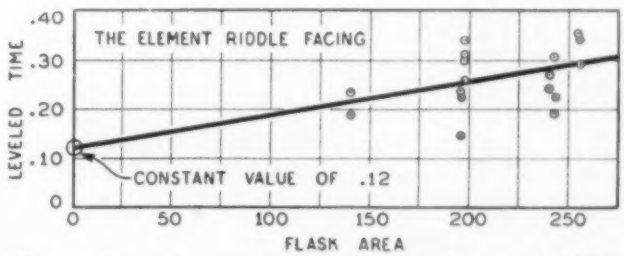


Fig. 7—Plot of timestudy data for element Riddle Facing.

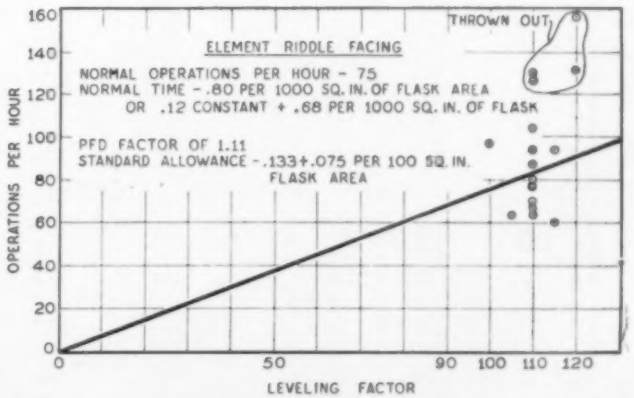


Fig. 8—Plot of timestudy data from Table 9 for element Riddle Facing.

TABLE 10.—TIMESTUDY DATA FOR ELEMENT HEAP SAND

Time Study	Drag Volume	Leveling Time	Cope Volume	Leveling Time
1	560	.213	560	.198
2	990	.260	990	.206
3	990	.135	990	.178
4	1180	.216	980	.143
5	980	.319	980	.240
6	1215	.379	1215	.270
7	1215	.195	1215	.173
8	1680	.340	1440	.395
9	1280	.272	1790	.358
10	1280	.254	1790	.326

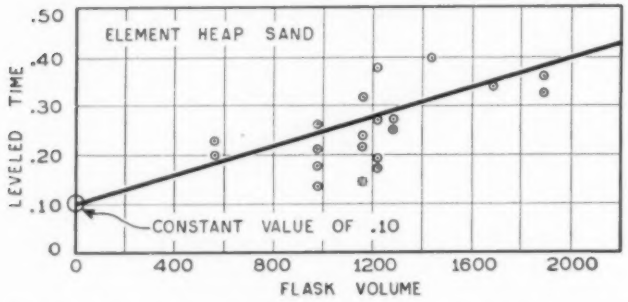


Fig. 9—Plot of timestudy data for element Heap Sand.

Figure 7 indicates a constant value of 0.12 minutes that is not dependent upon flask area.

To eliminate poor leveling and to arrive at a standard formula for this element, the following steps are taken:

1. Actual average times are listed from the timestudies along with the leveling factors for both Cope and Drag.

2. The constant value of 0.12 minutes is deducted from the actual average times to get the pure variable time.

3. The variable time is then revised to a basis of 1000 sq. in. of flask area and the constant value is 0.12 added back in.

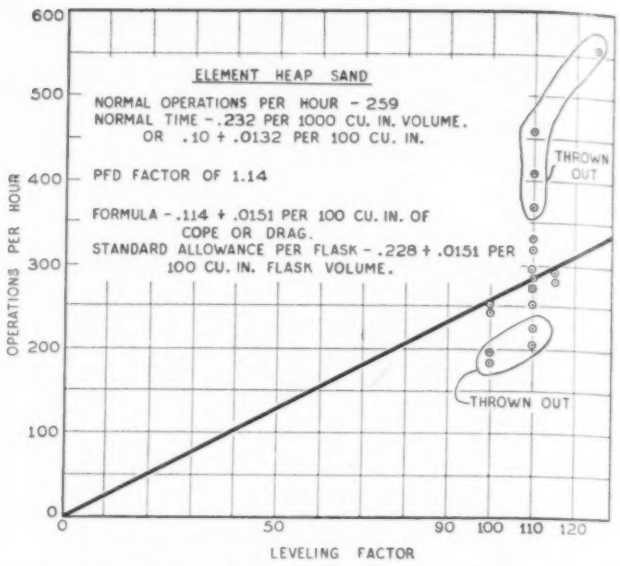


Fig. 10—Plot of timestudy data from Table 11 for element Heap Sand.

4. These total times are then converted to operations per hour by dividing into 60 minutes.

5. Operations per hour are plotted against the leveling factors. Calculations and values for plotting Fig. 8 are shown in Table 9.

6. In this case drag values in studies No. 5 and 6 were so far out of line as to be out of the limits of the graph. Four other points are also found to be out of line.

7. The line is drawn through zero and the average of all the other "in line" points.

8. The normal operations per hour is found to be 75. This is where the line drawn crosses the 100 leveling factor line. Dividing this into 60 minutes we get the normal time for riddling facing. This is 0.80 per 1000 sq. in. of flask area.

9. This is composed of the 0.12 constant and 0.68

TABLE 11.—CALCULATION AND TIMESTUDY VALUES FOR ELEMENT "HEAP SAND" OF DRAG AND COPE

Study No.	Actual Time	Less Constant	Variable Time	Variable per 1000 Cu. in.	Plus Constant	Total per 1000 Cu. in.	Operations per hr.	Leveling Factor
				DRAG				
1	.194	— .10	.094	.168	+ .10	.268	224	110
2	.236	— .10	.136	.138	+ .10	.238	252	110
3	.108	— .10	.008	.008	+ .10	.108	555	125
4	.196	— .10	.096	.081	+ .10	.181	331	110
5	.290	— .10	.190	.194	+ .10	.294	205	110
6	.379	— .10	.279	.229	+ .10	.329	183	100
7	.177	— .10	.077	.053	+ .10	.163	368	110
8	.340	— .10	.240	.143	+ .10	.243	247	100
9	.234	— .10	.134	.105	+ .10	.205	293	115
10	.231	— .10	.131	.102	+ .10	.202	297	110
				COPE				
1	.180	— .10	.080	.121	+ .10	.221	272	110
2	.187	— .10	.087	.088	+ .10	.188	319	110
3	.162	— .10	.062	.063	+ .10	.163	368	110
4	.130	— .10	.030	.031	+ .10	.131	458	110
5	.240	— .10	.140	.143	+ .10	.243	247	100
6	.270	— .10	.170	.140	+ .10	.240	250	100
7	.157	— .10	.057	.047	+ .10	.147	408	110
8	.395	— .10	.295	.205	+ .10	.305	197	100
9	.312	— .10	.212	.113	+ .10	.213	282	115
10	.296	— .10	.196	.110	+ .10	.210	286	110

TABLE 12.—TIMESTUDY DATA FOR ELEMENT "PEEN"

Study No.	Flask Area	Leveled Time Cope	Drag
1	140	.112	.128
2	198	.130	.133
3	198	.121	.193
4	156	.134	.180
5	156	.154	.115
6	243	.157	.124
7	243	.227	.154
8	240	.265	.215
9	256	.323	.192
10	256	.378	.234

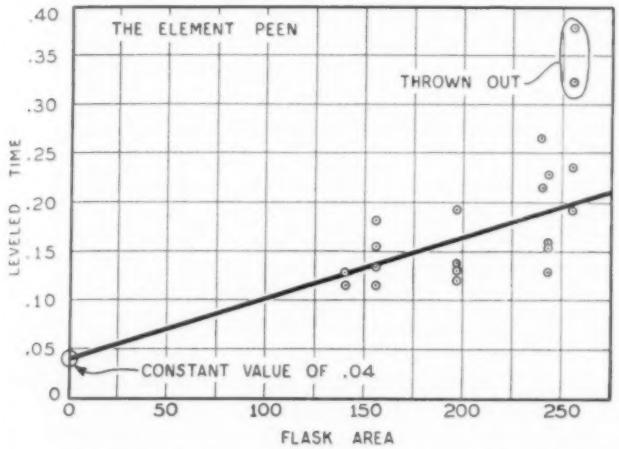


Fig. 11—Plot of timestudy data from Table 12 for element Peen.

variable per 1000 sq. in. of flask area, or 0.068 per 100 sq. in.

10. Since Riddle Facing involves the use of Hands, Fullarm and Shoulders, and the weight handled is over ten pounds, the P.F. factor, from Table 2 is 1.09 plus the assumed inherent delay factor of 0.02, for a total P.F.D. of 1.11.

11. The formula 0.12 plus 0.068 per 100 sq. in.

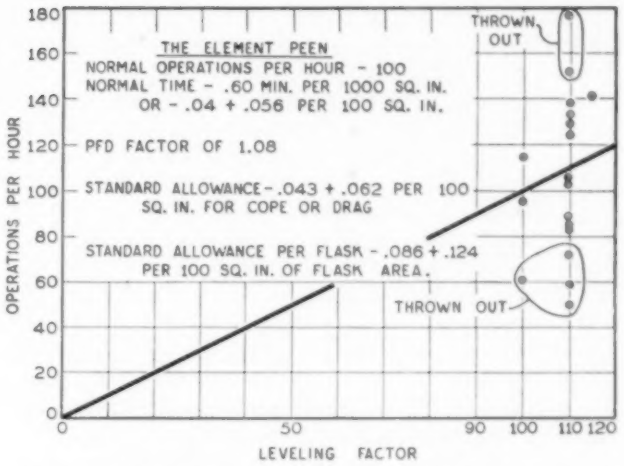


Fig. 12—Plot of timestudy data from Table 13 for element Peen.

then becomes 0.12 x 1.11 plus 0.068 x 1.11 or 0.133 plus 0.0755 standard allowance per 100 sq. in. of flask area.

12. This formula is for either Cope or Drag. The formula for the complete flask would be 0.266 plus 0.151 per 100 sq. in. of flask area.

13. Using this formula, the proper values for each flask size should be filled in on the formula summary sheet.

The Element Heap Sand

The time for heaping sand into the flask will definitely vary with the volume of the flask. Plotting the leveled times from the comparison sheet against the volume for both the Cope and the Drag, we arrive at Fig. 9.

The points in Fig. 9 are fairly well scattered so it would be difficult to determine just where the line should be drawn. However, this curve is used only to determine the constant, 0.10, to develop Fig. 10, which indicates leveling factors which are out of line.

TABLE 13.—CALCULATIONS AND TIMESTUDY VALUES FOR ELEMENT "PEEN" OF DRAG AND COPE

Study No.	Flask Area	Actual Time	Less Constant	Variable Time	Variable per 1000 sq. in.	Plus Constant	Total per 1000 sq. in.	Operations per hr.	Leveling Factor
				DRAG					
1	140	.116	— .04	.076	.543	+ .04	.583	103	110
2	198	.121	— .04	.081	.409	+ .04	.449	133	110
3	198	.175	— .04	.135	.682	+ .04	.722	83	110
4	156	.164	— .04	.124	.794	+ .04	.834	72	110
5	156	.100	— .04	.060	.384	+ .04	.424	141	115
6	243	.113	— .04	.073	.300	+ .04	.340	177	110
7	243	.140	— .04	.100	.412	+ .04	.452	133	110
8	240	.215	— .04	.175	.489	+ .04	.529	114	100
9	256	.192	— .04	.152	.594	+ .04	.634	95	100
10	256	.213	— .04	.173	.675	+ .04	.715	84	110
				COPE					
1	140	.102	— .04	.062	.444	+ .04	.484	124	110
2	198	.118	— .04	.078	.395	+ .04	.435	138	110
3	198	.110	— .04	.070	.354	+ .04	.394	152	110
4	156	.112	— .04	.082	.525	+ .04	.565	106	110
5	156	.140	— .04	.100	.642	+ .04	.682	88	110
6	243	.143	— .04	.103	.425	+ .04	.465	129	110
7	243	.206	— .04	.166	.682	+ .04	.722	83	110
8	240	.265	— .04	.225	.940	+ .04	.980	61	100
9	256	.294	— .04	.254	.990	+ .04	1.030	58	110
10	256	.344	— .04	.304	1.185	+ .04	1.225	49	110

The data for Fig. 10 is developed in a manner similar to that for the element Assemble Flask and Match. It is shown in Table 11.

The normal time for a 1000 cu. in. flask is 0.232 (from Fig. 10). This is made up of the constant 0.10 and a variable 0.132 per 1000 cu. in. or 0.0132 per 100 cu. in. Since the constant 0.10 applies to either cope or drag, a 0.20 must be used if the formula is to apply to the total flask volume including both cope and drag. The formula then becomes 0.20 plus .0132 per cu. in. total flask volume.

As the element involves the use of Hands, Fullarm and Shoulders and Back, partially bent, and the weight handled is between 11 and 30 lbs., the PF factor from the table is 1.12, which with the 0.02 inherent delay gives a total PFD factor of 1.14.

With this PFD factor then the standard allowance formula for heaping sand then becomes

0.228 plus 0.0151 per 100 cu. in. total flask volume.

Based on this formula the values for the various flask sizes are entered on the formula summary, Table 6.

The Element Peen

As the amount of time spent in peening appears to follow match or flask area, the same methods are followed as in heaping sand to arrive at the formula. Plotting leveled times against the flask area for both cope and drag we get Fig. 11. Values plotted are shown in Table 12.

Having plotted Fig. 11, if it is felt that poor leveling should be eliminated, then Fig. 12 is plotted on the values shown in Table 13 in a manner similar to Fig. 10 for heaping sand.

As indicated on Fig. 12, the standard allowance for this element then is 0.086 plus 0.124 per 100 sq. in. flask area.

Discussion

In getting the time study data into useable form, it will not always be necessary to go through every step outlined in the four preceding examples. These steps are a mechanical means of detecting and omitting time study data that is in error, and of averaging other factors that otherwise would be impossible to measure. It must be remembered, in analyzing time study data, that no mechanical method will ever be able to substitute for good judgement, imagination, and a knowledge of the fundamentals of motion analysis.

By use of good judgement some of the standard allowances for elements may be determined by inspection alone. This occurs where the time study data and common sense indicate that the element should be a constant value regardless of flask size, volume, weight, etc.

Similarly, some of the extra elements, such as core setting, pattern draw, and cope lift, cannot be equated to any definite flask or pattern characteristic and should be analyzed by inspection, setting up minimum and maximum limits for the element. In this case, the man who sets standards from the final formula must be carefully trained as to the reasons for variation in these elements values, such as complexity of core, complexity and depth of draw or cope lift, and intricacy of pattern for making extra finish allowance. In addition to being well trained the standards setter must use good judgement and be conscientious about his job.

The standards for some other elements can be determined by plotting only one graph. That is, plotting the normal times from the comparison sheet against some varying flask or pattern characteristic as in Fig. 5. A satisfactory standard allowance can be derived from this graph alone if it is felt that the points plotted are consistent and that fairly accurate leveling was used. This condition will be more nearly attained if the observer is properly trained and his ability to pace rate is kept in line by routine use of the Bench Mark procedure for pace rating previously outlined. Another condition which determines the ease or difficulty of analyzing the timestudy data is proper training of the observers in the use of correct break-off points between elements. In preparation for the timestudy of a certain operation a few preliminary studies should be taken. Then all the observers should discuss these trial studies to determine just exactly which elements or which combination of elements shall be recorded, and where the break-off points for each element shall be. If this is done conscientiously much labor and guess work will be saved in the final analysis of the timestudy data.

It will be necessary, therefore, to follow through all the steps indicated in the given examples only when the leveling or break-off points of the elements being considered are not consistent.

The Formula

Having listed each of the regular elements on the formula summary sheet (Table 6), as they were derived for the various flask sizes, it is then possible to further simplify the final formula and equalize the inconsistencies due to bracketing of the fatigue allowances within certain weight ranges (Fatigue table, Table 2) by plotting the totals of the regular elements shown on the summary sheet against the flask volume. This is done in Fig. 13.

By using the formula derived from such a graph, the total value of the regular elements can then be obtained for flask sizes other than those originally recorded on the formula summary sheet.

The formula derived from Fig. 13 is found to be,

1.90 plus 0.119 per 100 cu. in. flask volume.

This of course, is standard time allowance, since PFD allowances have been included in all the values on the summary sheet.

The extra elements listed on the comparison sheet (Table 3), are treated in the same manner as the regular elements. As previously stated, limitations of space have made it necessary to cut down on the number of elements actually involved in all of the time studies, and only show those recurring frequently. From the extra elements shown on the comparison sheet, after careful analysis, the final standard allowances, including PFD, are determined. These are as follows:

Cut and clean sprues	.05 to .25 for each sprue
Cope lift	.15 to .40 (range based on complexity)
Vent thru cope	.06 to .05 per cent

Draw	.10 to .20 (range based on complexity and depth of draw)
Extra finish	.10 to .50 (range based on intricacy)
Set small core	.17 to .25 (range based on size and complexity)
Set nails	.06 per nail

The final form of the formula shown below shows other extra elements which have not been included in this discussion but were developed in a similar manner. It is not necessary to cover all possible extra elements to use a formula. As the formula is used, and new extra elements are encountered in practice, studies are taken on such elements, the data analyzed, and standard allowances are incorporated in the formula.

The final form of the formula follows:

Snap Flask Match Plate Molding

Std. Min./Mold =

A+B+C+D+E+F+G+H+I+J+K

A=Total regular elements – cope and drag.
=1.90 plus 0.119 per 100 cu. in. flask volume.

B=Extra Finish – (Normal Finish allowed in regular elements).
=0.10 to 0.50 (range based on complexity of pattern.

C=Extra Band Allowance – (Two bands per flask allowed for in regular elements).
=0.20.

D=Chaplet Allowance
=0.10 each simple set – 17 each difficult set.

E=Chill Allowance
=0.10 each multiple chill
=0.17 each small chill – under 2 lbs.
=0.40 each medium chill – 2 to 20 lbs.
=0.50 each large chill – over 20 lbs.

F=Cope Lift
=0.15 to 0.40 – Based on complexity of cope.

G=Core Set Allowance
=0.17 to 0.25 – Based on complexity and size of core.

H=Draw
=0.10 to 0.20 – Based on complexity and depth of draw.

I=Nail Set Allowance
=0.06 per nail

J=Sprue Allowance
=0.05 plus 0.25 each sprue cut and cleaned.

K=Vent Allowance – (Normal venting allowed for in regular element).
=0.05 plus 0.06 each vent through cope.

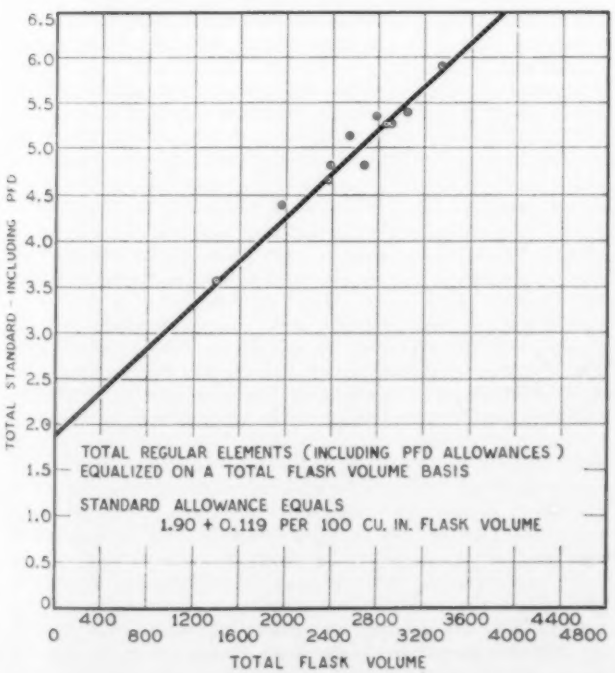


Fig. 13—Plot of totals of the regular elements vs. flask volume (Table 6) for derivation of formula.

To set a standard allowance for any particular job from the formula, the specifications of the job must be obtained. This is usually noted on a form whereby all the possible elements of the job are covered. Using the specifications on the same job as the sample timestudy, Figs. 3 and 4, we have the following:

Pattern No. B 69304 C
Flask Size 14 x 14 5/6 – Vol. 2156

Finish	– moderate
Chills	– one medium
Cores	– one small
Draw	– moderate – 3 in.
Cope	– flatback
Sprues	– one
Vents	– normal

From the formula, the time allowances for these specifications would be.

A– 4.47
B– .15
E– .40
F– .15
G– .17
H– .15
I– .30
———
Total– 5.79
(Elements omitted in example have zero value)

This standard from the formula is comparable to the average over-all cycle time during the timestudy (discussed on p. 228) of 4.97 minutes. Which means that during the study the operator was hitting 116 per cent of standard, based on the formula standard, which is entirely reasonable.

The standard allowance of 5.97 minutes can be converted to a PW price as follows. If the hourly base rate for this class of work is $1.20, dividing by 60 gives an equivalent base of $0.02 per minute.

Then PW price=5.79 x .02=$0.116

Or, it can be converted to molds per hour by dividing into 60 minutes, thus,

60÷5.79=10.4 molds per hour.

Or to standard hours for the job by dividing by 60 minutes,

5.79÷60=0.965 standard hours per mold

Conclusion

In conclusion the writer wishes to state that he has tried not so much to point out any hard and fast rules for the development of standard formulas from the timestudy data, as to try to indicate possible methods by which the problem might be attacked. Other methods and techniques are in use and will be found which will be equally effective. Imagination and ingenuity are the prime requisites for analyzing the timestudy data and arriving at the best answers. The examples shown only indicate a few of many possible solutions. It is not intended that the facts and figures given in this discussion be used blindly without due thought and consideration, and reasonable judgement.

Reasonable results will be obtained however if the facts and figures given are used with judgement, and the methods and data can be modified to cover practically all of the operations in the foundry.

DISCUSSION

Chairman: R. J. FISHER, Falk Corp., Milwaukee.

Co-Chairman: J. A. WESTOVER, Westover Engineers, Milwaukee.

MEMBER: To what percentage do you exceed your standard pace rate in actual performance on your incentive basis?

MR. ANNICH: With standards set up on these normals, or normals taken from these paces, for an average department you will run from 25 to 30 per cent over normal at top performance. In other words, employees can earn from 25 to 30 per cent above their base rate.

MEMBER: How does it fluctuate in your cleaning rooms as compared with the molding department?

MR. ANNICH: In the cleaning room it probably would be much higher and much lower in individual cases. You cannot tie cleaning to a standard as well as you can molding, referring to general jobbing work. When you set up standard data on cleaning you cannot tie it down as well to various physical characteristics that are involved in the cleaning to get as consistent individual standards as you can on molding or on core making.

Going back to the 25 or 30 per cent performance above normal, in individual cases you may have an operator who is very good on some things and exceed normal by 50 or more per cent. Some of you may feel that the normals shown here are a bit loose; but from my experience, they will give that average figure. 25 or 30 per cent above normal for the average worker.

MEMBER: Were those standards established from your own operations, or is that a combination of industry operations?

MR. ANNICH: These normals, although I do not know their source, fit what we have been doing. There are several engineering companies using these normals. Another well known engineer has a normal for walking which is very close to the figure given here. Instead of 45 ft. in 10 sec., he uses 47 ft. in 10 sec. It amounts to about one-tenth mile more per hour. I think the standards come from the Bureau of Standards in Washington, although I never checked the source.

Now we will discuss the method that we usually employ to set up our standard data. Having made our time studies, we then set them up on a comparison sheet (Table 3). I do not have all the elements on this comparison sheet. Because of lack of space, I have left out some elements that occur only in possibly one, two or three timestudies. I have tried to cover just the elements that recur most frequently.

If you are going to set up standard data, you do not go any farther on your timestudies than entering the levelled time for each element. We may call that either levelled time or normal time, and it is the time that a man would do a job in if he was working at a standard pace. There are no allowances made for fatigue, personal, relaxation, or anything else. This normal time is the time we usually work with on the comparison sheets.

Let us consider the first element, Assemble Flask and Match. That element actually is composed of about three elements. For lack of space these have been combined. If the worker has a small flask, he carries out both cope and drag at the same time. He takes his snap flask off at the bench, leaves it on the bench or machine, and carries his cope and drag molds out together and sets them down. When he comes back he takes the cope, puts it on the machine and puts the match plate on the cope. He then puts the drag on the match plate before he starts the mold. If he has a double carry out, he takes out the drag, sets it down, and then he takes the cope out and closes; then he takes the snap back, both cope and drag. He brings them back and sets them on the table, takes the cope off, sets it on the machine, places the match, then the drag and starts the mold. So that assembled flask and match plate consists of those three elements: cope on, flask on, and drag on; then he starts the mold.

In Table 3 we have the studies graduated with small flask sizes on the left and larger flask sizes on the right. In general, as the flask sizes get larger, the time usually increases. We plot this against the snap flask weight. Then in order to get some figure that is comparable to the flask weight and more readily obtainable we just took the perimeter times the depth, which gives a figure directly proportional to the flask weight. We plotted the normal time against this figure. The graph in Fig. 4 is the result. Values plotted in Fig. 4 are shown in Table 4.

We have two points that are circled. We feel that those points are out of line, possibly due to the fact that poor leveling was done. It may have been some other condition that threw those points out of line. Do not expect that all your points are going to fall in line when you plot these values. They are not. You must use considerable imagination. The other points looked good, and we drew in the line.

You will notice where the line crosses the zero axis or where the perimeter times depth is zero, the value is 11. That is a constant value of 11. That number 11 has a significance. The constant is the value that would be approached if your flask weight was diminished to zero. In other words, there are certain elements in this assembled flask that you have to do regardless of the weight of the flask. If the flask weighed only one ounce, you would still have certain elements to do, and those elements would in this case amount to 0.11 of a minute. If the points that you plot will line up well and you feel the timestudies you have used for that particular element are good, and that your timestudy men have done a good job on their leveling, you can stop right there and derive your formula for that particular element.

V. J. SADLER, JR.:[1] On the graph in Fig. 4 you have ten different element values. Do you always set a formula on ten different studies, or would you take more than ten?

MR. ANNICH: If you feel that those ten studies are good studies, and if they give you consistent results when you are setting up a formula, it is not necessary to go any further; at least for the regular elements.

You may have certain jobs where you have not covered all the elements that are in those jobs, and you will have to take special timestudies for those elements alone. If you can cover the entire range of all your operators and all your flask sizes, small flasks, moderate flasks, and large flasks, 10 studies would be minimum. 15 would be good and 20 would be better. Most of the time, though, you only confuse the issue by taking more studies. Providing you get the right definition of different flask sizes and different conditions that you come across, it is best to keep the number of studies at a minimum.

In order to avoid confusion about the terms "constant" and "variable" as pertaining to the graphs, we have substituted the term "regular element" for what is usually known as a "constant" element in our timestudies, and similarly we have substituted the term "extra element" for what is usually known as a "variable" element in our studies. We have then regular elements and extra elements in every operation. Regular elements are those elements which occur every time in the cycle. The extra elements are those which do not occur every time in the cycle. They depend on a particular job and are peculiar to that job.

On one particular job, you may have three cores to set. On another job you may have no cores to set. On one job you may have a chill. On another job, you may have no chill. On one job you may have three gates to cut. On another one, you may have one. These are known as the extra elements. But there

[1] General Foundry & Mfr. Co., Flint, Mich.

are other elements like heaping sand, peening, squeezing, assemble flasks, carry out, pattern draw, and the cope lift, which occur every time the job is done; and they are what we call the regular elements.

Once you have derived your formula from the curve for a particular element, you can figure the synthetic standard or the synthetic normal time for any flask size. You line up eight or ten flasks normally used in the shop, starting from the smallest to the largest as on the formula summary sheet (Table 7), and calculate the normal time for each flask size and put it in under this element, Assembly Flask, as in Table 3.

If you have an 11 x 18 x 5/5 (5-in. cope and 5-in. drag) flask, to get P x D, your perimeter would be 58 in. and the depth is 10 in., so the P x D is 580. Now the formula value from the curve is 0.026 for every 100, so the total is 0.026 times 5.8 or 0.151 plus the constant of 0.11, which equals 0.261.

When you have drawn this graph (Fig: 5), if you feel that it is as far as you need go, then you can derive your formula for that element immediately. There may be some cases where you feel you should go further, that you should eliminate some elements you feel possibly were not leveled properly. So you go through a more complicated procedure to determine what elements were good and what elements were no good as far as leveling is concerned. That procedure is shown in Table 6.

The mathematics involved is not difficult. When you have gone through the procedure once or twice, it becomes simple.

We are trying to go back to our original timestudies on the job. We say: This man did the job in so much time. Based on that time, what leveling factors should he have had compared with what leveling factors the timestudy man gave him? So we go back to the original, actual time taken from the timestudy (Table 6). That is not the normal time, the time that has been leveled, but the actual time from the timestudy. We have a constant value (Fig. 5) of 0.11, which will recur regardless of the size of the flask. We want to find what the variable value is, so we deduct that 0.11 and get the variable time in Table 6. That variable time is for flasks of different sizes, and we have to make those times comparable. Since we have assumed that our times will vary with the perimeter times the depth, or with the weight, then we have to convert these variable times to what they would be if the flask was the same weight in every case or the same P x D. So converting them to a value of 1000 P x D, we would get the values shown in column 3, Table 6.

Having that, we add our constant of 0.11 in again because that is part of the element. We then have an adjusted time for each study equivalent to that for a flask with 1000 P x D.

You cannot plot time actually the way it is, i.e., you cannot plot it and get a straight line. So we plot, not the time but the times per hour that the element could have been done in those times. In other words, if a man does an element in one minute, he can do 60 per hour. If he does it in half a minute, he can do 120 per hour; so we convert those times that we have in that column to operations per hour, as we designate it, and then those operations per hour should be directly proportional to the leveling factor used to begin with. So the last two columns (Table 6) give you operations per hour and the leveling factor used in the timestudy.

From these values we plot the curve in Fig. 6. Now you see that two points are out of line. The rest of the points seem to be in line, and if you draw a line from the zero and through the average of all the in-line points you can read the value from that curve just by going up on the 100 leveling factor line and reading the value. That will be your normal operation per hour.

To get this operation per hour is simple. Since you have 60 minutes in an hour, divide 60 by the time to give you the operations per hour. After you have your normal operations per hour read right from the graph, then you can convert that back to normal time by dividing it into 60 again. That will give you the normal time.

It is not necessary to go through all these steps on everything you do, but you will find occasion when you will want to have this method as a means of getting the right answer. I struggled many times trying to find what I felt was the right answer, because I was so confused with leveling factors that were all wrong, and other factors entering into the picture. To avoid confusion, follow this procedure of plotting the operations per hour against the leveling factors originally used in the timestudy.

MEMBER: Do you find that the P x D, which is equivalent to weight increase, actually results in a straight line curve? Would it not result in a curve that would tend to go up as the weight increases and then taper off?

MR. ANNICH: I am opposed to using any curve other than a straight line curve. If you get one that is different from a straight line curve, you have to break it down to get straight line curves out of it. You can see from Fig. 5 within the limits on the weight of the flask in a flask job, we would get a straight line curve. I have had problems before where some points appear down here and some points up here, and it looks definitely as though you would have a curve. If you do get a curve and feel that is the kind of curve you should use, approximate it with straight lines, otherwise you cannot set up a formula without having to pick values from your curve.

R. CATLETT:[2] I assume that these assembling flask plates were drawn to arrive at a normal. Does that include parting? I did not see an element, liquid parting, or justifying included in assembly.

MR. ANNICH: Not in the assembling of the flask; that is only in the assembly of the snap flask before you start to mold. You do have one parting as one of the extra elements. It is shown in Table 3. But in Table 7 we have only regular elements, and since we do not use the parting element all the time, it is not considered a regular element.

MR. CATLETT: The regular procedure will probably be three out of ten.

MR. ANNICH: It is possible. If you feel that it is used often enough, you should include it in the standard and then put it into your regular elements. In this case, it was not used often enough, so I did not put it in the regular elements. That is the criterion. Make allowances for separate elements whenever it is necessary in your formula. I have not tried to cover this formula in this paper in absolute detail. I just tried to point out the method that we used in deriving it.

M. M. MRVICHIN:[3] Is this P x D factor used to represent the weights?

MR. ANNICH: Yes, it is approximately equivalent to the weight.

MR. MRVICHIN: Why not use the weight directly?

MR. ANNICH: You can if you want to, but I just did not have it available at the time.

V. R. WAGNER:[4] In tapered flasks, with a given P x D, if the drag is much deeper than the cope in one case, and the cope deeper in the other case, the weight of the one with the deep drag could be much greater than the one with the deep cope. Do you not take that into consideration?

MR. ANNICH: Yes, if these were tapered flasks, but they were not tapered flasks that we were working with. In that case I would definitely use weight. I have combined both cope and drag and put both cope and drag elements in one lump. But actually in doing the job, if you want to be more accurate on your standards data, you should split up the cope and drag elements, because you may have a flask, let us say, 11 x 18 in., with an 8-in. drag and a 4-in. cope; and you may have another flask with a 4-in. drag and an 8-in. cope. Now, if you have them all combined and call it 11 x 18 x 12 for the total depth, you will get a different answer where you have the drag and the cope separate, than where you have them combined. So in order to comply with the best definition on the thing, you should pull your elements together by cope and drag separately. It makes the formula more complicated, but it gives a better standard. If you find normal time from your formula, then you have the question of how much to allow for personal (P) and delay (D).

Consider Assemble Flask, for instance. You get your normals for these various sizes. Then you have to refer to your fatigue table (Table 2) and make allowances for personal and fatigue based on weight; and the D factor is obtained from all-day studies, and that is allowed too. For instance, in this particular paper we assumed a fatigue factor of 0.02, or two per cent; and then, based on the weight and the type of movement involved in the element, you make your personal and fatigue allowances, add your D factor to that from Table 2. You get

[2] Minneapolis Moline Power Implement Co., Minneapolis.
[3] Pettibone Mulliken Corp., Chicago.
[4] Kondu Mfr. Co. Ltd., Preston, Ontario, Canada.

your PF and you add your D factor to it and you multiply that by the normal time from your formula for the element, and put that value into your formula summary sheet (Table 7). You follow this method for each element; there may be different factors that you might use other than weight. In other words, in some of your elements as in sand handling, there is the factor of volume, and you plot that against volume. In the element, Riddle Facing, we correlated our times with the flask area, or pattern area. After you have derived all your formulas, put in your personal, fatigue and delay factors and record the values on the formula summary sheet; then you can get a total of all your regular elements.

Due to the fact that your fatigue factors are grouped or bracketed in certain groups based on weight, you will get somewhat erratic results which you can iron out in this way: If you will look at Fig. 13., you will see plotted the total of all the regular elements for each of ten flask sizes against the flask volume itself. You notice that some of the points come above and some below the line. That, in general, is due to the fact that the fatigue factors allowed were bracketed by certain weights so that in one case you would get perhaps a difference in flask weight of only 2 lb., and yet it would throw the fatigue factor in the higher weight bracket thus making an allowance out of proportion to the 2-lb. difference in weight. Then if you used these standards as they are, it would give you erratic results. You smooth out these variations by plotting against flask volume and then getting a formula for the total of regular elements. In this case, the stated allowance would be 1.9 plus 0.119 per 100 cu. in. of flask volume. That is the formula for that curve. On p. 235, we show the type of form used for the various formulas. You can use any form you wish. In this case we have it so that we can break in a man easily on working up the formula. The man who is setting a standard has his specifications for the job, such as the number of gates to cut, cores to set, etc. In order that he will not miss anything, he goes right down the line to make sure that he has covered everything.

If you come across some element which you have not considered in the formula originally, make some studies to get that particular element, and then gradually extend your formula by the data on these new elements as you get them. Just as a check, on page 235, we took the flash specifications for that sample timestudy, and from the formula we derived what the standard should be. The standard comes out to 5.79 standard min. That standard of 5.79 min. compares with an overall study cycle of 4.97 min., showing that for that timestudy (Fig. 3) the operator was working 116 per cent of standard. Some people will think that probably you did not rate the man more than 100 per cent of standard on the average, why should he get 116 per cent? The answer is that you have made the fatigue, personal and supplemental or delay allowance, and while you were taking the study, that man was not getting any of those, or very little of them. That is why at that particular time he did it 116 per cent of standard. But working at that same rate a full day, he would not rate 116 per cent of standard because he would stop to smoke, to go to the rest room, and to relax once in awhile. Because some of those elements like the Carry Out, are difficult, and when a man does them 100 or 110 times a day, he will get tired. That is why you make the fatigue allowance.

E. THOMPSON:[5] Information that I have been getting here stresses lighter work, bench molding and snap flasks. What do we do when dealing in big castings, where the molds will contain a 20,000-lb. casting?

CHAIRMAN FISHER: The same thing.

MR. THOMPSON: With the pit work also?

CHAIRMAN FISHER: The principles apply to large size molds as well as snap flask and bench work.

MR. THOMPSON: There is a lot of grinding here on all sizes of castings. Can this be worked up from a series of timestudies and then charted and graphs drawn, so it can be used for basic standards on different kinds of grinding regardless of size of castings?

CHAIRMAN FISHER: That is right. If your data are developed finely enough, it is possible to take blue prints even before pattern equipment is made, and in discussing with supervision, discover how they would make the casting if they had the choice of pattern equipment, and set your standards before you ever get it in the shop.

MR. THOMPSON: Would you say that I was wrong if I said I set up most of the work based on the weight of the casting?

CHAIRMAN FISHER: If it is a jobbing shop, I would say yes. If it is production with similar lines of work, it might work.

MR. THOMPSON: It is not a production shop. It is a jobbing shop dealing with castings running from extremely light castings up to castings weighing from 20,000 to 30,000 lb. There is a wide variation. I work it exclusively on the weight per casting.

MR. ANNICH: The only reason we chose the light work here was so we could show its development in a small number of pages. Heavy work would require about 150 pages to discuss. It will work, and it is a question of using your imagination when you start to assemble this data. Not every man will be suited for getting this data together. You cannot get an accountant or bookkeeper to do it. You have to pick out possibilities and try them on graph paper. That is what I mean when I say try them and see how close they work; how your points fit in. There usually is a solution to it.

We have done it on big work. Ours is not as heavy as some of the castings mentioned, but we have done it on work up to where you have five and six days' molding time for two men. There again it is a question. Sometimes the formula that you set up is not definitely tied down. Where the fellow sets the standard from the formula, he has to use his imagination, too. You may put the castings into certain classes. For instance, on the finish allowance, you may classify castings into Class A, Class B, Class C, and Class D, depending on the complicity of the casting. Then it is going to be up to the standard setter to pick the right class so he sets the right standard.

That is the solution we have used, and it worked out well. Other people may have different solutions. If you could tie it down more definitely, it would be much better, but that is the one we have used, and it has worked.

CHAIRMAN FISHER: In our own plant, strictly a jobbing steel foundry, we have standard data on castings running considerably higher than 20,000 lb. We had one mold gear that weighed about 40 tons that we applied standard data on and it worked nicely. A molder and helper earned about 35 per cent premium on the job, using these same principles. Our data on just the charts alone is considerable to cover all conditions, and it is by no means complete. We have two full-time time setting men working constantly on getting data on that floor. We will never get done with it, but it has paid off in reduced costs.

R. R. MCCLINTIC:[6] Can you give me some idea of the cost of administering this type of timestudy. I have had one experience where the timestudy department, which consisted of time setters, standard checkers, and what not, becoming unwieldy, and it was necessary in that particular type of plan to make extra allowances on the floor to cover unforeseen conditions and so on. The cost of administering the plan was excessive for what they got out of it. Is it the practice in your foundry to operate with a large force?

CHAIRMAN FISHER: We employ around 475 men in our steel foundry. That is including indirect labor as well as so-called direct. About 90 per cent of our labor is on standards. We have covered the indirect labor with group plans, which we do not approve of very much but were forced into it during the war. To handle that whole thing we have four timestudy men, two standard setters and two clerks in the office. We have five timekeepers, but we had them, anyhow, before we had standards.

MR. MCCLINTIC: Is it your practice to include in your standards sufficient allowances to cover the average condition and let the unusual situations balance themselves out? Or do you make it a practice, at the request of the foreman, to make extra allowances?

CHAIRMAN FISHER: We make the extra allowances as they are needed.

MR. MCCLINTIC: Most of my timestudy experience has been in steel mills, and that was definitely not our policy. They took the bitter with the sweet and it tends to simplify it.

CHAIRMAN FISHER: You would have to set up your standards considerably looser in that manner to balance out.

MR. MCCLINTIC: They were, it is true. On the average, how-

[5] Baker Perkins, Inc., Saginaw, Mich.

[6] Oil Well Supply Co., Oil City, Pa.

ever, the earnings on a two-weeks' period were fairly consistent.

CO-CHAIRMAN WESTOVER: You can get away from a lot of checkers and extra allowances and so on if you will set looser standards and then use a formula like the Halsey 50-50 Premium or Rowan formula to automatically bring earnings into line. A lot of companies have a wide variation in their operating conditions and could well use a formula like these that will take care of a lot of troubles caused by variations.

There are also some other formulas that are not as well known that do the same thing, so the matter of having checkers is really a management decision, depending upon how accurate they want to control costs. If you would allow for a certain amount of variation, if you have variations but think it is not worth the cost to try and eliminate them, or control them, then you can eliminate checkers.

MR. MCCLINTIC: What is your practice or policy where your studies indicate that the men are working far below even a break-even point? Do you attempt to get your production up to a reasonable level before you attempt to establish standards?

CHAIRMAN FISHER: When you are leveling, if your leveling is done properly, it does not make much difference at what pace the man is working.

MR. MCCLINTIC: It is not the pace the man is working when he is working—it is when he is not working.

CHAIRMAN FISHER: That is one of the first things you do when you go into the department, is to remove all the reasons for a man not working. At least, you try to do it.

MEMBER: We had the standards set four years ago, when everybody said they never could be equaled. They were justly set by the timestudy department. Within a year's time those standards were exceeded, and today the figure is 175 per cent of those standards which two years ago they could not even equal. When the men are properly trained and they finally get it on the pay check, that old standard goes up so fast you exceed it immediately.

S. M. ZIMMERMAN:[7] I am looking for information on how to go about getting an incentive for maintenance men and so on. We have some plans which have worked out successfully. I am wondering if any other foundries have any group incentive plan that might be applied that way.

CHAIRMAN FISHER: Some people have told me that it is possible to set direct incentive on all of these extra-curricular activities around the shop, but I would like to find out if it really has been done and if it is successful, because we are faced with that same problem right along.

MEMBER: We take the attitude that the shop people can affect the productive efficiency of the plant; and their bonus, therefore, is set on the basis of whatever their income happens to be per hour. If that productive efficiency is, for instance, 100 per cent, which we use as standard, they get paid a percentage on their base pay over and above that.

CHAIRMAN FISHER: Most of us have done something similar to that, except that I feel that the incentive to the millwright, or whoever he is, is very remote. He can see no connection between the efforts he puts forth today and the money he receives at the end of the month in extra compensation. So far as he is concerned, personally, it is nothing but a gift to him. If you could get that incentive directly to the man, you could save at least 25 to 50 per cent more.

CO-CHAIRMAN WESTOVER: By using budgetary allowances and then putting a very heavy weight on any delay time that occurs in the shop as a result of mechanical breakdowns, the maintenance department will be watching carefully any delay time that is reported as a result of anything that they should have prevented which will be charged against any premium they earn. Your budgetary part is to hold your indirect costs on maintenance to a relationship with the amount of work that is being done and accounted for as direct costs. Those plants that have time standards have a good measure and they can relate their indirect to their direct in what will be a fairly constant proportion. Some of you using a money measure have a greater problem, in that as your money value changes from time to time, the ratios would therefore change. Then you probably would have to make a lot of corrections. Whereas the time standard method does not present that problem.

K. A. POWELL:[8] On the question of synthetic standard time for pattern work, I am wondering if the Westinghouse system used on jigs and fixtures might not be applied.

MEMBER: We want to establish standard data. If we set a synthetic time from our synthetic data and a man disputes it, how can we back up that timing?

CHAIRMAN FISHER: In our particular instance, we do not pay much attention to what the man says until he has convinced his foreman that the time is too short. If the foreman makes a request for a check on it, then the timestudy man goes out to the work station with the foreman, and both of them observe while the man makes the job, and the timestudy man times him.

We find that in most instances part way through it the foreman will turn around and say that the man did not work like that before. If the fellow works like that, he will make the job, and we walk away. If he gets that unsympathetic attitude a few times, he does not complain unless it is tight. We check with the foreman to see whether we have all the work in it first. Sometimes the standard setter leaves out something. That has happened. Or the man might be making the job in a different method than we have set it up for.

MEMBER: There were instances in our plant where we could not back it up other than by taking an actual study.

CHAIRMAN FISHER: You could back it up on the basis that standard data has been worked out on other similar jobs on which there was no complaint, and why should this one be so different that the data does not apply? If the man cannot show you that the data does not apply to it, then there is no argument there at all.

MR. ANNICH: When you make up what you call the formula summary sheet, you still have all those elements in there that you have summarized. When you take a timestudy on this particular job the man is complaining about, you can check it with that formula summary sheet. If that still does not satisfy, the only thing to do is to go back to a job that is similar, where you have timestudies to back it up. The main thing about the standard data is that it is not a standard set from one timestudy. It is a standard set from an average of many timestudies taken at many times of many operators by many observers.

MR. MRVICHIN: In speed rating a core maker ramming up a core and performing other core making operations, how would you rate that when a core maker's normal rate of motion is faster than anything we have seen demonstrated as normal?

CHAIRMAN FISHER: By definition, could he do that particular element of work for eight hours without undue fatigue?

MR. MRVICHIN: They have been doing it for years. I mean the ordinary speed with which a core maker rams, or, rather, the normal speed at which the entire department will perform that particular element, is faster than the forearm demonstration.

MEMBER: That is just one element in your whole operation. The time he is going to make up on that will not necessarily influence the whole operation. By that one motion, even though it is 200 per cent, he only does that for such a small time that the additional incentive he gets for that extra effort would not change the overall picture much. The operation has a lot of other elements in it that he is slower on.

MR. MRVICHIN: You mean that one element should balance out another one?

MEMBER: It will tend to keep it on a level basis. He will be fast in some and slow in others.

MR. MRVICHIN: I am interested in that one particular element, though. The others are simple.

MEMBER: He could not keep that up all day.

CO-CHAIRMAN WESTOVER: I am wondering if he is moving that arm a full action. The rammer movement is a very short movement.

MR. MRVICHIN: That is exactly what my question is.

CO-CHAIRMAN WESTOVER: Now, if all your operators normally work at a fairly fast pace, pick out what your normal for that particular shop is. It may be different from what these are, but you are not comparing on those tables the standard time of the short action against that full action. You have an altogether different picture of what would be normal. In other words, are you using a little butt rammer?

MR. MRVICHIN: Yes.

CO-CHAIRMAN WESTOVER: For that short movement of the small butt rammer, your normal time is maybe one-fourth of

[7] Hansell Elcock Co., Chicago.

what that full arm action is, so that actually you have a faster action there just for normal.

MR. MRVICHIN: In other words, you disregard the normals demonstrated and you go back to the normal for the conditions of the particular shop. Is that right?

MEMBER: No.

CO-CHAIRMAN WESTOVER: Yes and no. I have not been able to pick out and use a normal from one section of the country to another. In other words, I am in disagreement with this normal. I find that I have to judge it more on the normal of that particular shop, the pace at which that particular shop is working, what seems to be the general feeling, because some shops would annoy you on these normals because they are already used to such a fast pace. I have one in mind that is about twice what you normally run into. In other words, if I would use that normal for those ratings in that shop, those ratings would be so low that they would be running 250 per cent. Normal is when the men will run 133 to 150 per cent.

CHAIRMAN FISHER: I hope nobody got the impression that they could use the values in here in their own shops, because if you have that idea, you are going to run into much difficulty. The values as given in here apply to that particular plant where they took these timestudies, and that is the only one it does apply to. The principle used will be applied any place, though, and that is my idea on these level pace rating factors that he has used. The factors that he has used have worked out satisfactorily for him in the plant where they apply. Possibly in our plant we would have to raise or lower them. In your plant, you might have to raise or lower them, or you might be able to use them as given. I do not know. Please do not try to use these values as you get them out of this book, because you are going to have much trouble if you do.

CO-CHAIRMAN WESTOVER: Do you agree or disagree with both of us on that?

MR. ANNICH: I disagree with Messrs. Fisher and Westover. One of these days we hope that a day's work in Kalamazoo will be the same as a day's work in Houston, Texas. There are a lot of factors involved in making them the same, and we know and I will grant you that they are not the same right now. And more power to the shop that has what they consider a normal which is 125 per cent of what I consider normal in this paper. If you have a shop like that, be careful how you use these things. Is that what you are getting at?

CO-CHAIRMAN WESTOVER: Yes.

MR. ANNICH: Probably if you have something like that, you do not have to worry about timestudy or incentive. Very definitely it is the man who has an average pace throughout the shop of 50 per cent who has to worry about incentive, and he is the man who is going to be happy to use these normals and get them across without loosening up on them.

These things are going to be tried. They are going to be found wanting in some instances. But we are just making a start now. We should have made it possibly ten years ago. We are making a good start now, and as the years go by and we get more people interested in these things and get more experience in them, we are going to be coming together year after year and checking our results together. Then we are going to get something that is worthwhile. Let us look at this paper as a start on that program. Possibly ten years from now we will realize something that will be a benefit to all of us.

CHAIRMAN FISHER: But you still would not advocate them going out and using your chart values in here?

MR. ANNICH: Not if they have a shop that they feel is over the normal that I have presented here.

CO-CHAIRMAN WESTOVER: The forearm test illustrated a method by which a standard is set. If you will observe your method before you start in taking timestudies and then specifically describe the method so that you can refer to it you will be able to justify any change in time standards due to changes in methods and the new standards will generally be acceptable to all parties concerned.

A NEW GATING TECHNIQUE FOR MAGNESIUM ALLOY CASTINGS

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ABSTRACT

In the past, the need for avoiding turbulence in the pouring of magnesium alloy castings has dictated the use of some form of bottom gating for the great majority of shapes cast in this group of alloys. Bottom-gating methods have proved particularly effective in minimizing pouring turbulence, thereby avoiding the inclusion of non-metallic films in the casting.

However, bottom gating as a rule sets up basically undesirable conditions for the application of controlled directional soldification, and for this reason such gating methods increase the difficulty of chilling and risering castings to soundness.

The authors describe a new gating technique which makes it possible to introduce magnesium alloys into molds without flowing large amounts of metal through the lower portions of the casting cavity. It has been found that freedom from radiographically visible microporosity is achieved more economically with this gating method than with conventional means of filling the mold.

The favorable mold-filling conditions attained by this gating method make it possible to arrive at a high degree of radiographic soundness at a lower ratio of poured weight to shipped weight, and with the use of fewer chills, than with previously utilized methods. Further, castings of excellent freedom from included non-metallic films have been attained. Experimental and production experience with this gating method is described.

THE PHYSICAL AND CHEMICAL CHARACTERISTICS of a casting alloy dictate to a great extent the means by which it must be gated and risered to secure satisfactory castings most economically. In some alloys, unusually high density or high melting point necessitate special and difficult techniques. In others, high affinity for and sensitivity to dissolved gases create a necessity for costly foundry methods. In magnesium alloys, the high chemical activity of the liquid metal has created special foundry problems.

Partial solution to these problems has come through development of gating techniques suited to the characteristics of the metal. The purpose of this paper is to present to the industry a new gating method by which some of the problems created by the chemical activity of liquid magnesium alloys have been overcome more economically than when using previously published gating methods.

Bottom-Gating Used Widely

Since liquid magnesium alloys react rapidly with the moisture and air in molds to form oxides, it is necessary to minimize gating turbulence and to prevent any turbulence from occurring within the casting cavity. For this reason, bottom-gating methods are used widely with these alloys. These methods have been discussed previously in the literature.[1]

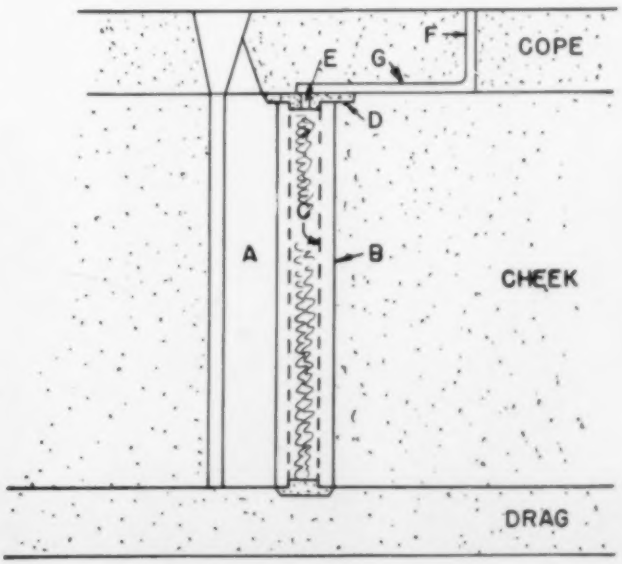
In such methods, for the most part, the metal is first brought, in the gating, to the level of the lowest part of the casting cavity. Here it usually is filtered through skim gates or strainer cores; and then is caused to enter the casting cavity at its lowest extremities, and to rise without turbulence until the mold is full.

While bottom-gating methods are effective in preventing the inclusion of non-metallic films in the casting, they set up basically undesirable conditions for securing directional solidification. For this reason, gating systems have been sought which do not require all the metal to flow through the lower portions of the casting cavity.

Side-Gating Developed

Beck[2] described a side-gating technique which has found considerable use in the casting of vertical-walled

Fig. 1—Basic features of the new gating system. Letters shown refer to description in text.



parts. Baker[3] has discussed other gating variations intended to secure soundness through controlled directional solidification, although his work dealt chiefly with relatively small, experimental castings.

In the casting of large vertical-walled parts, side-gating techniques represent a considerable advance over bottom gating. However, for reasons to be discussed the authors believe that conventional side-gating systems are not completely satisfactory as means of introducing magnesium alloys into molds.

The new gating system discussed here seems to provide more favorable mold-filling conditions. Not only are more favorable conditions for controlled directional solidification set up with this system, but castings of excellent freedom from included non-metallic aggregates result.

New Gating System Described

Basic features of the new gating system (sketched in Fig. 1) include a slot gate *(A)* which continuously connects the casting with the well *(B)*, as in the conventional side gating described by Beck.[2] An annular screen *(C)*, placed concentric with the well *(B)*, consists of a cylinder of tinned steel skim gate, commonly used in magnesium-alloy founding for filtering the metal stream. This cylinder is located at each end by cores placed as sketched in the drag and in the cope-cheek parting. A quantity of coarse steel wool is placed loosely inside the annular screen.

The top end of the annular screen is located by the core *(D)* in the cope-cheek parting and through this core there is an orifice (E). The metal enters the mold through the sprue *(F)*, flows through the runner *(G)* to the orifice *(E)*, then drops through the annular screen containing steel wool, whence it flows through the slot gate into the casting cavity.

With this gating system, the flow of metal into the

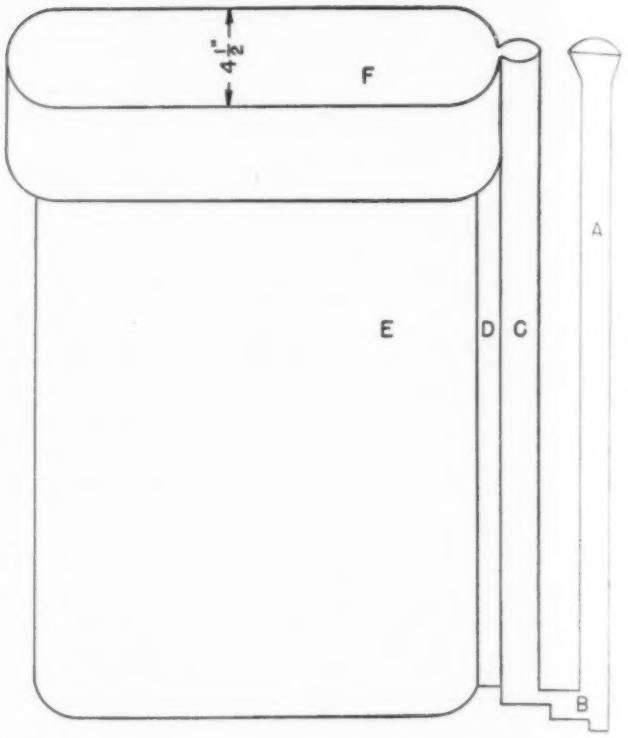
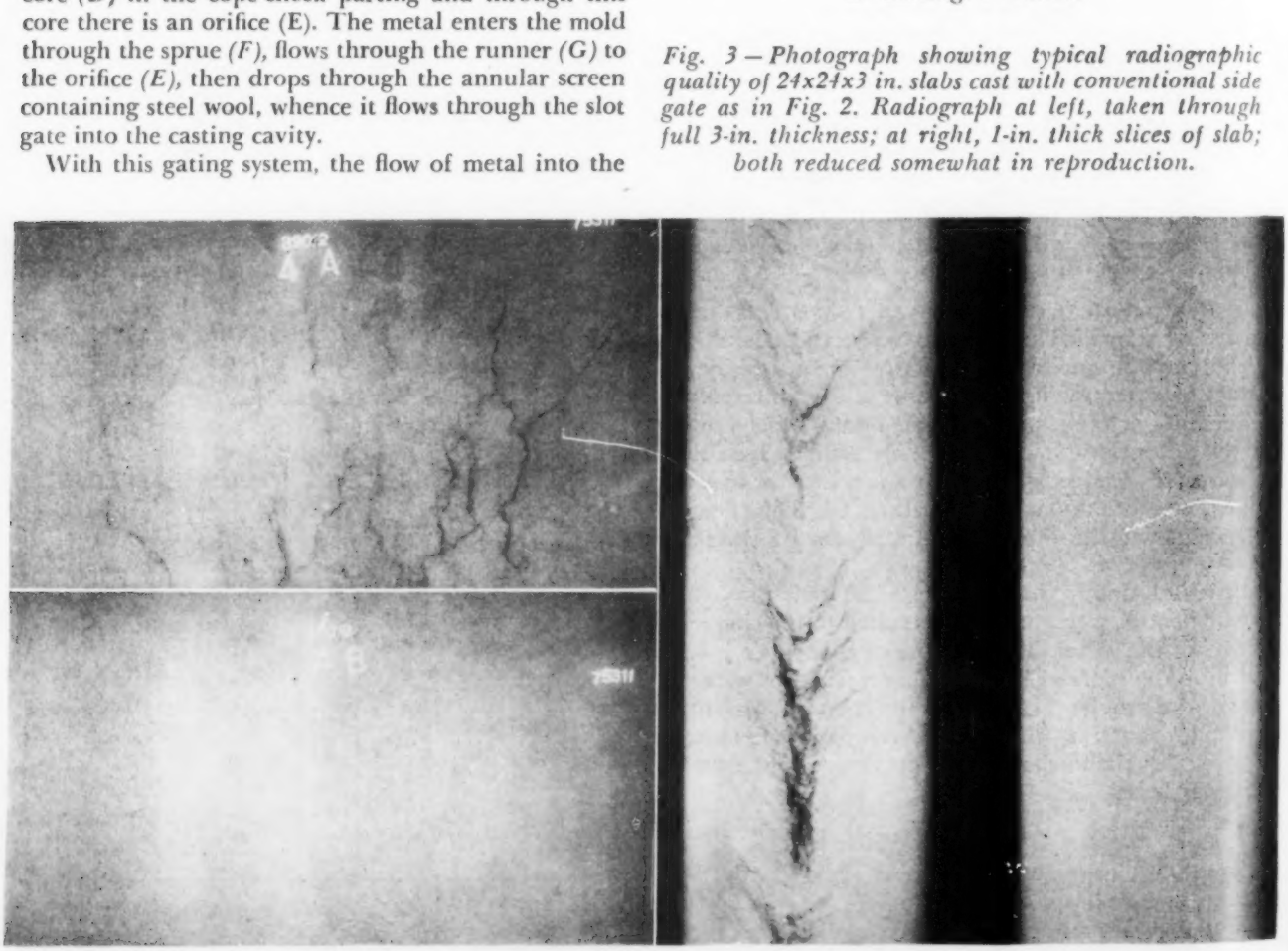


Fig. 2—Sketch showing the conventional side gate, first applied to the casting of a 24x24x3 in. rolling slab. A—Sprue. B—Screen pocket. C—Well. D—Slot gate. E—Casting. F—Riser.

Fig. 3 — Photograph showing typical radiographic quality of 24x24x3 in. slabs cast with conventional side gate as in Fig. 2. Radiograph at left, taken through full 3-in. thickness; at right, 1-in. thick slices of slab; both reduced somewhat in reproduction.



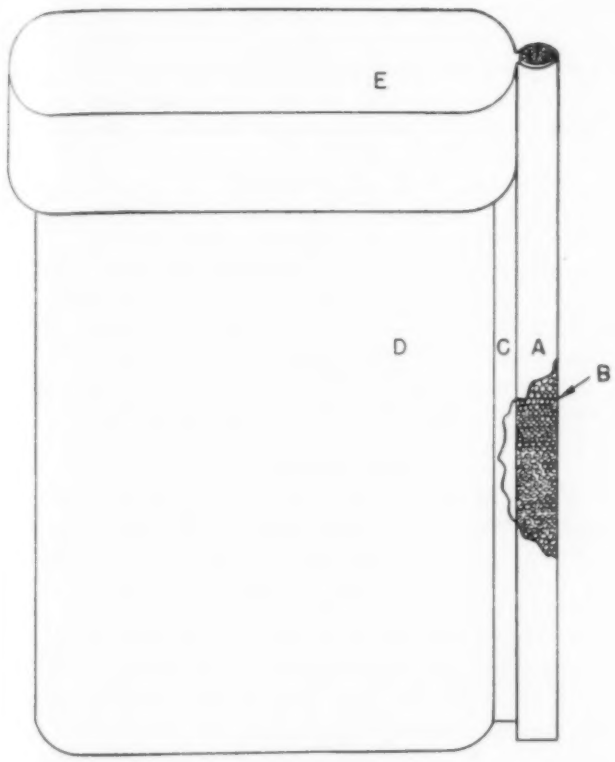


Fig. 4—Sketch showing application of the new gating method to the casting of a 24x24x3 in. rolling slab. A—Well. B—Annular screen. C—Slot gate. D—Casting. E—Riser.

Fig. 5 — Photograph showing typical radiographic quality of 24x24x3 in. slabs cast with the new gating system as in Fig. 4. Radiograph at Left, taken through full 3-in. thickness of slab; at Right, 1-in. thick slices of slab; both reduced somewhat in reproduction.

casting cavity is so controlled as to set up favorable thermal conditions for controlled directional solidification. The last metal poured (the hottest metal) comes to rest in the top risers, with little flow of metal through the lower portions of the casting cavity during the filling of the mold.

Oxides Filtered Out

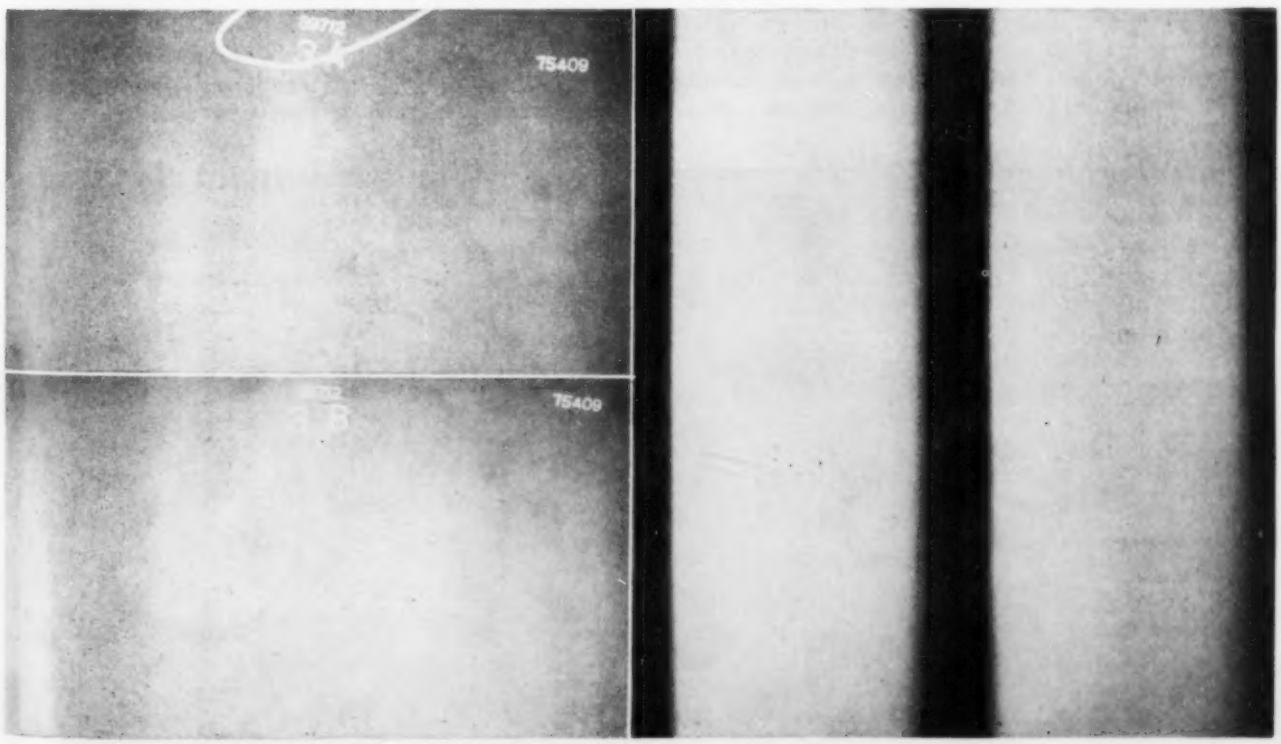
The annular screen serves to filter out any reaction-product skins which may form as the metal drops freely through the orifice down through the well. Thorough fracture tests of many castings made by this method indicate that this filtering action is effective in preventing such films or oxides from entering the casting cavity.

In early tests, the steel wool was not used inside the annular screen. As a result, the metal to some extent flowed through the upper part of the annular screen without first dropping and finding its level in the well. When this happened, the metal would run down the outside of the screen and the reaction products so formed would be washed into the casting cavity.

It was found that a small quantity of coarse steel wool in the annular screen prevented this type of flow. The metal stream followed the steel wool to the bottom of the well (or until it found its own level in the well), then flowed horizontally through the screen into the casting cavity.

Metal Allowed to Find Own Level

The function of the orifice is primarily to direct the metal stream into the center of the annular screen, thus further insuring that the metal will not be forced through the screen until it reaches its own level in the well. The orifice may also be used to control the pouring rate, although the sprue is more commonly designed to perform this function.



Other variations of this gating principle have been applied, as discussed later in this paper.

Experimental and Production Results

A—Cleanliness of Castings

The first casting poured with this gating method was a simple, vertical test panel. Fractures of a number of panels were consistently free of any indication of included skins or films of a non-metallic nature. Later evaluation of many other experimental and production castings confirmed this observation. It therefore was concluded that castings made by this method compare favorably in cleanliness with those made by any other method.

B—Quality of Castings

One of the first applications of this gating method was in the casting of an experimental rolling slab in a permanent mold. Attempts first were made to cast this shape by means of the conventional side gate sketched in Fig. 2. The radiographic appearance of the resulting slab is shown in Fig. 3.

After several attempts to obtain acceptable quality, the gating method sketched in Fig. 4 was employed and it was found that by pouring the metal slowly and directly down the well, the radiographic appearance shown in Fig. 5 resulted. Complete freedom from radiographically visible porosity thus was attained.

This was considered a convincing confirmation of the beliefs that initiated the study of this type of gating—namely, that more ideal filling conditions for directional solidification would result. In addition, the fracture quality of the slabs in which the new gate was used was as good as or better than that obtained with the conventional side gating.

C—Production Experience

These initial successes led to introduction of the new type gate into the production sand foundry, where it has seen its highest development. A great number and variety of aircraft-quality sand castings have been made by the use of this method. For purposes of this paper, it is not practical to describe in detail the numerous observations made, in this production experience, of the value of the new gating method. However, many economies have been achieved.

The value of the method hinges on the more favorable mold-filling conditions obtained than with conventional gating systems. All the metal poured is not forced to flow through the lower portions of the casting cavity, and the foundryman finds it much easier to avoid shrinkage defects such as draws and microporosity in the lower portions of the casting.

Production experience has indicated four major advantages inherent in the new gating system.

Foundry Advantages Listed

(1) A gating and risering system can be developed more readily to give acceptable quality in a new job than with conventional side gating or bottom gating. A successful gating system can be determined after fewer unsuccessful attempts.

In the experience of the authors, whereas previously it commonly required four or five attempts to find a suitable system for such parts as large aircraft wheels, more than two or three attempts now are rarely necessary when the new gate is applied. In one instance the new gate was successful on a large wheel after more than a dozen efforts had been made to secure satisfactory quality using conventional methods.

This advantage is attributed to prevention of the flow of large amounts of metal through the lower part of the casting cavity in the filling of the mold, thereby avoiding local overheating of the mold material.

Use of Chills Reduced

(2) Fewer chills are required to secure soundness. In numerous jobs converted from conventional side gat-

Fig. 6—Photograph of 110-in. wheel casting, showing application of the new gating method.

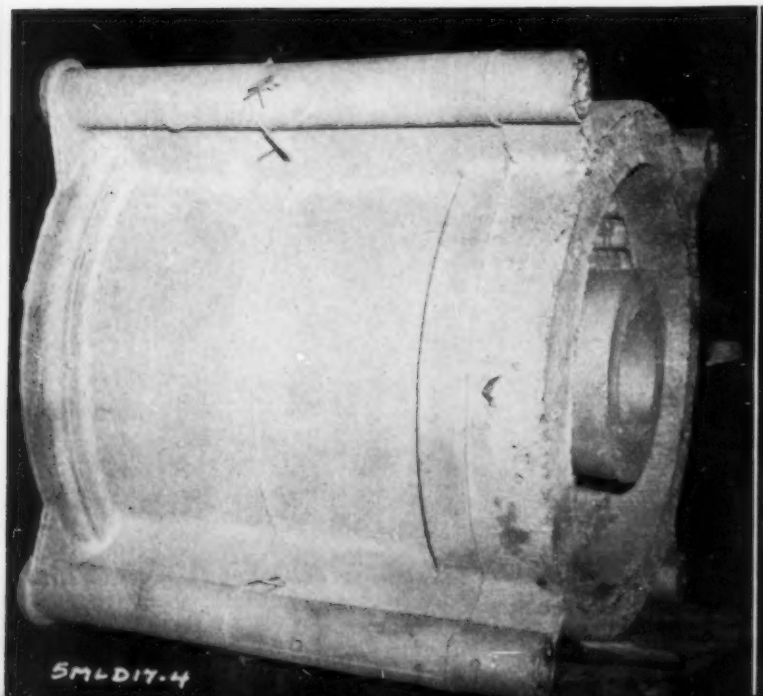
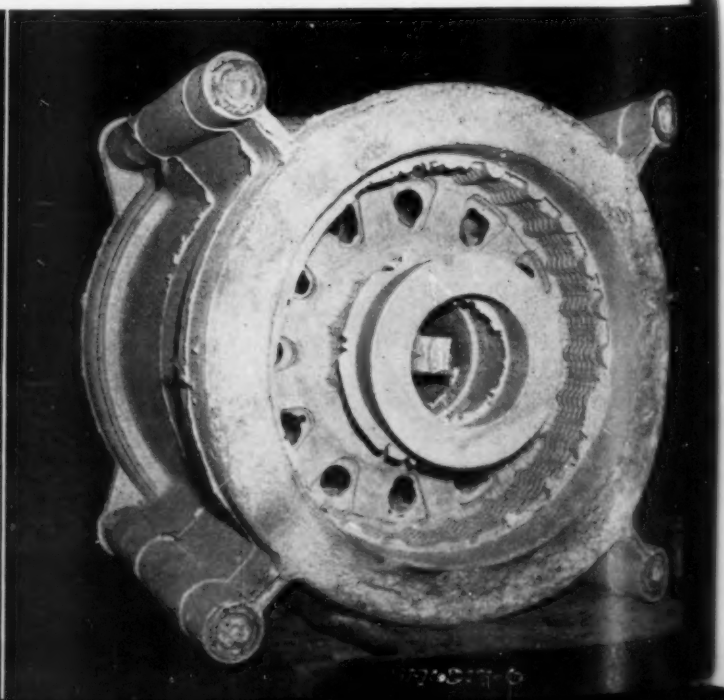


Fig. 7—Another view of the new gating system as applied to same 110-in. wheel casting shown in Fig. 6.



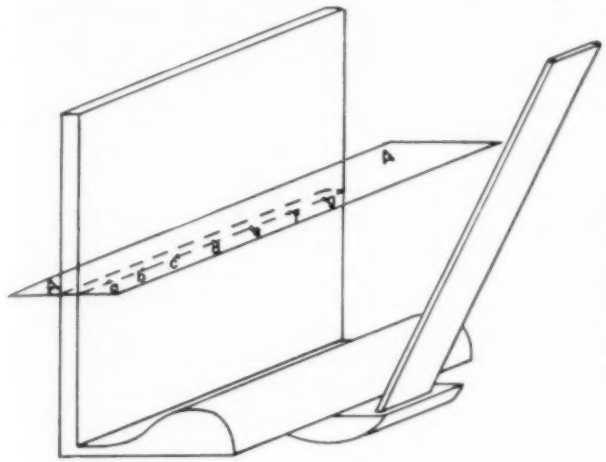
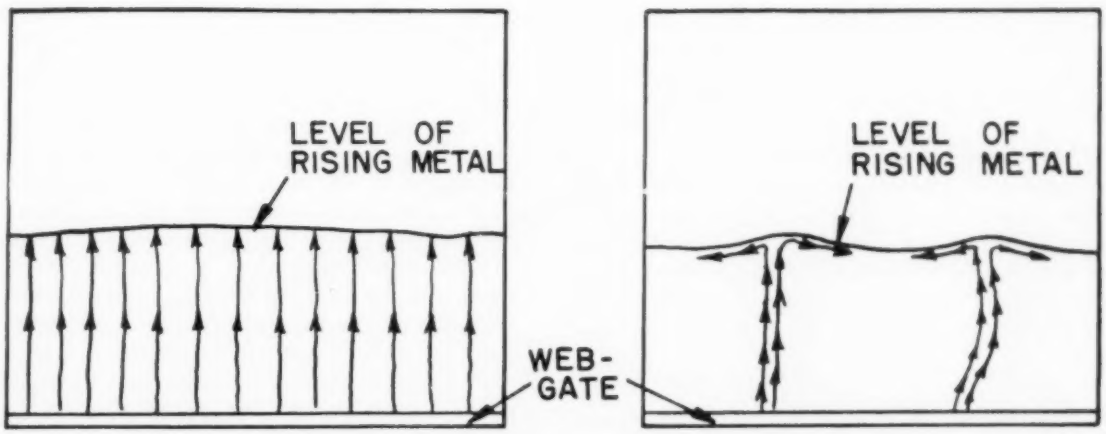


Fig. 8 (left)—Sketch of a vertical panel, bottom gated through a uniform web gate.

Fig. 9—Channelling of flow in bottom-gated castings. Idealized (left) and actual (right) flow conditions during the filling of a vertical panel bottom-gated through a web gate. While the same amount of metal flows, in theory, over each portion of mold material, actually most of the metal flows in a few random channels, overheating certain streaks of the mold surface.



ing to the new gating or some variation of it, it was found possible to eliminate many chills from the drag portions of the casting, without sacrifice of quality. This was accomplished without significant changes in the size, shape or position of feeders.

This observation indicates that with conventional side gating some chills are necessary to counteract the heating effect of the flow of metal through drag portions of the casting cavity. When a system is used which avoids such flow, the chills are no longer needed.

Uniform Quality Obtained

(3) Uniformity of quality from one casting to another is significantly increased. With conventional gating systems, random draws and random objectionable amounts of radiographic porosity are more common than with the new system. Even with simple test castings, the streaks of porosity associated with bottom gating are not found in the same location from one casting to another.

It is evident that the channelling of flow frequently is of random nature when much metal is caused to flow through the lower parts of the casting cavity. When this type of flow is avoided, a basic cause of the random occurrence of shrinkage defects is removed.

Increased Yield Results

(4) Substitution of the new gating system for conventional methods almost invariably has resulted in increased casting yield (ratio of shipped weight to poured weight), without sacrifice of quality. Such increases in casting yield were possible because it no longer was necessary to use extreme riser sizes in an attempt to counteract the tendency toward random shrinkage defects caused by improper mold-filling conditions.

Practical Application of Method

Early in the development of this gating method, an unusual opportunity for testing its value developed in the casting of a large landing wheel to be used experimentally on the B-36 bomber. The wheel had a shipped weight of approximately 830 lb and numerous attempts had been made to cast with conventional gating methods. Despite the most careful planning and utmost application of known principles, all attempts had failed to yield a production method which would insure satisfactory quality. The most serious sources of scrap were draws and radiographic microporosity.

The new gating method described here then was attempted, as photographed in Figs. 6 and 7. Draws were practically eliminated as a source of scrap, and much less microporosity appeared in resulting castings.

Magnesium Demands Minimum Turbulence

One of the foundryman's most fundamental controls over the quality of a casting lies in the manner in which the mold is filled with molten metal. The freedom of a

casting from included non-metallic aggregates, and from a great variety of other internal and surface defects, is dependent to a great extent on how well the casting is gated. One requirement of a good gating system is that pouring turbulence be kept to a minimum. While this is a desirable attribute of the gating system for any alloy, it is essential in a gating system for a magnesium-alloy casting.

The reaction of liquid magnesium with air and water vapor necessitates the use of inhibitors in the molding sand. These inhibitors prevent burning of the metal by reacting with the metal to form protective films. Since the films which form during the pouring of magnesium-alloy castings differ little in density from the metal itself, there is little tendency for gravity segregation to separate them from the metal stream, once they have been intermixed.

In gating magnesium alloy castings, therefore, two precautions are necessary: (1) Turbulence in the gating system must be kept to a minimum to prevent entrainment of films in the metal stream; (2) turbulence cannot be permitted in the casting cavity itself.

Why Bottom Gating Used

These necessities have led to the use of bottom gating as the most general method for introducing magnesium alloys into molds. In these methods, the metal is first brought to the level of the lowest part of the casting cavity with as little turbulence as possible. Usually, the metal is filtered at this point by causing it to flow through skim gates or strainer cores, thus separating any entrained films from the metal stream. The clean metal then is introduced into the lowest parts of the casting cavity, and is allowed to flow upward.

It is important that the gating be so arranged that the metal does not drop from one level to an appreciably lower level within the casting cavity itself. When this happens, protective films form on the surfaces exposed by the resulting turbulence and tend to be entrapped in the casting.

These are the considerations that have dictated the use of bottom gating to so great an extent in the founding of magnesium-base alloys. Bottom-gating methods have proved highly successful in preventing the inclusion of non-metallic films in castings. Great quantities of aircraft-quality castings have been produced by these methods.

Disadvantages of Bottom Gating

However, serious disadvantages are associated with bottom-gating techniques. One of these involves the point that all metal poured must flow through the lower portions of the casting cavity, with the result that the last metal poured (the hottest metal) comes to rest in the bottom of the casting cavity, while the metal poured first (consequently, the coldest metal) comes to rest in the top part of the casting cavity or in the risers.

Since top-risering often is necessary, this results in adverse conditions for directional solidification. Further, the mold material adjacent to the lower parts of

Fig. 10—Method by which a 24x24x⅜-in. panel was bottom-gated through a uniform web gate.

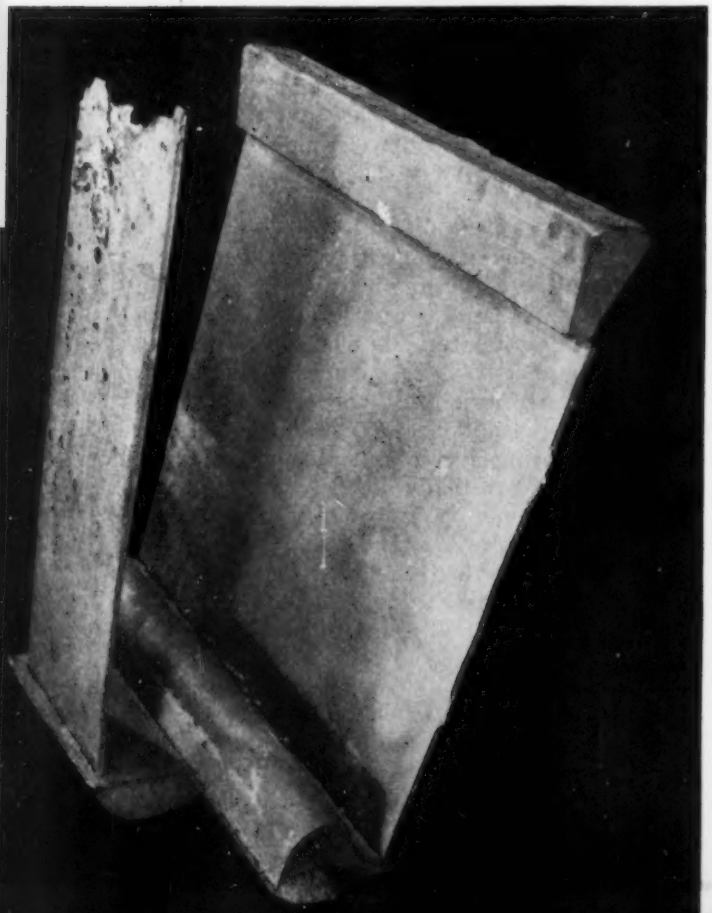
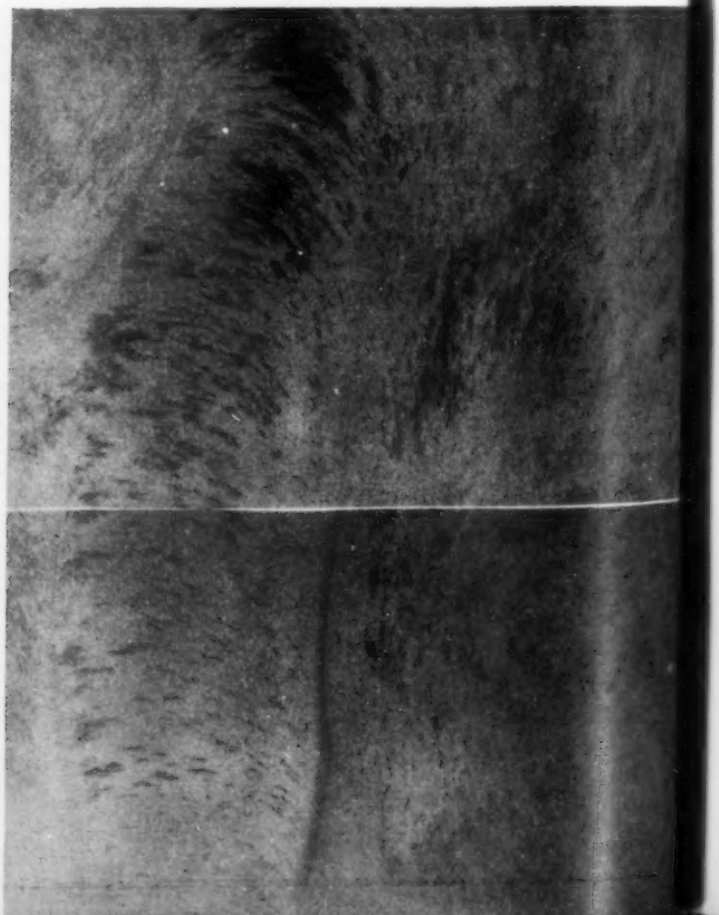


Fig. 11—Radiograph of 24x24x⅜-in. panel cast by the methods of Fig. 10. Only one-half of panel is shown.



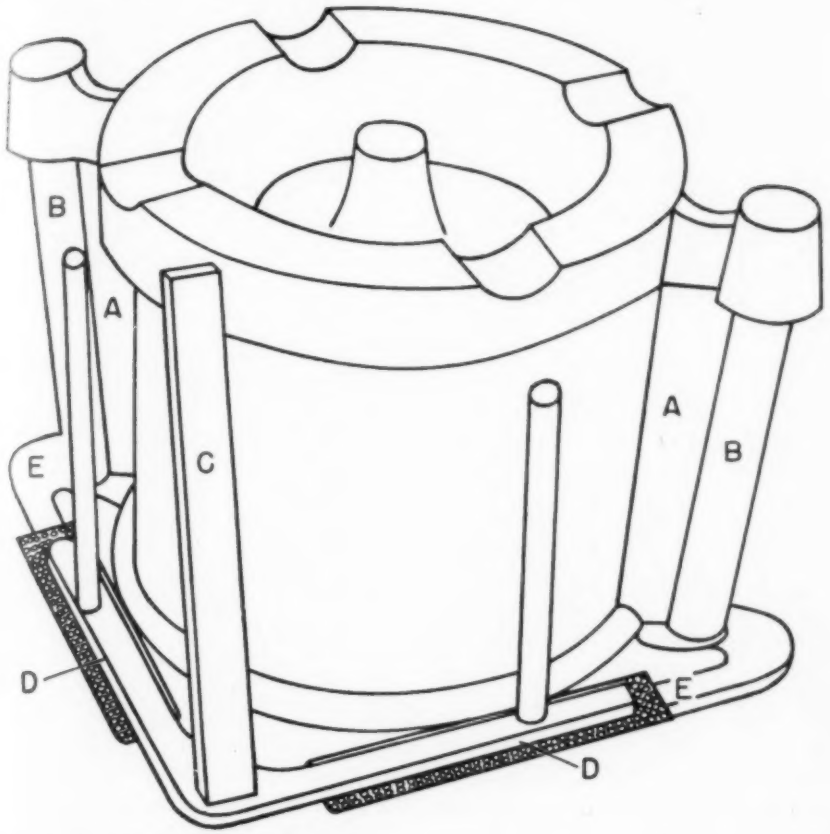


Fig. 12—Application of a conventional side gate (as described by Beck) to the casting of an aircraft wheel. Slot gate (A) runs up the side of the casting and is continuously connected with a well (B). Sprue (C) carries the metal to the lowest part of the casting, where it is screened by the skimgate (D) before continuing along runner (E) into the bottom of the well.

the casting cavity is heated to a greater extent than that adjacent to the upper parts. This also is unfavorable to feeding with top-risers.

Metal Flow Tends to Channel

If this were the only disadvantage of flowing all the metal through the lower parts of the casting cavity, its bad effects still could be overcome without great difficulty by bottom feeding with blind risers. In many cases this can be and is done, especially when relatively thin sections are involved.

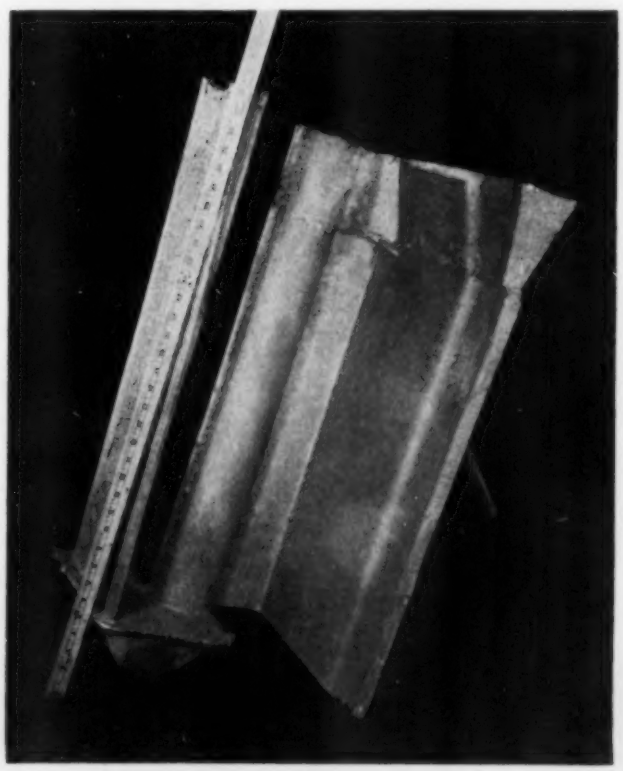
However, a still greater disadvantage of this type of mold-filling lies in the fact that the flowing metal tends to follow certain channels in filling the casting cavity. In a uniform vertical section, for example, gated as sketched in Fig. 8, the same amount of metal will not flow over each portion of the mold material intersected by the horizontal plane *A-A* and no uniform filling of the panel occurs such that the same quantity of hot metal will flow over each point *(a, b, c . . .)*. Instead, the flow tends to channel as sketched in Fig. 9.

The mold material is heated by the passage of metal over it, and naturally it is heated more in locations where channelling of flow occurs. Similar flow conditions exist in horizontal or inclined sections. In any case, the result is that some shrinkage defect is likely to appear where channelling occurred. Sometimes a streak of porosity occurs; in other cases, a shrink or draw type of defect occurs.

Multiple Gating Widely Used

Foundrymen have combatted this problem by multiple gating of magnesium-alloy castings. In any magnesium-alloy casting of appreciable size, it has long been

Fig. 13—Casting of 24x24x⅜ in. panel, of type illustrated in Figs. 10 and 11, produced by use of the side gate method represented in Fig. 12.



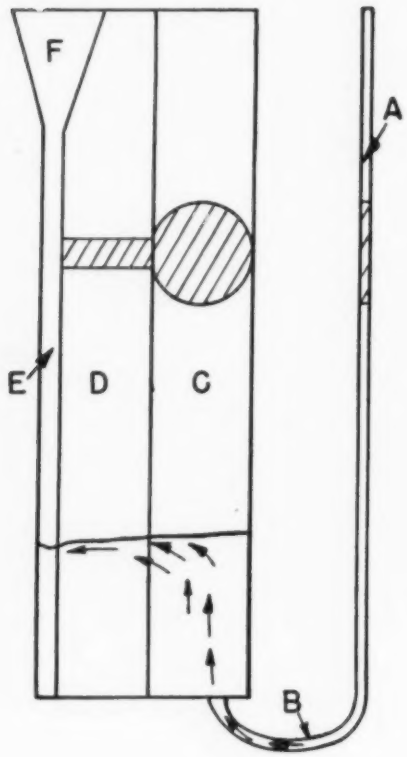


Fig. 15 (left)—Idealized flow conditions in a side gated casting. Metal rises in well, then flows horizontally into casting cavity. A—Sprue. B—Horn gate. C—Well. D—Slot gate. E—Casting cavity. F—Riser.

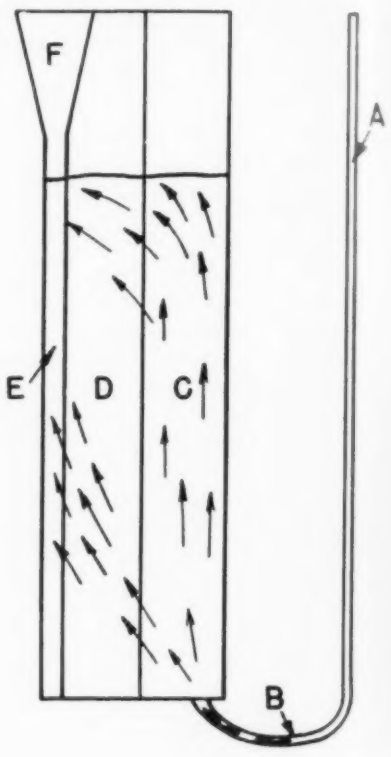
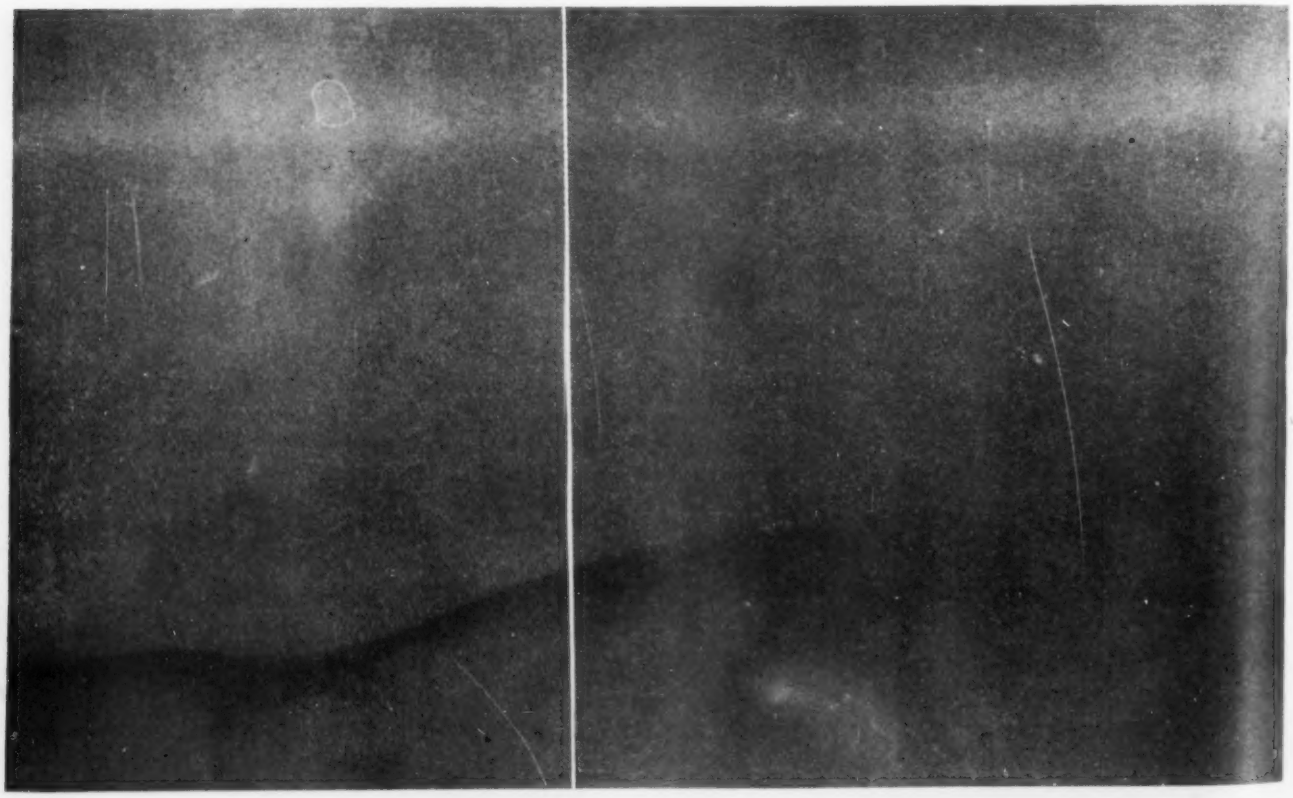


Fig. 16—Actual flow conditions in a conventional side-gated casting. Much of metal continues to flow through lower parts of casting cavity, following channels established in the early part of the pour.

recognized as good practice to gate into the casting cavity at numerous points. In this way it is attempted to prevent too large a volume of metal from flowing over any portion of the mold material. The use of a single gate into a large magnesium-alloy casting is almost never encountered.

The importance of this effect of channelling of flow may be visualized by studying Figs. 10 and 11. Figure 10 shows the method by which a 24x24x⅜ in. panel was gated and risered, employing a uniform web gate along the entire bottom edge of the panel. Despite the uni-

Fig. 14—Radiograph of a 24x24x⅜-in. panel cast by the methods of Fig. 13. One-half of panel is shown.



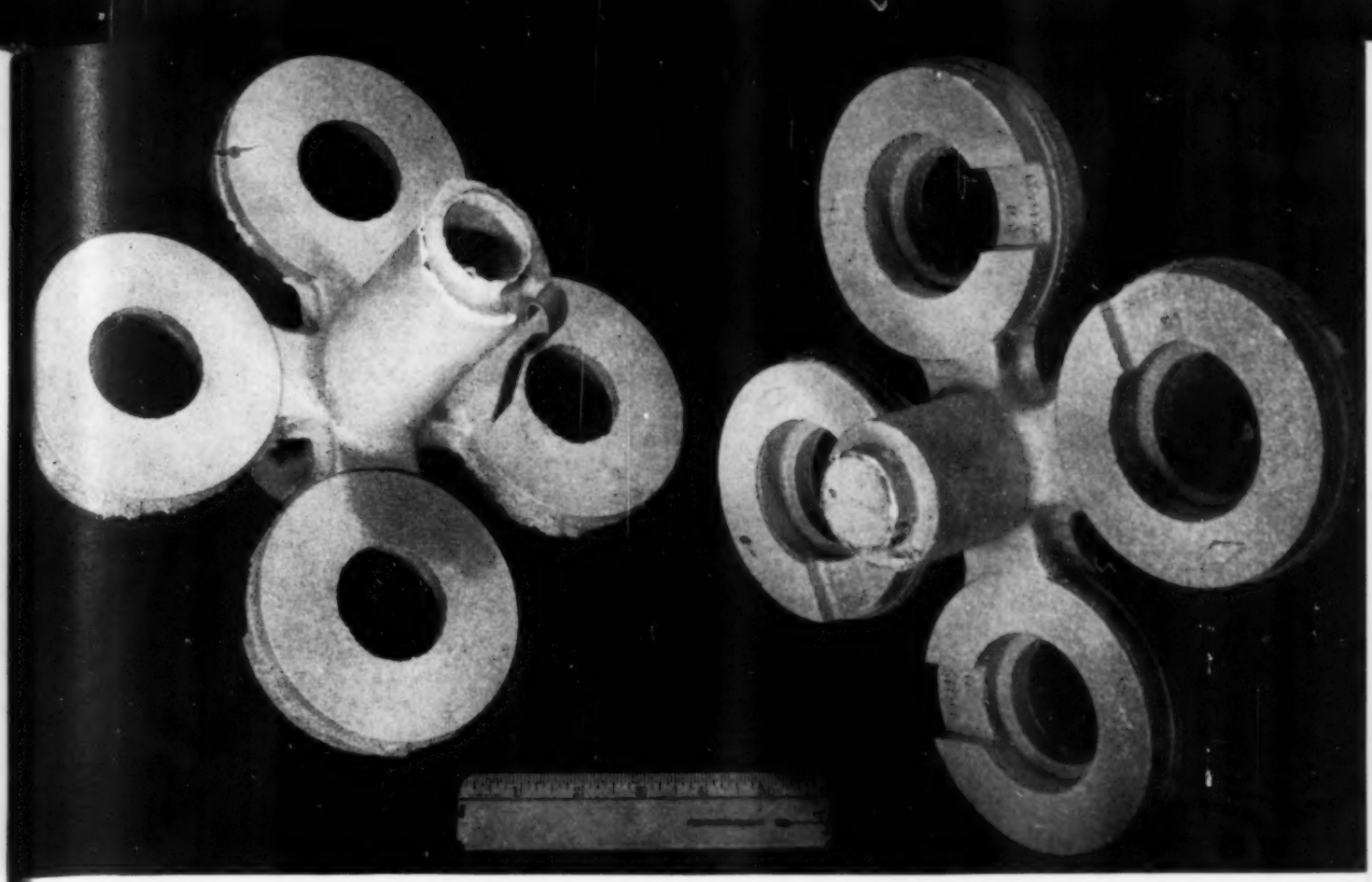


Fig. 17—Adaptation of the new gating method to a small bench casting. Satisfactory quality and increased production were obtained.

formity of design of this panel and its gate, no pouring rate could be found by which the streaks of porosity, shown in Fig. 11 could be eliminated.

The panel illustrated was of simple design, yet the ill effects of channelling of flow are evident. An even greater opportunity for seeking preferred channels of flow occurs in production castings of non-uniform section. For these reasons, better methods of filling molds were sought.

Application of Slot Gate

Beck[1] describes a type of gate which represents a significant improvement over conventional bottom gating for the casting of vertical walled parts. This may be referred to as the side gate or conventional slot gate, and it has found considerable application in the casting of such magnesium-alloy parts as aircraft wheels.

The method, sketched in Fig. 12, employs a slot gate *(A)* running up the side of the casting and also continuously connected with a well *(B)*. A sprue *(C)* carries the metal to the level of the lowest part of the casting cavity, as in bottom gating. Here it is screened with the skim-gate *(D)*. A runner *(E)* carries the metal into the bottom of the well *(B)*.

Figure 13 shows how this system was applied to the 24x24x⅜ in. panel illustrated in Figs. 10 and 11. Figure 14 indicates the resulting freedom from the streaks of porosity typical in such a panel when bottom gated. Production experience at the authors' plant has repeatedly shown that for such parts as aircraft wheels, draws and microporosity in drag parts of the casting can be eliminated much more easily with this side-gating technique than with bottom gating.

It is believed that the effectiveness of this system lies in the fact that all metal poured need not flow through the lower portions of the casting cavity. Instead, the metal tends to flow up the well, and thence to flow horizontally into the casting cavity.

Shrinkage Defects Due to Flow

However, a study of the occurrence of shrinkage defects in castings gated in this manner led to the inference that the system was only partially effective in preventing the flow of large quantities of metal through the lower portions of the casting cavity. The occurrence of draws and porosity in many castings, and the relation of such defects to the positions of gates and risers, made it appear that during the filling of molds much of the metal still had a strong tendency to enter the casting at its lower extremities, following channels established in the initial stage of pouring.

Figure 15 sketches ideal filling conditions envisioned when a slot gate is applied; Fig. 16 illustrates the type of filling which, it appears, actually occurs in many cases. It is believed that this gate, in actual application, is only a compromise between the type of flow occurring in bottom gating and the ideal type sketched in Fig. 15. Depending on the design of the casting, the ideal flow is more closely approached in some applications than in others.

The new gate described in this paper reconciles the divergent aims of securing both maximum cleanliness of the casting and ideal filling conditions for directional solidification.

It is difficult to overstate the effect that conditions of mold-filling may have on the setting-up of desirable or undesirable thermal conditions for directional solidi-

fication. Countless times in the development of a gating and risering system for a new pattern, a shrinkage defect occurs which may be attributed to the flow of too great a volume of metal through a particular portion of the casting cavity.

Before this new gate was developed, the foundryman frequently was unable to correct the basic condition causing such defect, and either had to resort to the use of chills, change the risering, or change the placement of the gates (within the limits imposed by the necessity for casting cleanliness). Many times he was forced to these costly expedients without any certainty of their success and, too frequently, such changes succeeded only in driving the defect elsewhere within the casting.

With the new gate it has been found possible readily to correct basically undesirable flow conditions in a large variety of situations. One such situation, previously mentioned, involved the casting of an experimental rolling slab. In the sand foundry, similar cases have been very numerous.

Adaptability of this new gating system indicates that its value is not limited to the casting of deep vertical-walled parts. With any top-risered casting, however shallow, if a top riser is accessible it may be filled through a gate of this type, and thus the necessity of flowing the riser metal through the casting cavity is avoided.

System Highly Flexible

Figure 17 illustrates an adaptation of this type of gate to a small bench casting. Both greater production speed and higher casting yield are realized by this method than would be attained using conventional methods. The castings are entirely satisfactory both as to cleanliness and as to radiographic quality. Comparison of Fig. 17 with Figs. 6 and 7 illustrates the great range of casting types and sizes to which this gate has been adapted.

Many variations of the basic features of the new gating system are possible, and some have already been attempted. For example, the screen need not be annular; however, the cylindrical shape does afford ample screening area, and this avoids plugging the screen with any reaction-products which may form as the metal falls through the well.

One variation of the system which has found considerable successful application occurs in the practice of first bringing the metal to the level of the lowest portion of the casting cavity (as in conventional side gating), then introducing the metal into the bottom of the annular screen through an orifice. This practice facilitates molding when it is desired to extend the slot and well up through the cope to fill a top riser directly from the gate.

Advantages Outweigh Disadvantages

Disadvantages of this gating technique can be weighed against its advantages. First, the slot gate is not so easily removed from the casting as are conventional bottom gates. Secondly, there is a tendency toward centerline porosity at the junction of the slot and the casting wall. These two disadvantages are also shared by the conventional slot gate. In addition, remelting of the gate scrap involves the somewhat cumbersome operation of removing the annular screens from the melt, when the new gating system is used. In the opinion of the authors, none of these disadvantages seriously prejudices the use of this gate, in view of its many economies.

Summary

A new type of gate has been developed by which clean, sound, magnesium-alloy castings of many designs can be made more economically than by previously published methods. The chief value of this gating system lies in the fact that its use corrects basically undesirable filling conditions which are difficult to avoid with other systems. By virtue of the improved mold-filling conditions attained, the problem of setting up proper conditions for controlled directional solidification is much simplified. The method has been adapted to a great variety of cast shapes.

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DISCUSSION

Chairman: R. T. WOOD, American Magnesium Corp., Cleveland

Co-Chairman: HIRAM BROWN, Solar Aircraft Corp., Des Moines, Iowa

DR. L. W. EASTWOOD[1] *(written discussion):* This paper is a welcome addition to the technology of casting because there is far too little work done on methods which will produce the proper temperature gradients in the mold cavity.

Melts tend to flow in straight lines unless there is an obstacle which will cause a deflection of the melt stream. The use of the steel wool in the authors' system of gating helps to retard the downward flow of the melt, thereby assisting a lateral movement of the melt into the mold cavity at the melt level. Their screen effect is incidental to this and, if necessary, could be done more simply.

There is another way of obtaining the proper flow of metal by means of a properly designed "side gate." The design of the side gate, as shown by the authors' Fig. 2, 12, 13, and 16, misses an important point which is illustrated in Beck's *Technology of Magnesium,* page 331, Fig. 369D. The side gates illustrated by the authors would not be expected to be too successful because they do not reverse the direction of the flow of the melt in the gate. It is entirely possible with their design of the side gates for the metal to flow more or less horizontally from the down sprue through the gate into the mold cavity. An examination of the figures illustrated by Beck, referred to above, shows that the metal is completely reversed in direction in the gate so that it is traveling vertically when it enters the side reservoir. In contrast to this, the gate shown by the authors' Fig. 16 enters the well at an angle, and, obviously directs metal into the bottom of the mold cavity as the authors have indicated. If, however, Gate B had been properly designed, the melt would have entered the wall, C, vertically, or possibly even with a slight back slant tending to place the metal in the well at the side opposite from the slot gate connecting it with the casting. If screen systems are required, they can be very readily incorporated in the gate at B and it is especially desirable to have the metal flow through the screen from below and not from above as shown by one of the authors' figures. Had this been done, it is quite likely that the side gate, prop-

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erly designed, would have produced as good results as the authors' new gating system.

MR. ELLIOTT: Dr. Eastwood has indicated that it is easy to avoid the flow of large quantities of metal through the base of the casting cavity, by adjusting the direction imparted to the metal stream as it enters the base of the well. This is contrary to our experience with this type of gate. We have tried horn-gating into the well and other means of producing a vertical flow of the metal at this point in the gating, in the attempt to improve mold-filling conditions. It is found, however, that there are more significant factors affecting the mode of filling of the mold. The first metal to enter the casting must necessarily enter at the base of the casting cavity. This first metal heats up a path through the mold material, and during later stages of filling, the metal tends to follow these heated channels which were established in the early part of the pour. We were unable to find a way of introducing the metal into the base of the well which was as consistently successful in preventing channeling effects as the use of cylindrical skim-gate here described.

R. E. WARD[2]: There would have to be diligent use of the ratio of the diameter and the length of the cylindrical screen to prevent spraying at the top of the screen by too rapid introduction of the metal. I wonder if this ratio has been worked out and whether it has been found that you can pour too fast and have metal spray from the top of the screen before the casting cavity was filled?

MR. ELLIOTT: Under some conditions we have had that happen. If the flow rate is too great in relation to the diameter of the screen, there is a tendency for the screen to fill up and for the metal to run down the outside of the screen. In running down the outside of the screen it oxidizes and the oxide is washed into the casting. When pouring into the top of the well, one of the things that has to be avoided is pouring at too great flow-rate down a single one of these wells. One of the things that we do to alleviate that defect is to increase the diameter of the screen.

MR. MEZOFF: In one of the illustrations there were some fuzzy lines down the center of the screen. These lines indicate the presence of stringers of steel wool along the axes of the screens. The steel wool acts more or less like a stirring rod as it is used in the laboratory to pour a liquid from one beaker to another. It helps to keep the metal following a course down the center of the screen rather than trying to wet the screen and flow down the screen itself.

Another observation you may have made from the illustration was in the case of the large 110-in. wheel. If you look closely you will see there were actually two screens concentric with each other. That application, as has been mentioned, was one of the very early applications. It was at a time when we were having trouble adjusting the ratios Mr. Ward mentioned. We found that by using a double screen method, we could get away from any kind of premature flow through the outer screen. It was after that, that we started using the steel wool, so that both of those tools are available also in the application of this gating.

M. E. BROOKS[3]: This method of gating was developed in our foundry about a year ago. We had considerable trouble trying to determine the proper ratios of gate size to casting wall thicknes and various other difficulties in the early stages of developing the gate. As soon as we were sure that the gate would give satisfactory results the necessary information for its use was passed on to other foundries.

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[3] Dow Chemical Co., Midland, Mich.

GRAPHITIZATION OF WHITE CAST IRON

EFFECT OF SECTION SIZE AND ANNEALING TEMPERATURE

By

Richard Schneidewind,[1] D. J. Reese,[2] and A. Tang [3]

ABSTRACT

A study of time necessary for first-stage annealing of sections of white iron step bars indicated a great effect due to cooling rate of the casting during solidification. Cooling rate was considered to be proportional to the ratio of volume to surface area of the casting. It was found that the logarithm of this ratio is directly proportional to the logarithm of annealing time and to the grain size of the metal.

SINCE RATE OF GRAPHITIZATION of white cast iron is influenced by many factors, it is difficult to measure the precise effect of any one variable, such as cooling rate, for example, unless all other factors and foundry conditions are maintained constant throughout the series of tests. Unless the influence of all the other variables is clearly understood, it is unwise to draw unjustifiably wide conclusions from such a study.

A partial list of the variables which affect rate of graphitization would include the following items.

1. Basic composition: carbon, silicon, and iron.
2. Other elements: manganese, sulphur, phosphorus, chromium, hydrogen, etc.
3. The rate of cooling of the casting in the mold. This is controlled principally by section size, pouring temperature, and mold material.
4. Raw materials. This involves other elements mentioned under 2 and also hereditary effects of possible combinations of elements, their distribution and graphite size in the pig. In addition, gases resulting from moisture in the blast and melting furnace atmosphere and time held in the furnace all have their effects.
5. Superheat in the melting operation.
6. The temperature of soaking in the first stage of annealing.
7. The rate of heating to the soaking temperature.
8. The atmosphere in the annealing oven.

It is obviously necessary, if any one of these factors is to be studied, that all the other variables must be maintained constant.

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[3] University of Michigan

This paper will confine itself to factors 3 and 6 above, the influence of section size and soaking temperature in the first stage of annealing. The specimens studied were all made at the Jamestown Malleable Iron Co., Jamestown, N.Y. and were poured within a period of about ten minutes so that it may be safely assumed that items 1, 2, 4, and 5 are as constant as commercially possible. The foundry data are as follows for a period of 45 min.:

Superheat temperature:	2920 F (Pyro Optical Pyrometer)	
	2960 F (L and N Optical Pyrometer)	
Pouring temperature:	2740 to 2810 F (Pyro Optical Pyrometer)	
Molds:	Green Sand	
Chemical Composition:	Carbon	2.30 percent
	Silicon	1.38 percent
	Manganese	0.46 percent
	Sulphur	0.136 percent

The specimens reported upon in this paper represent but a fraction of the specimens covered by the above foundry data so that the pouring temperature range is probably only between 2790 and 2810 F.

The specimens poured consisted of step bars and tensile test bars. The step bar was composed of five sections with a thickness of 1.5, 0.75, 0.5, 0.25 and 0.125 in. respectively. Figure 1 shows a sketch of this piece. The tensile bars were standard as to gating but were provided with pads to give a uniform cross section of 0.75 in. diameter.

Annealing was conducted in a laboratory electric resistance furnace. Specimens were placed upon the hearth of the furnace which was at the required temperature and were covered with charcoal to minimize decarburization. Although this procedure does not simulate commercial conditions, it does, however, maintain constant items 7 and 8 above which deal with rate of heating and furnace atmosphere. The specimens were at heat in 15 min. which is equivalent to a heating rate of 99 and 109 F per min.

It has been shown by Forbes,[1] Schwartz [2, 3, 4], Boegehold [5] and others that faster cooling rates as the iron solidifies result in an increase in the rate of carbide decomposition upon annealing. The reason for such behavior must be reconciled with the mechanism of graphitization which has been clearly stated by Schwartz [6] as embracing the following steps:

a. "Solution of cementite
b. Decomposition of cementite
c. Migration of carbon
d. Crystallization of carbon

The reaction as a whole will have its rate determined by that of its slowest step. Solution and crystallization are steps dependent upon area of interface, therefore, in the very beginning of the reaction, when the surface of graphite is infinitely small or absent, that reaction must be the slowest and must govern the process."

Effect of Faster Cooling Rates

Faster cooling rates may increase the rate of malleabilization at a given soaking temperature in one or both of two ways. First, it will produce a finer casting grain size and hence a finer eutectic cementite network. The increased austenite-cementite interface will increase the rate of cementite solution (Schwartz's factor a. above) since rate of solution is proportional to the surface of solute exposed to the solvent. Secondly, a faster cooling rate increases the under cooling of austenite at the lower critical and the austenite at its transformation will contain more than the theoretical eutectoid concentration of carbon. Upon reheating above the critical there may either be a tendency to form a supersaturated austenite or excess carbides may be precipitated in fine dispersion which will offer a large amount of surface area to the austenite.

The latter effect has been carefully described by Schwartz,[3, 4] Saito and Sawamura,[7] Hultgren and Edstrom [8] for the case where white irons were given a preliminary heating above the critical temperature and were quenched prior to annealing. Simmons [9] quenched a heated bar on one end as a Jominy bar and spectacularly demonstrated that faster rates of quenching produced finer graphite nodules.

In all these experiments the effect of carbide network was kept constant and only the nature of the matrix produced the effect noted. Prequenching is rarely a practical treatment but the effect of cooling rate such as determined by the cross section of the casting is of great interest to the foundryman.

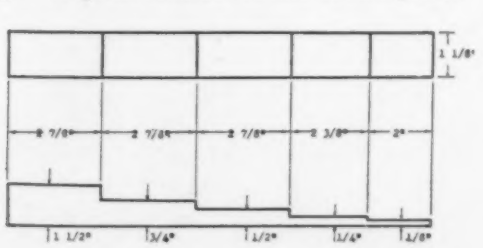


Fig. 1 (below)—Sketch shows step bar with dimensions.

Fig. 2 (right)—Curves showing effect of section and temperature on first-stage annealing time.

Outline of Test Procedures

Pieces were cut with a rubber wheel, plentifully supplied with coolant to prevent overheating, from each section of the step bar and from the test bar. Specimens were placed in an electric furnace at 1700 F and were covered with charcoal. It was noted that after about 15 min. the temperature controller indicated that the furnace and its contents were at heat. Pieces were removed from time to time cooled and were polished and examined for free carbides.

The same procedure was later repeated for a soaking temperature of 1550 F. In each case further samples were treated in order to determine more closely the time necessary to cause disappearance of carbides.

Results of Tests

The results of the tests are presented in Table I. Many samples were treated but only the last sample containing carbides and the first one free from carbides of each series is reported. In judging the specimens, the outside layer of metal was ignored. Although the surface of many of the specimens was a cut rather than a cast surface, there seemed to be a condition of nonuniformity extending inward for about $\frac{1}{32}$ in. which is probably due to atmosphere in the furnace. This phenomenon is more easily seen in thin sections than in heavier ones.

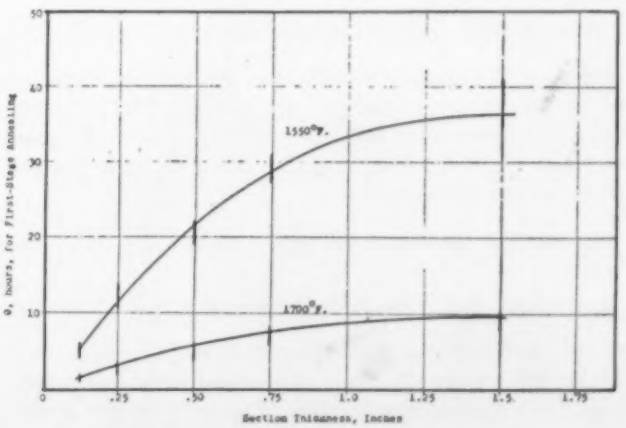
Metallographic examination in the unetched condition also confirmed the previous observations of many workers in the field that fast graphitization and numerous temper carbon centers go hand in hand.

These data are also presented graphically in Fig. 2 where section thickness and time for carbide decomposition are plotted. The test bar of course does not fit this simple curve and is therefore omitted.

Correlation of the Results

It is apparent qualitatively that faster rates of cooling produces faster annealing rates. In the foundry field many phenomena are proportional to cooling rate and a large amount of work has been done to correlate cooling rates during solidification with mechanical properties. Bolton [10] and others have pointed out that the ratio of volume to surface is at least roughly proportional to cooling rate.

If two castings are to be poured out of similar metal from the same pouring temperature, it may be assumed



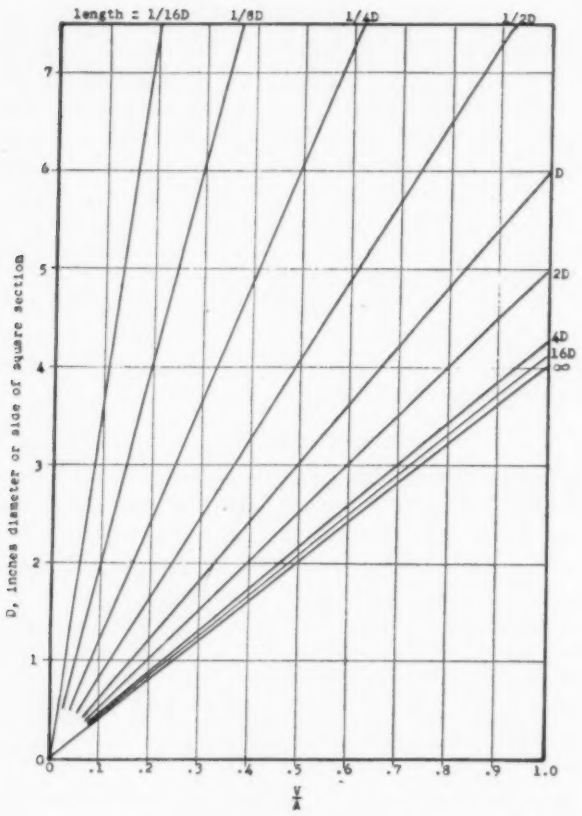


Fig. 3—Curves showing computation of V/A for simple shapes.

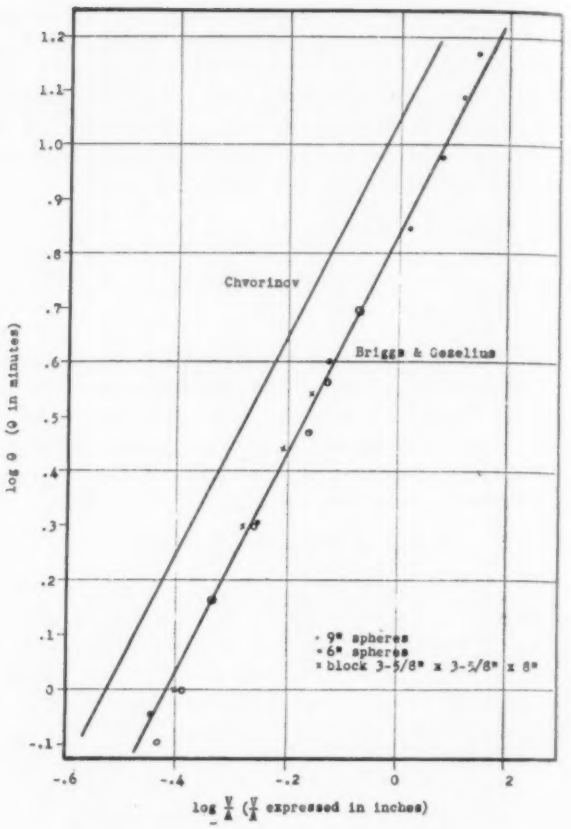


Fig. 4—Data of Briggs and Gezelius plotted according to Chvorinov Method.

that the sensible heat, the specific heat, and the latent heat of solidification are the same. In other words, the number of heat units to be removed by the mold will be directly proportional to the volume and weight of the casting.

If poured into molds made of the same material then the rate of heat transfer to the molds is constant and the amount of heat absorbed by the mold in a given time is directly proportional to the area of the mold. Simply, it may be said that the ratio $\frac{V}{A}$ is a measure of the number of Btu of heat dissipated to each square inch of mold cavity surface; if the molds are of the same material, $\frac{V}{A}$ is proportional to cooling rate.

This ratio of $\frac{V}{A}$ is here computed where V equals the volume of the casting in cu. in. and A is the cooling area of the casting expressed in sq. inches. The area of gates and risers must be subtracted from the total surface since cooling does not take place at these points prior to solidification.

In order to facilitate the computation of $\frac{V}{A}$ for simple sections, Fig. 3 has been drawn with the ratio $\frac{V}{A}$ as the abscissa and D as the ordinate where D, expressed in inches equals the diameter of a round cross section or a side of a square cross section. The cooling rate at the center of a round bar of 2 in. diameter is the same as that in the center of a bar 2 in. square by this computation.

Radiating from the origin are lines indicating the relative lengths of the bars. The line at the extreme right is for bars of infinite length. For a bar 2 in. in diameter $\frac{V}{A}$ may be read as 0.5. If the bar is 16 in. long it falls on the line labeled 16d and for 2-in. diameter, $\frac{V}{A} = 0.495$. If a 2-in. cube or a 2-in. diameter casting, 2-in. high is considered, this will be found on the line corresponding to a length d and $\frac{V}{A} = 0.335$. Finally, if the line on the left side is used, labeled $\frac{1}{16}$d, for a 2-in. diameter disc, $\frac{1}{8}$-in. thick, $\frac{V}{A}$ may be read 0.06. Gates and risers will reduce the value for A but usually will not change $\frac{V}{A}$ appreciably.

The next problem is to find a correlation between $\frac{V}{A}$ and casting properties.

Briggs [11] presents a large amount of work done by him and his collaborators, as well as by others on the rate of solidification of steel castings of various sizes and shapes. A number of equations are given which attempt to correlate section thickness, D in inches, with time for solidification, θ in minutes

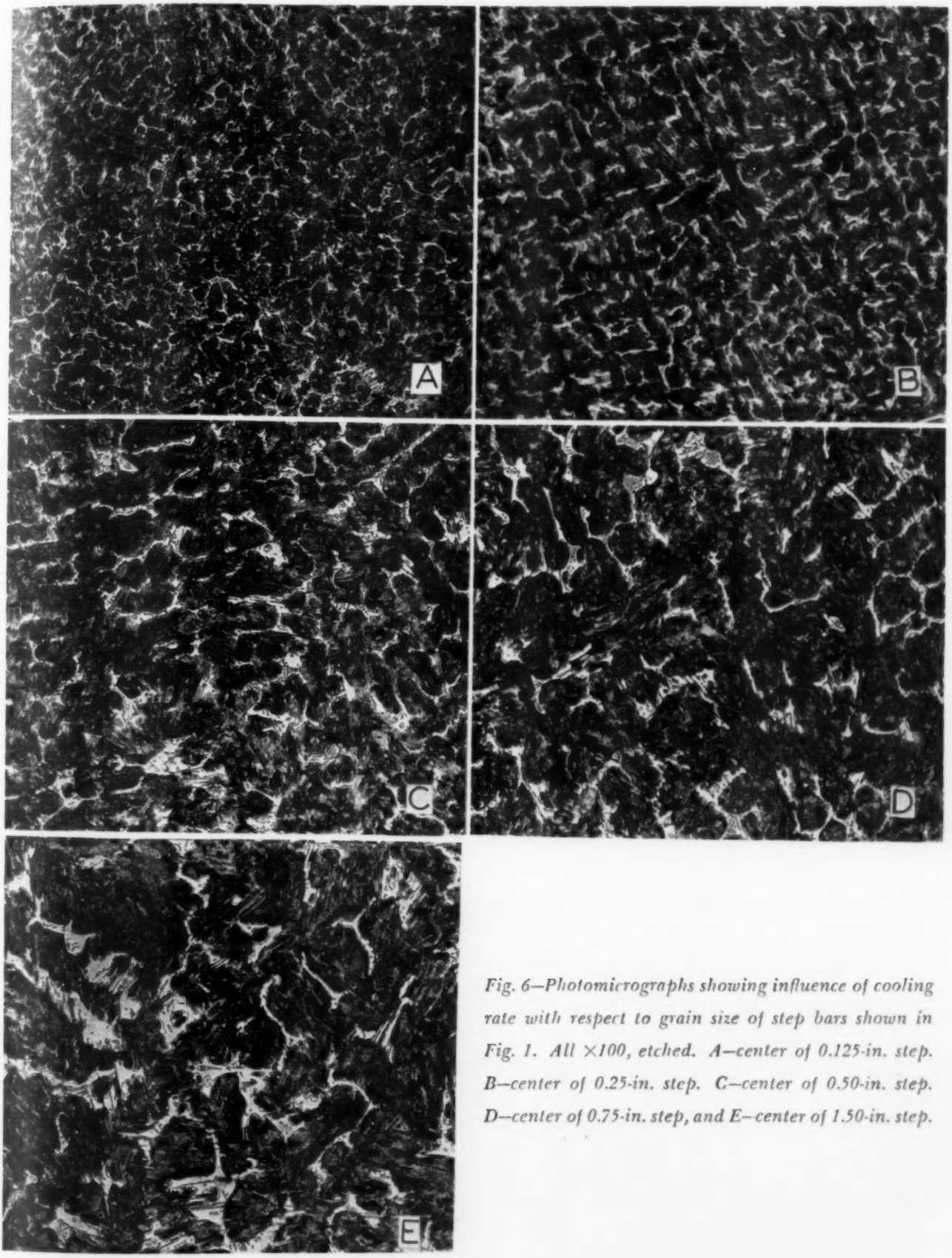


Fig. 6—Photomicrographs showing influence of cooling rate with respect to grain size of step bars shown in Fig. 1. All ×100, etched. A—center of 0.125-in. step. B—center of 0.25-in. step. C—center of 0.50-in. step. D—center of 0.75-in. step, and E—center of 1.50-in. step.

TABLE I.—INFLUENCE OF SECTION SIZE AND SOAKING TEMPERATURE ON TIME FOR FIRST-STAGE ANNEALING

Section thickness, in.	Time to decompose the carbides, hr.	
	1700F	1550F
0.125	>1 < 2	> 4 < 6
0.25	>2 < 3 (nearer 3)	>11 <14 (nearer 11)
0.50	>4 < 6	>19 <21½
0.75	>6 < 8	>27½<31
1.50	>8 <10 (nearer 10)	>35 <41
0.75*	>5 < 6 (nearer 6)	.....

* Tensile test bar.

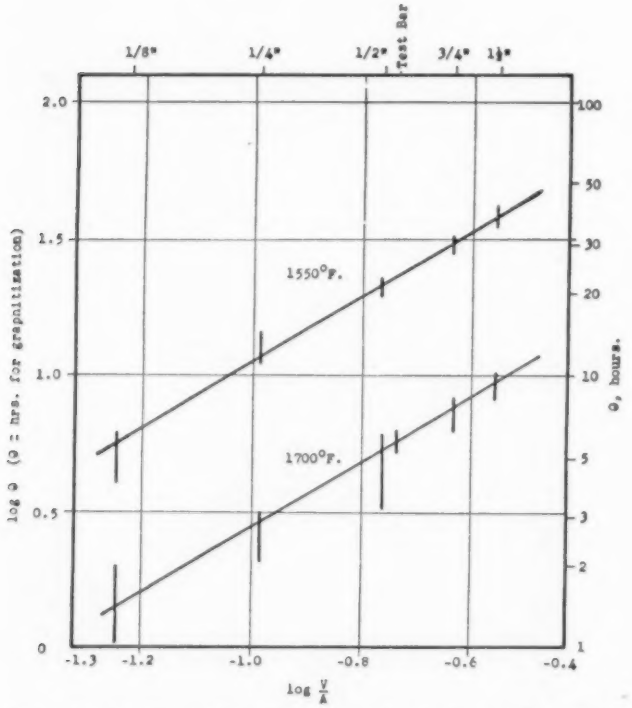


Fig. 5—Relationship between cooling rate of the casting and the time and temperature of first stage annealing.

Chvorinov [12] proposes the equation

$$\theta = \frac{R^2}{K^2}$$

where

θ = time for solidification of steel castings, expressed in minutes

R = the ratio $\frac{V}{A}$ expressed in millimeters

K = a constant

His results plotted on log-log paper with θ as ordinate and $\frac{V}{A}$ as abscissa give a straight line for castings varying in shape and also in "size from a plate 10 mm. thick up to massive castings of 65 tons." [11] After converting from millimeters to inches the equation for this line may be written

$$\log \theta = 2 \log \frac{V}{A} + 1.05$$

In order to check these findings, which show that the factor $\log \frac{V}{A}$ correlates with solidification time, the authors recomputed Briggs' [11] data on the solidification of 6 in. and 9 in. spheres and of a casting 3⅝ x 3⅝ x 8 in. These spheres were poured and at definite intervals the molds were bled and the wall thickness measured. From the values given in his Figures 60 and 61, the volume and areas of the castings were computed and also $\frac{V}{A}$. The computations are shown in Table II and graphically in Fig. 4. When $\log \theta$ is plotted against $\log \frac{V}{A}$ a straight line results whose equation is $\log \theta = 2 \log \frac{V}{A} + 0.83$. This line is obviously parallel to that of Chvorinov and slightly displaced indicating different mold material or conditions. Nevertheless, the results of these investigators does indicate

TABLE 2.—DATA OF BRIGGS & GEZELIUS ON SOLIDIFICATION
Recalculated by Chvorinov's Method

Dimension	θ Minutes	Skin thickness inch	Volume of solid cu. in.	Area of sphere sq. in.	$\frac{V}{A}$	$\frac{V}{A}$ corrected*	$\log \frac{V}{A}$	$\log \theta$
	0.9	0.4	94	254	0.370	0.364	−0.440	−0.046
	2.0	0.65	143	254	0.564	0.552	−0.258	+0.301
	4.0	1.0	204	254	0.804	0.786	−0.127	+0.602
9 in. diam.	7.0	1.55	275	254	1.082	1.054	+0.023	+0.846
	9.5	2.0	316	254	1.242	1.206	+0.082	+0.978
	12.2	2.5	348	254	1.370	1.320	+0.121	+1.087
	14.8	3.0	367	254	1.442	1.399	+0.142	+1.170
	0.8	0.45	43.5	113	0.385	0.370	−0.432	−0.097
	1.0	0.5	48.0	113	0.425	0.409	−0.389	0.0
	1.47	0.6	55.3	113	0.489	0.470	−0.328	+0.168
6 in. diam.	2.0	0.73	64.5	113	0.570	0.550	−0.260	+0.301
	2.97	1.0	79.6	113	0.705	0.685	−0.165	+0.473
	3.7	1.2	89.9	113	0.795	0.742	−0.130	+0.568
	5.0	1.5	99.0	113	0.876	0.850	−0.071	+0.699
	1	0.5	56.6	142	0.399	0.395	−0.405	0.0
Casting	2	0.75	75.4	142	0.530	0.523	−0.283	+0.301
3⅝ x 3⅝ x 8 in.	2.78	1.0	88.9	142	0.616	0.613	−0.212	+0.444
	3.5	1.2	102.0	142	0.719	0.702	−0.154	+0.544

* Assumption—sprue on spheres are 2 in. diam. sprue on block is 2 in. square.

that $\log \frac{V}{A}$ has significance in foundry work.

An attempt was next made to determine whether the value, $\log \frac{V}{A}$, would correlate with the results on first-stage annealing of this investigation. The volumes and surface areas of the tensile test bar and of each section of the step bar were computed.

Corrections for gating were subtracted from the surface areas. In the step bar a gate 1/8 in. by 1 in. entered where the 1/8 in. and 1/4 in. sections joined. One-sixteenth of a square inch was therefore subtracted from the surface of each section. The heaviest section was provided with a gate 1 in. x 1/4 in. and a similar correction was made. Each tensile bar was fed by two 1 in. x 1/4 in. gates. Table III presents these data.

These data were plotted in Fig. 5 with the range of annealing time from Table I versus the ratio of $\frac{V}{A}$ on logarithmic coordinates. Straight lines are the only ones which will connect the data points. The equations for the lines are as follows:

$$\text{at 1700 F} \qquad \log \theta = 1.2 \log \frac{V}{A} + 1.633$$

$$\text{at 1550 F} \qquad \log \theta = 1.2 \log \frac{V}{A} + 2.25$$

where

θ = time in hours
V = volume in cubic inches
A = cooling area of casting in square inches

The time necessary to anneal all the specimens have been compiled from the graph of Fig. 5 and have been added to Table III to show the good correlation.

It would seem, therefore, that the cooling rate as measured by the ratio of volume to area of the casting stands in a similar logarithmic relationship to first stage annealing as it does to rate of solidification of steel castings as shown by the data of both Briggs and of Chvorinov.

The two lines in the graph represent the behavior of the irons when annealed at 1700 and 1550 F respectively. One of the present writers [13] has previously shown the relationship between temperature of first stage annealing and time necessary for decomposition of cementite. This earlier work was done at a constant $\frac{V}{A}$ since all specimens were cut from tensile bars. Comparison with these results shows that the difference in annealing times at the two temperatures found for the step bars is exactly predictable from the previous tests.

Confirmation of this relationship was sought in the data presented by Boegehold.[5] On p. 451 of his article he presents drawings of a small and a large casting. The dimensions were taken from data and by scaling the drawings. His section C is a ring 5.75 in. outside diameter with 13/16 in. wall thickness and 3/4 in. long cut from the end of a brake flange hub weighing about 50 lb. It is assumed that this ring section connected to a riser through an area of 2 in. x 13/16 in.; the small casting is assumed to have a gate 1/4 in. x 1/8 in. Naturally, since the iron and the molding sand are different than those of the step bars in this investigation, a different constant appears in the equation

$$\log \theta = 1.2 \log \frac{V}{A} + C$$

than given before. The data are presented in Table IV. Using this formula and assuming that the annealing times for the small casting, A, are correct, the annealing times for section C were computed and are shown in the table.

The influence of cooling rate was also studied with respect to the grain size. Photomicrographs in Fig. 6, at 100 diameters were prepared from the center of each part of the step bar. Readings were made on each picture along the diagonals, across through the center, and vertically through the center to count the number of crystals per inch. Since the photomicrographs are at 100 diameters, these numbers are actually per 1/100

TABLE 3.—ANNEALING TIME AND VOLUME-TO-SURFACE RELATIONSHIPS FOR STEP AND TENSILE BARS

Section thickness, in.	Volume, cu. in.	Area sq. in.	$\frac{V}{A}$	Time to decompose carbides, hr. at 1700 F found	at 1700 F computed	at 1550 F found	at 1550 F computed
0.125	0.312	5.594	0.056	>1 < 2	1.34	> 4 < 6	5.55
0.25	0.782	7.594	0.103	>2 < 3	2.89	>11 <14	11.9
0.50	1.875	10.810	0.173	>4 < 6	5.23	>19 <21½	21.6
0.75	2.810	12.012	0.234	>6 < 8	7.53	>27½ <31	31.2
1.50	4.690	17.00	0.276	>8 <10	9.16	>35 <41	38.0
0.75*	3.320	18.06	0.184	>5 < 6	5.60	———	23.2

* Tensile bar.

TABLE 4.—COMPUTED ANNEALING TIME FOR MALLEABLE IRONS

Rate of Heating	Specimen	Volume, cu. in.	Area, sq. in.	$\frac{V}{A}$	$\log \frac{V}{A}$	Time for Annealing, hr. found	computed
Rapid	A	0.1284	1.97	.0653	−1.186	4	
	C	8.0	32.1	.249	− .604	25	19.9
Slow	A	0.1284	1.97	.0653	−1.186	1	
	C	8.0	32.1	.249	− .604	5	4.96

TABLE 5.—NUMBER OF GRAINS LINEAL INCH AT 100 DIAMETERS

Section thickness in.	$\log \frac{V}{A}$	at 100X
0.125	−1.254	5.37
0.25	−0.987	4.52
0.50	−0.762	3.06
0.75	−0.632	2.44
1.50	−0.560	2.02

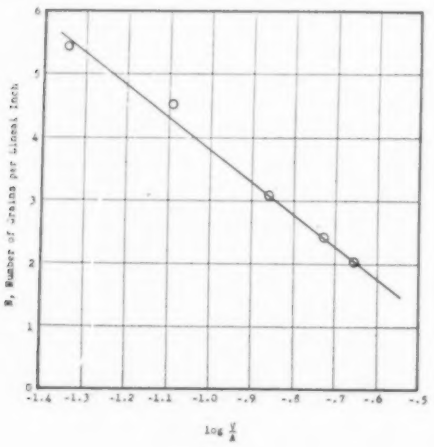


Fig. 7—Relationship between grain size and cooling rate.

in. The average number of crystals per inch, N, as photographed, were compared with the cooling rates as measured by $\log \frac{V}{A}$ and are given in Table V. The grain size, as the term is generally used, is expressed as $\frac{1}{N}$. Greater accuracy in measurement is possible when the grains are small so that the most weight should be given to the values obtained in the smaller sections. These data are presented graphically in Figure 7 which shows good correlation between cooling rate and number of crystals per inch formed during solidification.

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DISCUSSION

Chairman: V. A. CROSBY, Climax Molybdenum Co., Detroit.

Co-Chairman: M. TILLEY, National Malleable & Steel Castings Co., Cleveland.

J. E. REHDER[1] *(written discussion):* This paper is a valuable contribution to the metallurgy of malleable iron, and the authors are to be congratulated. It would now appear to be possible to compare work performed by investigators on test pieces of different shapes and sizes. The conclusions arrived at by the authors are so important that it is hoped that they and others will extend the work to put it on the soundest possible basis. Repeating of the work, using separate test pieces of various sizes to eliminate possible uncertainties due to the end effects of the sections of a stepped bar, would be of value.

In Fig. 7 is shown a relationship between size of the white iron casting and cooling rate. It would be of interest to know the nodule count per square millimeter of each of the annealed samples, if such were determined, in order to see if the relationship follows through as might be expected. In Table 3, values for the volumes of the various sections do not seem self-consistent. It would be appreciated if the details of calculation of the volumes of the sections could be given.

In the calculation of annealing times from Boeghold's data, it is noted that the value of "C" in the equation

$$\log \Theta = 1.2 \log \frac{V}{A} + C$$

is 2.025 for "rapid" heating and 1.423 for "slow" heating. The effect of decreasing heating rate to soaking temperature would then appear to be similar to the effect of higher soaking temperature.

Any elaboration of this subject by the authors would be appreciated.

DR. SCHNEIDEWIND: We started making nodule counts but the difficulties are very great. It would require many samples and statistical methods to report the results.

There is a relationship, using everything else constant, between the grain size and nodule count, but the nodule count can be altered by the rate of heating. It can be altered by many other things.

If the results of chemical analyses for combined and graphitic carbon are correlated to show the rate of decomposition under a certain set of conditions, this rate may be expressed by the expression

$$\log \frac{\text{Total decomposable carbide}}{\text{Carbide remaining}} = (K)\ (\text{Time})$$

If only temperature of annealing is altered, the change in the constant, K, may be determined by the expression

$$\log K = \frac{-\Delta Ha}{R\,T} + b$$

where ΔHa = heat of activation
R = gas constant
T = absolute temperature
b = a constant

If irons of other silicon contents, superheat temperatures, carbon contents, or other conditions of composition or method of manufacture are used, the behavior during isothermal annealing at different temperatures will follow the above equation but the constant, b, changes. Insufficient data are available to express the relationship quantitatively between annealing rate and all these variables.

G. M. GUILER:[2] We have been making combined carbon de-

[1] Bureau of Mines, Ottawa, Canada.
[2] National Malleable & Steel Castings Co., Cleveland.

terminations in graphitized material in the last year or two by the method worked out at Battelle where it is determined directly.

Some years ago we were led to believe that commercial malleable iron combined carbon as low as 0.05 or 0.10 per cent and we determined it then by the uncertain method of color carbon comparison. In determining these combined carbons by this Battelle method in high grade malleable iron we found that the combined carbon runs from say 0.12 to 0.30 per cent in extreme cases. My co-worker Reidar Eriksen, has examined some of these under the microscope and we made the rather astounding discovery that the combined carbon gets to 0.17 or 0.18 per cent before you observe anything under the microscope. The men who worked out this method at Battelle claim that graphite is not soluble to any appreciable extent in this acid treatment.

MR. REHDER: I have a photomicrograph of it. In looking at bars that are undergoing first stage graphitization, it is almost impossible to avoid noticing that you can see undecomposed primary carbide protruding into graphite nodules.

Furthermore, if you get the section just right, it is possible to find sections where the graphite nodule has grown almost around a piece of cementite. Apparently once it gets surrounded or partially surrounded with graphite, the cementite does not decompose further. In other words, some of the annealed bars do contain small particles of carbide. Those particles of carbide are not in solid solution in the iron and would indicate too high a figure for solid solubility. They would not affect machinability because they apparently are loose in the nodule. For the same reason I do not think they would affect tensile properties. They certainly would show up as combined carbon in an analysis, and the amounts quoted by Mr. Guiler seem to be about the same as under the microscope.

REIDAR ERIKSEN:[2] I agree with Mr. Rehder that primary carbide particles are found imbedded in graphite nodules now and then, but not often enough and not to such an extent as to warrant the high combined carbon found right along by Mr. Guiler in his chemical analysis. I thought that perhaps the carbides were submicroscopic in size and fairly evenly distributed in the ferrite matrix.

MEMBER: My experience in annealing malleable iron now has covered a period in excess of 45 years and so little was known of the actual reactions in the early period of my experience that it is rather marvelous to see what is placed before us under present day conditions, but I was very glad to see the question of grain sizes brought out in the paper by Dr. Schneidewind, which I am sorry I have not had time to read.

In the paper* presented at the A.F.A. convention in Columbus, Ohio, in 1920 by Highriter, Davenport and Bean, the grain size effect upon graphitization was touched upon without, however, a real explanation of the why and wherefore of the difference in grain size. But in that paper micrographs of the tip and base of a standard malleable iron wedge shows the vast difference in grain size after graphitization after annealing of those two sections.

Now of course, those were sections of the same casting. One section may have been influenced more by gating. The standard malleable wedge is gated on the butt end, the ½-in. section. When the data for this paper was compiled, the theory advanced by the old time annealer was that it was necessary to place heavy-section castings in the hotter portion of the ovens because it took the heat longer to penetrate a heavy casting than it does a light casting. Actually, the conductivity of iron is much higher than the conductivity of the packing material, which in those days surrounded all castings, because there was nothing then annealed that was not placed in packing of one kind or another. But actually grain size, it seems to me, is the determining factor, grain size being largely determined by the volume area.

By that formula, if you have a thin section casting it will be of small grain size, and if you have a heavy section casting it will be of large grain size, both in the white iron state and in the annealed state.

DR. SCHNEIDEWIND: When computing the ratio of volume to surface area, corrections were made for the gates. It is obvious that the value of the ratio lies in the fact that the volume is proportional to the number of Btu's which must be removed across a given area. Certainly heat will not be lost through the gate itself so that the cross sectional area of the gate must be subtracted.

As for grain size, its effect may be due to one of the following reasons or a combination of both. First, as Dr. Schwartz has stated, the area of contact between cementite and austenite is important. A coarse grain will have less carbide to austenite interface.

Secondly, a coarser grained iron has been cooled more slowly than a fine grained iron and hence the austenite prior to transforming to pearlite will have had better opportunity to approach equilibrium and hence will have a lower carbon content than a rapidly cooled crystal which will be somewhat supersaturated. A supersaturated austenite grain should tend to nucleate temper carbon more readily than a coarse grain which contains the equilibrium amount of carbon.

The important problem for the malleable foundryman is how to control nucleation. Too frequently nucleation rate and reaction rate are confused.

An interesting experiment was run in this connection on samples of a white iron. It was found that first stage graphitization was completed in 6, 20 and 40 hours at 1700, 1550, and 1450° F. respectively. Specimens were heated *one* hour at 1700 and then reheated at the above temperatures to complete first stage annealing. In this way the nucleation was completed during the 1700° F. heating and the graphite pattern was fixed. Complete annealing took place as follows:

1 hour (1700° F.) plus 5 hours (1700° F.), Total 6 hours
1 hour (1700° F.) plus 8 hours (1550° F.), Total 9 hours
1 hour (1700° F.) plus 23 hours (1450° F.), Total 23 hours

This test shows the great significance of the carbon count in annealing.

MEMBER: You cannot complete the annealing at the higher temperature, can you, because graphitization of the pearlite is progressive as you come down to the lower temperature or else you would have a residual cementite. First stage graphitization can be completed at higher temperatures and the lower temperatures are just as essential for perfect annealing.

DR. SCHNEIDEWIND: One interesting experiment, although not of practical interest, may be of general interest. We heated to 1700° F. for 6 hr. to complete first stage annealing and then we set the furnace at 1360° F. which I guessed would be in the three phase range and we held it one hour. The sample was practically entirely ferritic showing that first and second stage graphitization were completed.

MEMBER: The ones who collaborated with me years ago did the same thing. There was one other thing I wanted to mention in that same paper, which was the result of the graphitization of a 3-in. square bar which was after annealing cut into 12 one-inch square specimens. Now that 3-in. square by 6-in. long, half of the usual 3-in. square test piece, showed complete graphitization. It had a coarse structure, but it was annealed in the same heat treatment as castings 1/16-in. thick, but obviously as was done in all cases, we had to set our annealing sights for the worst case and let the other conditions take care of themselves.

I think Dr. McMillan has done more than anybody else, or as much as anybody else in segregating his castings for different annealing cycles, and where the production is large and varied, that is probably a worth while practice. It is justified. It is difficult to get a segregation, but it can be done.

CO-CHAIRMAN TILLEY: Twenty-five years ago, Dr. Schwartz delved into analysis of cementite and he found that some carbon migrated to the cementite as the temperature rose. Also, silicon increased as the temperature rose. That would mean you would have both the carbon effect on the graphitization rate plus the silicon effect and you would have more of it in the lighter section than you would in the heavier section.

How do you figure that those two points affect this curve with relation to the size of the grains?

DR. SCHNEIDEWIND: I am not willing to hazard a guess yet on how and why nucleation occurs. I believe that silicon has been more or less ignored. We just accepted it that more silicon content meant quicker annealing but I do not think the mechanism has been properly worked out.

* Fractures and Microstructures of American Malleable Cast Iron" by W. E. Bean, H. W. Highriter and E. S. Davenport, *Transactions*, American Foundrymen's Association, vol. 29, p. 306 (1920).

ISOTHERMAL TRANSFORMATION OF MOLYBDENUM CAST IRON

By
Charles Nagler, Ph.D. *
and
Ralph L. Dowdell, Ph.D. **

ABSTRACT

Isothermal heat treatment of gray and alloyed cast irons should result in improved mechanical properties. This treatment should also permit more complicated castings to be heat treated because salt bath quenching is less drastic than oil or water quenching.

The temperature for isothermal transformation may be determined by metallographic methods which are time consuming or by a magnetic method which is rapid. Both methods correlate quite well.

The molybdenum cast irons investigated did not have a simple typical "S" curve. They showed deep hardenability in the temperature range 900 F to 1300 F indicating the possibility of heat treating comparatively heavy sections. Hardenability as determined by the end-quench type test correlated well with results determined on a one-inch round.

CAST IRON, an alloy of iron, carbon, and silicon, plus small amounts of manganese, sulphur, and phosphorus and impurities is characterized by having high tensile strength, good hardenability, good damping properties, low impact toughness, and zero ductility as measured by per cent elongation. The isothermal method of heat treatment, when applied to small section sizes of steel, has resulted in increased impact toughness for the same tensile strength and hardness. The successful application of the isothermal method of heat treatment is dependent on accurately determining the isothermal transformation curves for the steel.

With numerous publications available, illustrating the isothermal transformation curves for steel, the isothermal methods of heat treatment, it was postulated that similar heat treatment may result in improvement of the physical properties of cast iron. Cast iron and steel are similar in some respects. The combined carbon in cast iron is usually between 0.60 and 0.80 per cent, which compares favorably with the carbon content of a medium high carbon steel. The remainder of the carbon in cast iron is present in the uncombined form known as flake graphite. Cast iron and steel react favorably to hardenability.

The hardening heat treatment of steel or cast iron results in straining of the metal which increases the hardness. The strain within the metal is caused by the volume change which takes place in quenching the austenite, a gamma iron solid solution of carbon plus other elements, rapidly to a low temperature at which the alpha modification of iron exists. The volume change that takes place on quenching at a rate equal to or faster than the critical cooling rate is sufficiently drastic to often cause cracking. By the use of the isothermal method of heat treatment it is possible to ease the transformation of austenite to martensite and still produce the desired hardness in the part without cracking.

The temperature of isothermal transformation to be used in the heat treatment of steel or cast iron is determined from its isothermal transformation curves. By metallographic methods the determination of isothermal transformation curves is tedious and long. By the magnetic method it is possible to determine the curves rapidly.

The application of isothermal method of heat treatment has been limited to low alloy steels in small section sizes. The austenitic transformation at any subcritical temperature in cast iron is more sluggish than in steel and thus allows larger sections of cast iron to be heat treated in that manner.

It is believed that the application of the isothermal method of heat treatment to gray cast iron piston rings, cylinder liners, brake drums, brake shoes, crank shafts, etc. would result in increased physical properties.

Experimental Procedure

Preparation of Cast Iron Transverse Test Bars from the Cupola Heat. The cast irons used in this investigation were cast from a melt made in the 15 in. cupola in the Foundry Laboratory, Mechanical Engineering Department, University of Minnesota. Transverse test bars were poured according to the ASTM standards

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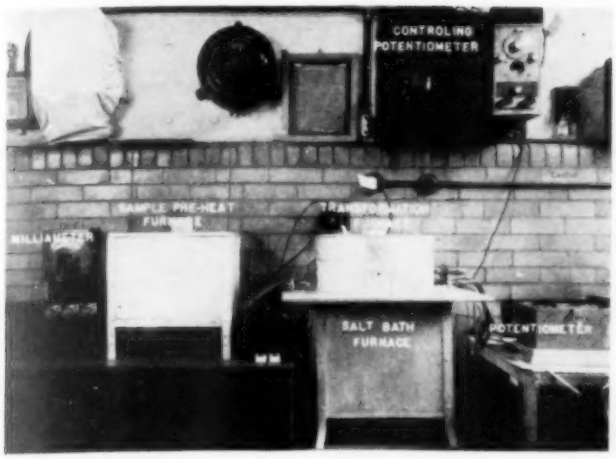


Fig. 1—Arrangement of austenite transformation equipment.

No. A 124-29 and allowed to cool in the mold over night. The following morning the bars were shaken out and labeled as indicated in Table 1.

Chemical Analysis of the Cast Irons. The chemical composition of Heat Nos. 355, 356, 357, 358 and 359 were determined and in Table 2 are indicated the analyses of the cast irons. In the production of the cupola heats it was desired to keep carbon, silicon, manganese, phosphorus, and sulphur fairly constant and to vary only the molybdenum content.

Physical Properties of the Cast Irons. The physical properties of the cast irons in the as-cast condition were determined on transverse bars from Heat Nos. 355, 356, 357, 358, and 359. The transverse breaking load was determined on standard 1.2 in. arbitration bars broken on 18 in. centers. The deflection on the transverse loading was measured at a load of 1500 lb. and at the transverse breaking load. Standard tensile specimens were prepared from arbitration bars, and their ultimate breaking load was determined. Hardness tests were made on samples cut from the threaded ends of broken tensile bars. Four samples were used from each heat in order to arrive at an average set of physical values for the heats. In Table 3 the physical properties as determined for each of the irons are tabulated and an average value shown for each of the mechanical properties measured.

TABLE 1.—LABELING OF TRANSVERSE BARS

Tap No.	Sample No.
9	355
10	356
11	357
12	358
13	359

Preparation of Specimens from the Cast Transverse Bars. The sand cast 1.2-in. bars 21 in. long were machined into samples $\frac{1}{8}$ in. in thickness and $\frac{1}{2}$ in. square. The samples were then drilled with a small hole on the $\frac{1}{8}$-in. by $\frac{1}{2}$-in. face and a length of chromel wire fitted into the hole which served as a specimen holder. The chromel specimen holder being austenitic and non-magnetic underwent no phase change when heated or cooled and for that reason did not influence the permeability values as measured on each specimen in the form of ammeter readings in the magnetic transformation furnace.

Description of Apparatus

Arrangement of equipment used in determining austenite transformation rates in cast iron by the magnetic method is shown in Fig. 1. The apparatus used was developed in 1938 for a study of the effect of normal and alloying elements on rate of austenite transformation in cast iron at sub-critical temperatures.

A complete description of the apparatus appeared in the publication of Nagler and Wood.[1] The apparatus used in this investigation was constructed similarly to the one described in the paper, with added improvements to facilitate the gathering of data.

The preheat furnace was of the electrical resistance type and contained a small nickel crucible two inches in diameter and two inches in depth. The crucible was filled with a carburizing material, and the specimens were placed in the media to prevent decarburization during preheating and prior to the austenite transformation determination. The controller thermocouple and the checking thermocouple were placed in the nickel crucible, an arrangement which allowed for a temperature variation of $\pm$ 5 F.

The salt bath transformation quenching furnace was of the automatically controlled electrical resistance type and contained a steel crucible 5 in. in diameter and 9 in. high. A fused salt, stable up to 1300 F and melting at 325 F was used as a quenching media. Above 1200 F the salt attacked the crucible and became contaminated with iron oxide. To eliminate this difficulty it was necessary to use a rectifier salt to settle out the oxide. When the oxide was scraped off the bottom and sides of the crucible, the salt was again useable at any desired temperature.

Figure 2 shows a cross section of the magnetic transformation furnace having the primary pole fixed and the secondary pole moveable. A spring arrangement was placed on the end of the secondary pole, outside of the furnace, which assured good contact between the

TABLE 2.—CHEMICAL COMPOSITION OF SPECIAL CAST IRONS

Sample Number	Carbon			Si	Mn	P	S	Mo
	Total	Graphitic	Combined					
355	3.14	2.49	0.65	2.15	0.81	0.219	0.064	0.000
356	3.12	2.37	0.75	1.91	0.76	0.210	0.068	0.086
357	3.04	2.33	0.71	1.83	0.72	0.212	0.072	0.149
358	3.00	2.18	0.82	1.85	0.73	0.216	0.072	0.249
359	2.93	2.12	0.81	1.89	0.74	0.218	0.062	0.513

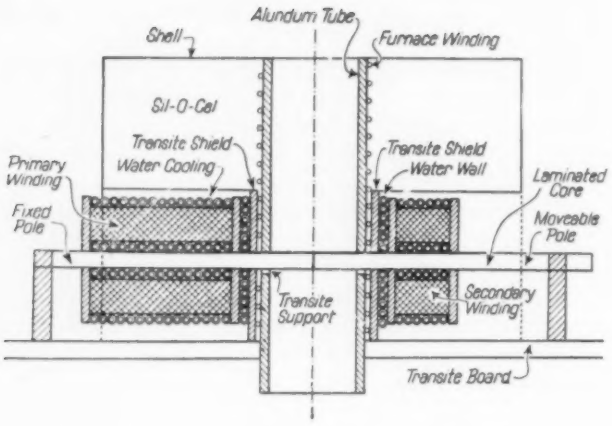


Fig. 2—Detail drawing of transformation furnace.

poles and the specimen. The primary coil contained 2200 turns of No. 22 copper enameled magnet wire and was connected across a 110 volt line. The resistance of the primary coil was 14 ohms, and a current of 7.8 amperes was found to flow in the circuit at room temperature.

The primary coil and secondary coil were water cooled to prevent breakdown of the insulation due to furnace radiations. The secondary coil was wound with 120 turns of No. 18 copper enameled magnet wire, and had a resistance of 1 ohm. With the faces of the poles of the austenite transformer together with the furnace at room temperature, and with 110 volts across the primary circuit, 500 milliamperes was found to be flowing in the secondary circuit.

The voltage produced in the secondary coil was equal to 6.4. With an air gap of $\frac{1}{8}$ in. between the poles of the transformer, at room temperature, 260 milliamperes was found to flow in the secondary circuit. As the temperature of the furnace increased, the current in the secondary circuit also increased.

Grain and Graphite Flake Size

Numerous heat treatments, as suggested in the literature, were carried out to reveal the austenic grain sizes of the cast irons. None of the tests tried gave satisfactory results, and the decision was made to compare the cast irons on the basis of fracture tests.

The transverse bars were fractured 2 in. from the bottom of each bar and a comparison of the fracture grain size since, as Fig. 3 shows, there was no visual difference in the fractures of the bars, the assumption was made that the cast irons were approximately of the same grain size.

The graphite flake size and graphite pattern were determined for each of the cast irons investigated. Samples adjacent to the end of the bar from which the fracture specimens were taken and polished, etched, and photographed at 100 magnifications (upper part Fig. 4) and were used in the determination of the graphite flake pattern and graphite flake size. The A.F.A.—ASTM Graphite Flake size and distribution chart appended in the A.F.A. CAST METALS HANDBOOK was used in studying and classifying the graphite flakes in each of the cast iron. See Table 4.

TABLE 3.—PHYSICAL PROPERTIES OF TRANSVERSE TEST BARS

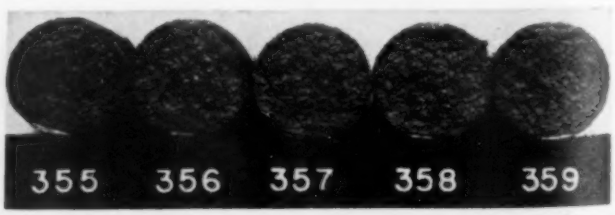
Heat Number	Transverse Breaking Load, Lb.	Deflection at 1500 Lb.	Deflection at Breaking Load	Tensile Strength PSI.	Hardness RC
355	2650	0.13	0.32	39,060	19
	2546	0.13	0.30	28,740	19
	2935	0.13	0.32	38,220	20
	2940	0.13	0.31		20
Aver.	2773	0.13	0.31	35,340	20
356	3009	0.12	0.33	40,360	15
	2926	0.12	0.28	42,560	15
	2859	0.12	0.29	41,100	14
	3009	0.12	0.32		15
Aver.	2951	0.12	0.30	41,005	15
357	2872	0.12	0.31	41,900	19
	3135	0.12	0.31	42,440	19
	2821	0.13	0.30	39,320	19
	3225	0.11	0.31		19
Aver.	3013	0.12	0.31	41,220	19
358	2788	0.12	0.27	43,880	20
	3142	0.11	0.27	46,660	19
	3390	0.11	0.30	47,260	20
	3302	0.11	0.29		19
Aver.	3206	0.11	0.28	45,900	20
359	3665	0.11	0.27	47,980	24
	3400	0.12	0.32	45,260	25
	3361	0.13	0.33	45,400	24
	4000	0.11	0.30		25
Aver.	3607	0.11	0.30	46,213	25

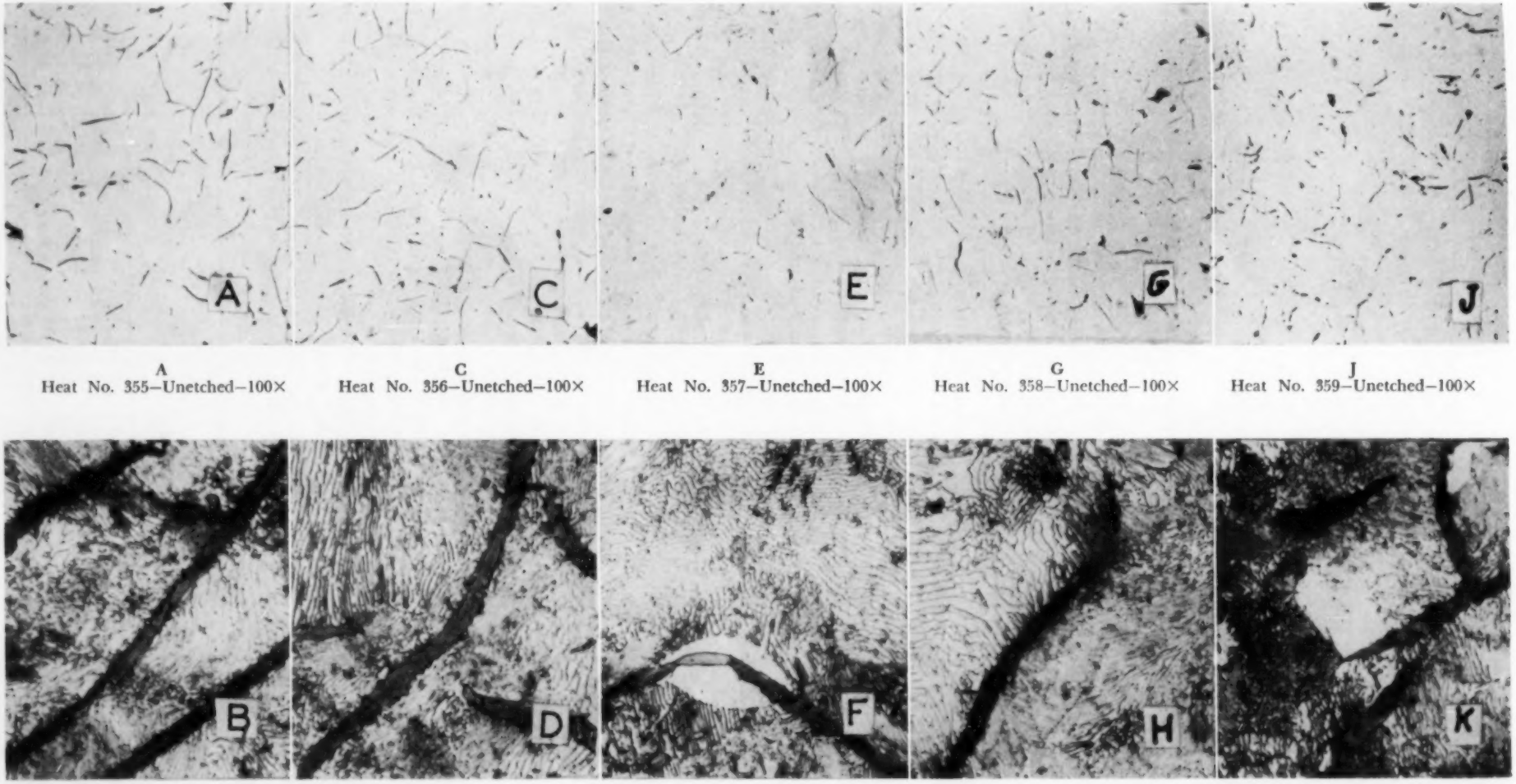
Hardenability of Cast Irons. The hardenability of the cast irons used in this investigation was determined by two different methods. The first method used was to make hardness determinations across the face of the specimen, and the second method was similar to ASTM Spec. A255-45T, End-Quench Test for Hardenability of Steel.[2] In the first method, samples from each heat, one inch in diameter and one inch long, were packed in carburizing compound, heated to 1600 F, and held at that temperature for one hr. and then quenched in water.

The samples were then sectioned into two parts with a cut-off wheel and hardness determinations made every $\frac{1}{16}$ in. (Table 5) across the diameter. In Fig. 5, the hardness is plotted, as determined on a quenched specimen across the face vs. dimension. In all cases, the cast irons were found to be deep hardening. There was little difference between the average Rockwell "C" hardness of each of the hardened cast irons.

From each heat, end-quenched (Jominy) hardenability test specimens 3 in. long and one inch in diameter were machined from the cast transverse bars. The hardenability samples were packed in carburizing compound, heated to 1600 F, and held at temperature

Fig. 3—Fractures of cast irons. Reduced appr. ½.





A	C	E	G	J
Heat No. 355—Unetched—100×	Heat No. 356—Unetched—100×	Heat No. 357—Unetched—100×	Heat No. 358—Unetched—100×	Heat No. 359—Unetched—100×

B		D		F		H		K	
Heat No. 355—Etched—1000×		Heat No. 356—Etched—1000×		Heat No. 357—Etched—1000×		Heat No. 358—Etched—1000×		Heat No. 359—Etched—1000×	
Carbon	3.14%	Carbon	3.12%	Carbon	3.04%	Carbon	3.00%	Carbon	2.93%
Silicon	2.15%	Silicon	1.91%	Silicon	1.83%	Silicon	1.85%	Silicon	1.89%
Manganese	0.81%	Manganese	0.76%	Manganese	0.72%	Manganese	0.73%	Managanese	0.74%
Phosphorus	0.129%	Phosphorus	0.210%	Phosphorus	0.212%	Phosphorus	0.216%	Phosphorus	0.218%
Sulphur	0.064%	Sulphur	0.068%	Sulphur	0.072%	Sulphur	0.072%	Sulphur	0.062%
Molybdenum	0.000%	Molybdenum	0.086%	Molybdenum	0.149%	Molybdenum	0.249%	Molybdenum	0.513%

Fig. 4—Microstructures of Cast Irons in the As Cast Condition.

TABLE 4.—RATING OF CAST IRONS ON BASIS OF FLAKE SIZE AND FLAKE TYPE

Sample Number	Graphite Flake Size Number	Graphite Flake Type
355	5+	A+++
356	5+	A+++
357	5+	A+++
358	6++	A+++
359	6++	A+++

\+ ¼ to ½-in. flake length at 100X.
++ ⅛ to ¼-in. flake length at 100X.
+++ Uniform distribution and random orientation.

for one hour. The samples were removed rapidly from the furnace and placed in a quenching jig, according to A.S.T.M. Specification A255-45T, End-Quenched Test for Hardenability of Steel which was so arranged that the bottom of the specimen was the only part quenched.

The water was sprayed continuously on the bottom of the specimen for 15 min., thus allowing the whole bar to cool below the lower critical. Following this, the bar was water quenched. The sample was ground flat along the 3-in. length to a depth of 0.015 in. and Rockwell "C" determinations were taken at stations ⅛ in. apart from the water cooled end to the air cooled end. Typical hardness data, as determined on one of the samples, were plotted versus length as shown in Fig. 5.

The cast iron from Heat No. 359 showed a tendency to be somewhat deep hardening in heavy sections as indicated by the air cooled end of the bar having a Rockwell "C" hardness of 41, and the quenched end

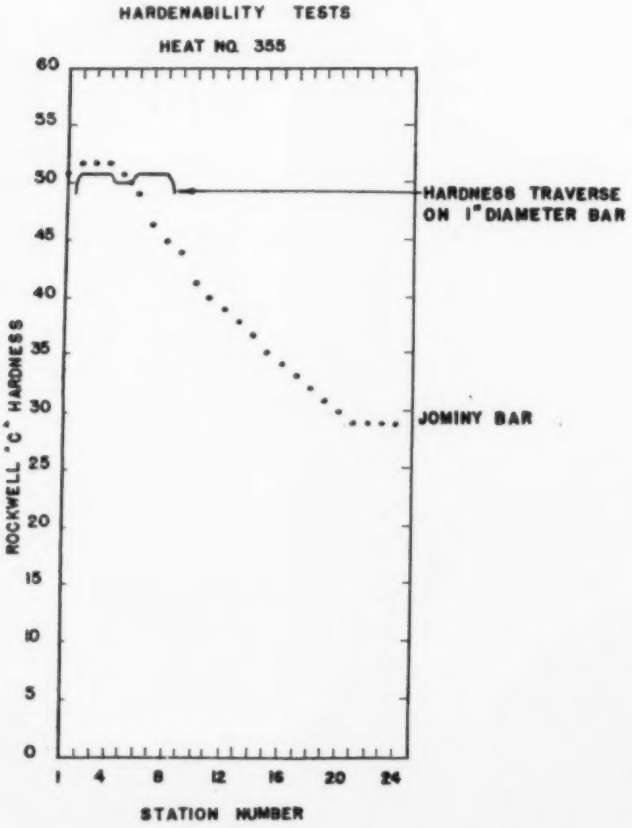


Fig. 5—Results of hardenability tests.

a hardness of 56. To show the usefulness of this hardenability test, in Table 6 are recorded Rockwell "C" hardness values as determined on samples from each heat at the stations indicated. At station 16 can be read an indication of the hardness at the center of a 4-in. section of cast iron, from each of the heats, after holding the specimens at 1600 F for one hour and quenching in water.

This method can be used in predicting the depth of hardening at the center of the specimen, which can be produced in quenching faster than the critical cooling rate any section size up to 6 in. The end-quench test for steel as applied to cast iron for study of hardenability works very well and can be used to indicate the maximum hardenability which can be produced at any section size up to 6 in. in one specimen. This allows the hardenability to be determined for a variety of section sizes on only one specimen.

Determining Isothermal Transformation Data

The sample studied isothermally was preheated 38 min. at 1600 F, 8 min. of which was required for the specimen to come up to the furnace temperature. The 8-min. preheat time was determined by inserting a thermocouple in the specimen and following the heating rate from room temperature up to 1600 F.

All specimens were preheated in a small nickel crucible which contained a mixture of 60 mesh charcoal and carburizing compound for the purpose of preventing any decarburization from taking place during solution of the carbides in the cast iron at the elevated temperature. The hot powdered carbonaceous material resulted in rapid heat transfer to the cast iron specimen and allowed samples to come to the solution temperature in 8 min.

At the end of the solution time, the sample was rapidly removed from the preheat furnace and quenched in the salt bath situated below the transformation furnace. The sample was held in the salt bath for 3 sec. to bring the specimen temperature from 1600 F down to the desired transformation temperature.

A holding time of 3 sec. in the salt bath was found to be sufficiently long to bring a ½ x ½ x ⅛-in. cast iron specimen containing a thermocouple from the solution temperature of 1600 F to the transformation temperature of 400 F. This being the lowest quenching temperature, it was, therefore, assumed that it would take less time to quench a specimen of similar size from 1600 F to any temperature above 400 F. A 3-sec. holding time in the salt bath was used for each of the samples on which isothermal transformation studies at any temperature were carried out.

TABLE 5.—VARIATION IN HARDNESS ACROSS DIAMETER OF ONE-INCH ROUNDS

	Hardness, R_C, at Stations 1/16 in. apart														
Sample No.	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15
355	49	51	51	51	51	51	50	50	50	51	51	51	51	51	49
356	46	50	50	50	50	49	49	49	49	49	50	50	50	50	46
357	47	50	51	51	51	51	51	51	51	51	51	51	51	50	47
358	49	51	51	51	51	50	50	50	50	50	51	51	51	51	49
359	51	53	53	53	53	53	50	50	50	53	53	53	53	53	51



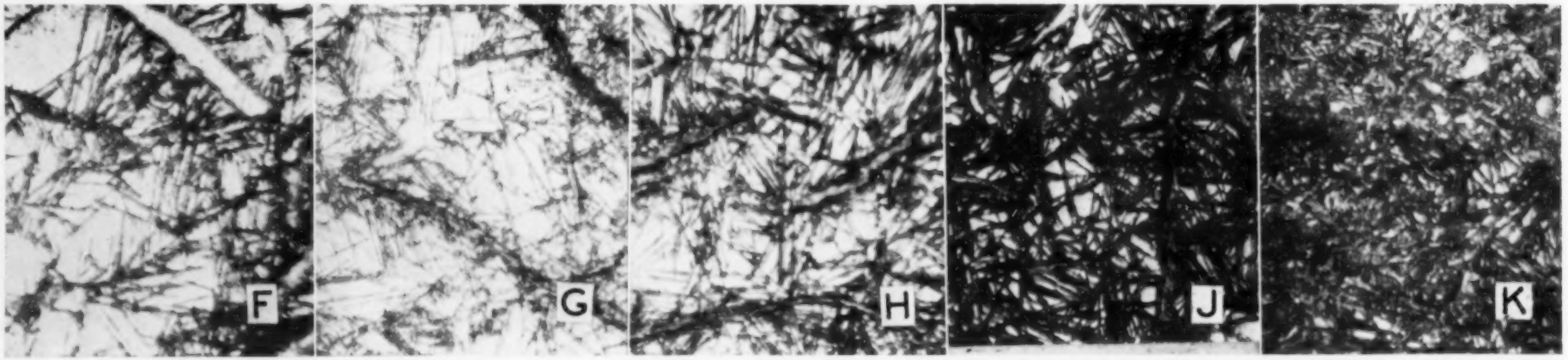
A
0% transformed after 0.5 min. at transformation temperature.
Rockwell "C" 52

B
1% transformed after 1.0 min. at transformation temperature.
Rockwell "C" 52

C
5% transformed after 2.0 min. at transformation temperature.
Rockwell "C" 51

D
10% transformed after 4.0 min. at transformation temperature.
Rockwell "C" 50

E
40% transformed after 8.0 min. at transformation temperature.
Rockwell "C" 49



F
60% transformed after 12.0 min. at transformation temperature.
Rockwell "C" 49

G
70% transformed after 16.0 min. at transformation temperature.
Rockwell "C" 48

H
80% transformed after 20.0 min. at transformation temperature.
Rockwell "C" 47

J
90% transformed after 25.0 min. at transformation temperature.
Rockwell "C" 46

K
99.5% transformed after 30.0 min. at transformation temperature.
Rockwell "C" 46

Fig. 7—Microstructure of isothermal transformation products of austenite in cast iron Heat No. 357. Cast iron held at austenizing temperature of 1600° F. for 30 min. and quenched in a salt bath at 500° F. for times as indicated. Analysis of Heat No. 357—C, 3.04; Si, 1.83; Mn, 0.72; S, 0.072; P, 0.212; Mo, 0.149. Mag.—1000×.

TABLE 6.—VARIATION IN HARDNESS ALONG EDGE OF JOMINY TEST BAR STARTING AT QUENCHED END

Sample No.	Rockwell "C" Hardness at Stations ⅛ in. apart																							
	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24
355	51	52	52	52	51	49	47	45	44	42	40	39	38	37	35	34	33	32	31	30	29	29	29	29
356	43	48	48	47	43	40	37	34	31	29	27	24	22	20	19	17	16	15	14	13	13	13	14	15
357	47	52	53	52	50	48	47	46	45	44	43	42	41	40	40	39	38	38	38	38	38	38	39	40
358	50	50	50	50	50	50	49	48	47	46	45	44	42	40	38	36	34	32	29	27	25	24	25	27
359	56	55	54	54	53	52	52	51	51	50	50	49	48	48	47	46	45	44	43	43	42	41	41	41

At the end of the 3-sec. quench in the salt bath, the sample was drawn up into the transformation furnace which was situated directly above the salt bath and placed between the poles of the transformer. In all cases the transformation furnace was maintained at the same temperature as the salt bath, the temperatures of both salt bath and transformation furnace being automatically controlled within ± 5 F.

A foot switch was placed in the primary current circuit so that as soon as the specimen had been placed between the poles of the transformer, the current could be made to flow. This arrangement made it possible to take data as soon as the specimen was placed between the poles of the transformer.

To follow the progress of the austenite transformation milliameter readings were taken every 10 sec. until the reaction was completed. This was indicated by a series of constant ammeter readings lasting over a period of 5 min.

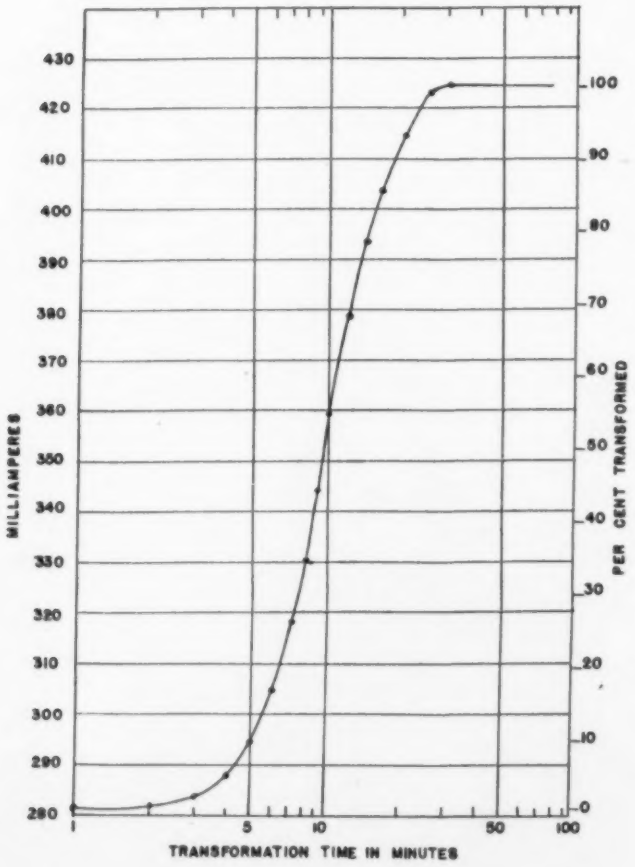


Fig. 6—Isothermal transformation curve for Heat No. 357 at 500° F.

Proof of the Validity of the Magnetic Method for Determination of Austenite Transformation Rates at Subcritical Temperatures. The magnetic method for determining the isothermal transformation rate of austenite in cast iron at subcritical temperatures was checked at 500 F and 1000 F. Twenty samples ½ x ¼ x ⅛ in. from Heat No. 357, were mounted on chromel wire hangers and were used in securing the metallographic proof of austenite transformation at the subcritical temperatures. Ten of the samples were isothermally transformed from 1600 F to 500 F for varying lengths of time from 0.5 min. to 30 min.

Each sample was placed in the graphite-containing nickel crucible of the preheat furnace and held at 1600 F for 38 min. The samples were then rapidly quenched from the preheat furnace into the salt bath furnace at 500 F for varying lengths of time (Table 7).

The samples were completely polished and etched and were examined metallographically. The amount of transformation which had taken place in each specimen was estimated visually. Also included in Table 7 are data showing the completeness of transformation at each time interval as calculated from the magnetic determinations of austenite transformation at 500 F on a sample from Heat No. 357, using the same solution temperature and time.

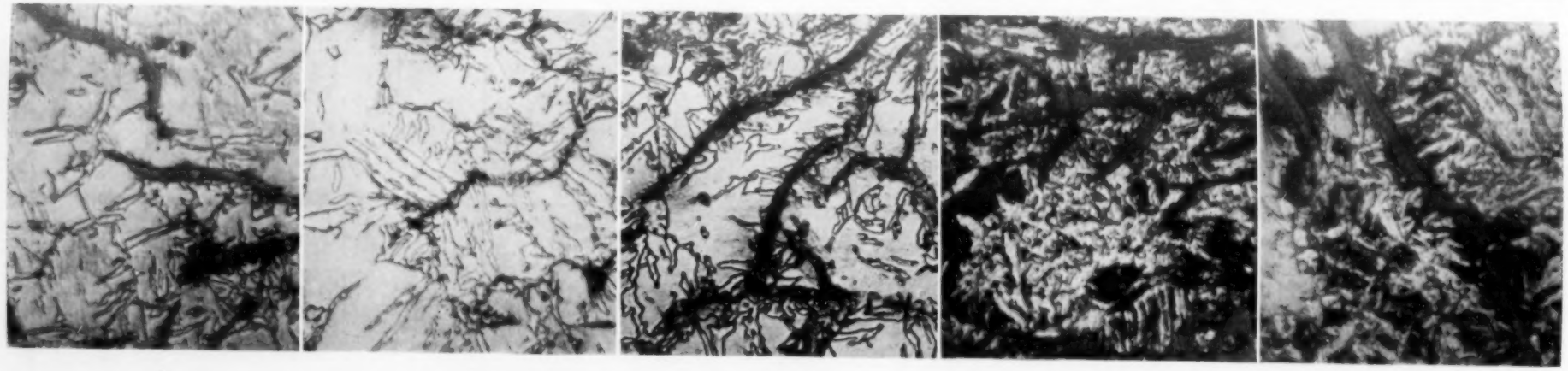
Figure 6 shows the transformation curve for a sample from Heat No. 357 isothermally transformed at 500 F. The vertical axis is in terms of milliameter readings or per cent austenite, and the horizontal axis is in time increments. In comparing the per cent of austenite transformation at 500 F as determined metallographically with the transformation as determined magnetically, the agreement is good.

The photomicrographs in Fig. 7 show the amount of transformation which had taken place at each time

TABLE 7.—COMPARISON OF ESTIMATED AND CALCULATED PER CENTS OF TRANSFORMATIONS AT 500 F

Sample Number	Time in Salt Bath, Min.	Hardness, Rockwell "C"	Austenite Transformation	
			Estimated Per Cent	Calculated Per Cent
A	0.5	52	0	0
B	1.0	52	1	3.5
C	2.0	51	5	4.1
D	4.0	50	10	6.2
E	8.0	49	40	36.0
F	12.0	49	60	70.1
G	16.0	48	70	85.7
H	20.0	47	80	93.9
J	25.0	46	90	98.0
K	30.0	46	99.5	99.5

Solution temperature 1600F. Solution time 38 min. Salt bath temperature 300F.



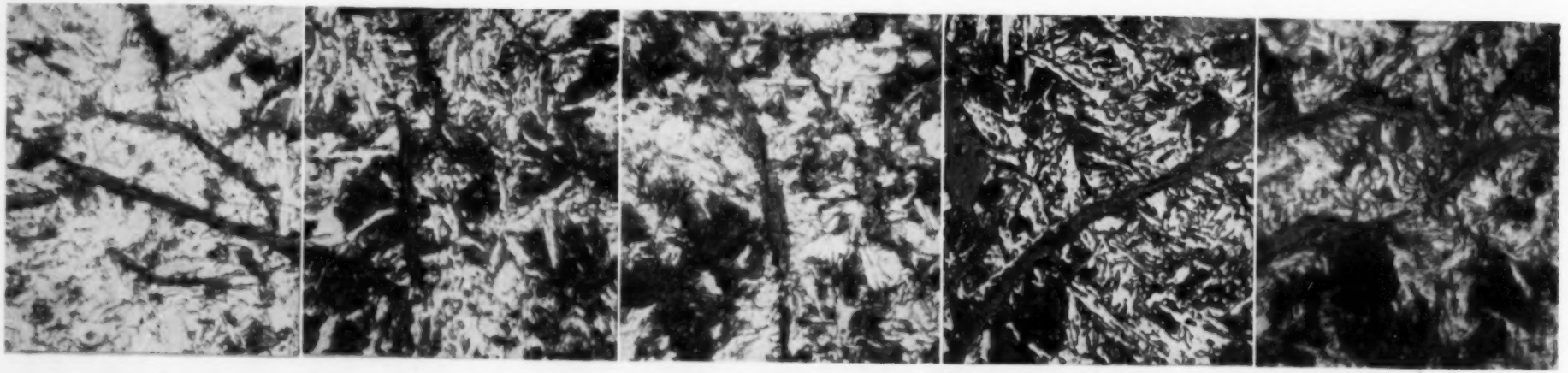
A
15% transformed after 0.5 min. at transformation temperature. Rockwell "C" 46

B
30% transformed after 1.0 min. at transformation temperature. Rockwell "C" 45

C
60% transformed after 2.0 min. at transformation temperature. Rockwell "C" 43

D
80% transformed after 3.0 min. at transformation temperature. Rockwell "C" 39

E
90% tranformed after 4.0 min. at transformation temperature. Rockwell "C" 35



F
95% transformed after 5.0 min. at transformation temperature. Rockwell "C" 31

G
98% transformed after 6.0 min. at transformation temperature. Rockwell "C" 28

H
99.5% transformed after 8.0 min. at transformation temperature. Rockwell "C" 26

J
99.5% transformed after 10.0 min. at transformation temperature. Rockwell "C" 26

K
99.5% transformed after 12.0 min. at transformation temperature. Rockwell "C" 26

Fig. 8—Microstructures of isothermal transformation products of austenite in cast iron Heat No. 357. Cast iron held at austenizing temperature of 1600° F. for 30 min. and quenched in a salt bath at 1000° F. for times as indicated. Analysis of Heat No. 357—C, 3.04; Si, 1.83; Mn, 0.72; S, 0.072; P, 0.212; Mo, 0.149. Mag.—1000×.

TABLE 8.—COMPARISON OF ESTIMATED AND CALCULATED PER CENTS OF TRANSFORMATION AT 1000 F

Sample Number	Time in Salt Bath, Min.	Hardness, Rockwell "C"	Austenite Transformation	
			Estimated Per Cent	Calculated Per Cent
A	0.5	46	15	7.7
B	1.0	45	30	32.4
C	2.0	43	60	68.2
D	3.0	39	80	82.6
E	4.0	35	90	90.3
F	5.0	31	95	94.9
G	6.0	28	98	98.2
H	8.0	26	99.5	99.5
J	10.0	26	99.5	99.5
K	12.0	26	99.5	99.5

Solution temperature 1600F. Solution time 38 min. Salt bath temperature 1000F.

interval. The photomicrographs show the mechanism of the black needle phase ejection from the solid solution austenite. Before water quenching the specimen from 500 F, at which temperature isothermal transformation from 1600 F is incomplete, the phases present are austenite plus the black needle phase of acicular troostite.

On water quenching from 500 F the austenite forms martensite, but the acicular needles remain unchanged. The black needles appear to come out along the 60° and 120° planes typical of white martensite. The transformations tend to start at the edge of the graphite flakes, which, in all probability, are the grain boundaries of the original austenite grain formed on the freezing of the cast iron from the melt.

The completeness of transformations was checked by hardness determinations on partially transformed samples from Heat No. 357. The sample which was quenched in water after being isothermally transformed from 1600 F for 0.5 min. at 500 F had a Rockwell "C" hardness of 52, whereas a sample transformed under the same conditions for 30 min. at 500 F had a hardness on the same scale of 46 (Table 7).

Ten samples of cast iron from Heat No. 357 were used in checking the isothermal transformation rate of cast iron metallographically at 1000 F. From an austenizing temperature of 1600 F the samples were isothermally transformed in a salt bath at 1000 F from 0.5 min. to 12 min. The samples were then polished and etched, and their microstructures reproduced in photomicrographs, "A" through "K" in Fig. 8. The product of transformation of austenite at 1000 F appears to be acicular ferrite and a nodular black etching constituent which resembles primary troostite.

Primary troostite is the structure which is ordinarily produced when the quenching rate is slightly slower than the critical cooling rate and generally appears in the original austenite grain boundary. The product formed at the critical cooling rate is martensite, which consists essentially of white needles 60° and 120° to each other. The formation of the transformation product is clearly shown in photographs numbered "A" through "K," Fig. 8. Photograph "A" shows the presence of acicular ferrite, but as the transformation approaches completion more and more of the black etching constituent appears.

A sample ½ x ½ x ⅛ in. from Heat No. 357 was used in determining magnetically the rate of austenite transformation at 1000 F. The data, plotted in Fig. 9, show a characteristic isothermal transformation curve. From the curve it is evident that the reaction is complete in 8 min.

This is further evidenced by Fig. 8 (photographs "A" through "K"). On further examination of Fig.

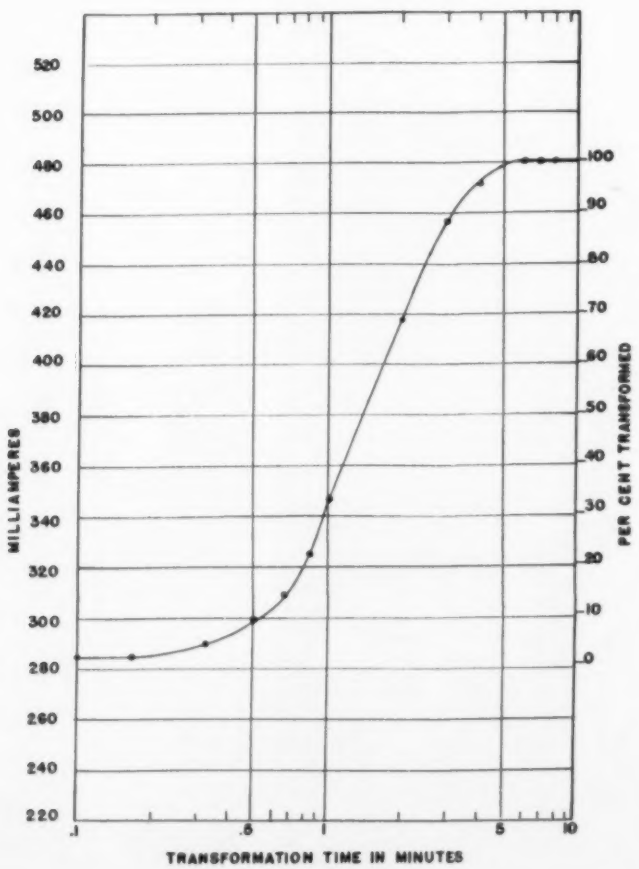


Fig. 9—Isothermal transformation curve for Heat No. 357 at 1000° F.

TABLE 9.—SAMPLE CALCULATION

Sample Number 357—Temperature 500 F
$I_f = 425$
$I_\theta = 278$
$A_0 = 147$

Θ	I_θ	A_0	A_0/A_θ	A, %	Log A_0/A_θ	$K_I^\bullet = \frac{\text{Log } A_0/A_\theta}{\theta \text{ in sec}}$
1	282	143	1.03	97.2	.01284	.000214
2	283	142	1.035	96.5	.01494	.0001247
3	284	141	1.04	95.9	.01703	.0000947
4	287	138	1.065	93.8	.02735	.0001138
5	295	130	1.13	88.5	.05308	.000177
6	306	119	1.24	81.0	.09342	.000259
7	318	107	1.37	72.7	.13672	.0003255
8	331	94	1.57	64.0	.19590	.000408
9	344	81	1.82	55.0	.26007	.000482
10	360	65	2.26	44.1	.35411	.000591
12	381	44	3.34	29.9	.52375	.000728
14	395	30	4.90	20.4	.69020	.000822
16	404	21	7.00	14.3	.84510	.000880
18	411	14	10.50	9.5	1.02119	.000945
20	416	9	16.50	6.1	1.21748	.001013
25	424	1	147.0	0.68	2.16732	.00144
30	425	0				

TABLE 10.—LOGARITHMIC PROBABILITY DATA OF PER CENT TRANSFORMATION AS DETERMINED BY EXTRAPOLATION

Transformation Temperature, °F.	Time in Min. for Beginning and End of Isothermal Transformation									
	Heat No. 355		Heat No. 356		Heat No. 357		Heat No. 358		Heat No. 359	
	0.50%	99.5%	0.50%	99.5%	0.50%	99.5%	0.50%	99.0%	0.50%	99.5%
400	9.0	168	6.1	200	4.6	80.0	9.2	205	4.1	170
500	2.3	37.5	3.2	27.0	2.6	34.0	3.2	34.0	2.0	37.0
600	.96	7.9	1.5	9.2	1.2	10.5	1.1	12.0	1.6	11.5
700	.30	21.0	.21	18.1	.28	15.0	.70	16.2	.48	19.6
800	.74	9.8	.41	9.4	.52	11.0	1.0	11.5	1.25	15.0
900	.10	20.0	.11	20.0	.11	25.0	.13	24.0	.28	48.0
1000	.92	4.7	.17	7.6	.20	10.8	.09	11.3	.47	31.0
1100	.45	1.5	.09	.64	.28	2.8	.40	1.63	1.65	4.90
1200	.24	1.0	.15	.40	.17	.86	.135	.83	.58	1.95
1300	4.5	11.6	.40	20.5	.38	9.8	.36	8.4	.33	11.1

8, it is evident that pictures "H," "J," and "K" are alike, indicating the transformation to be complete at the end of the eighth minute. In Table 8 are tabulated the hardness of the transformation product after each time interval. The per cent of transformation was calculated from the magnetic transformation curve and estimated from the photomicrographs.

By referring to Fig. 8 it can be seen that there is good agreement between the transformation data as determined magnetically and photographically. The magnetic method eliminated the human error which is inherent in estimating the relative amounts of each constituent present, as was done when determining isothermal transformation rate metallographically. It was assumed that there was sufficient accuracy in the magnetic method to warrant its use in making a comparative study of the austenite transformation rates of a number of molybdenum cast irons.

Discussion of Data. The isothermal transformation rate was determined magnetically on samples of cast iron from Heats Nos. 355, 356, 357, 358 and 359 at temperatures from 400 F to 1300 F in 100 F intervals. The method used was as described under the heading of "Method of Determining Isothermal Transformation Data."

Average data taken on three samples from Heat No. 357 isothermally transformed at 500 F will be used to illustrate the method used in treating the data and arriving at the isothermal transformation rates. Re-

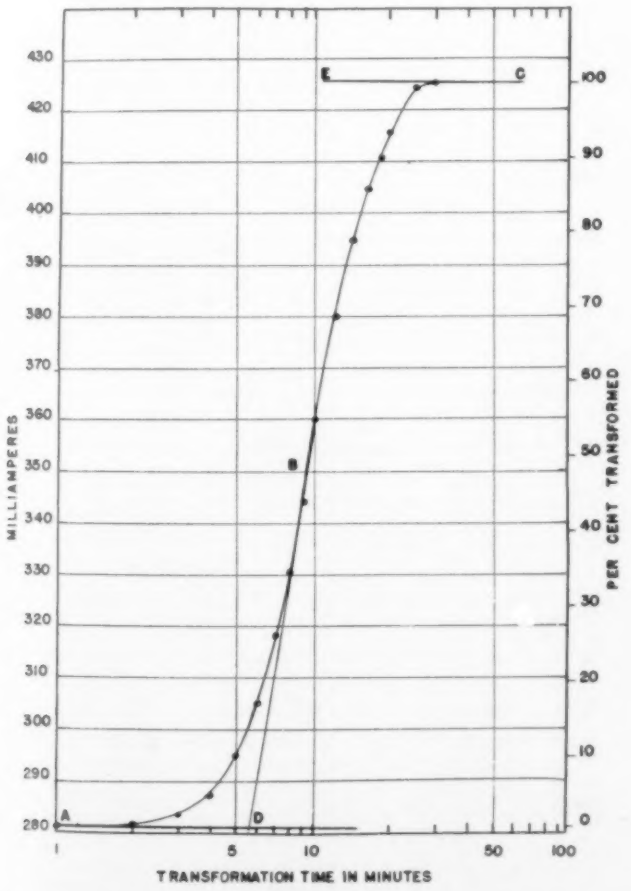


Fig. 10—Isothermal transformation curve for Heat No. 357 at 500° F.

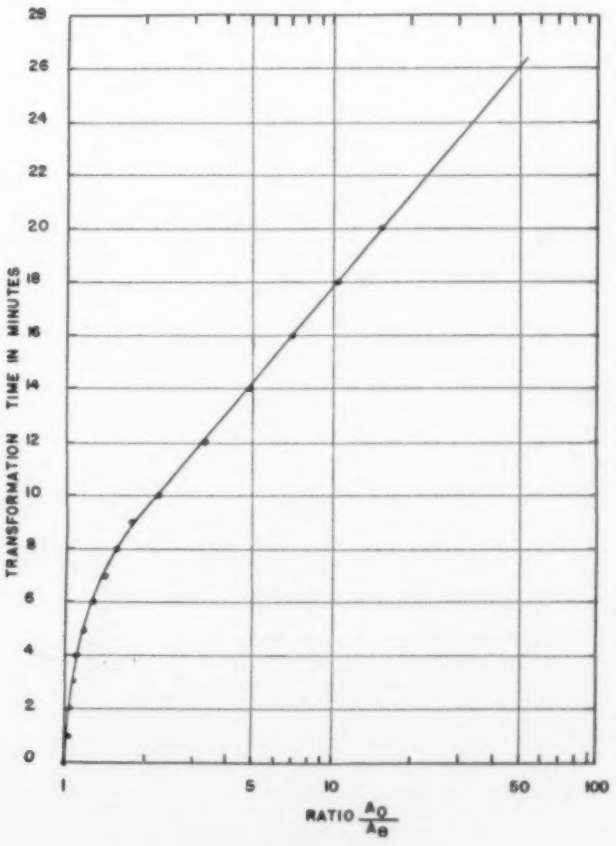


Fig. 11—Logarithmic plot of isothermal transformation data for Heat No. 357 at 500° F.

ferring to Table 9, the first column labeled θ is time measured in minutes starting from the instant the specimen was quenched in the salt bath at 500 F from the solution temperature of 1600 F.

The second column labeded I_θ contains the milliampere readings taken at each time θ. Fig. 10 is a plot of θ vs. I_θ, the lowest millampere reading of 282 indicating all austenite at the moment of quenching and the reading of 425 indicating all acicular troostite, the transformation product at 500 F ("K"—Fig. 7).

The transformation curve is similar to the curve that is characteristic of the unimolecular reaction for liquids and gases. The assumption was made that possibly the isothermal transformation of austenite followed a unimolecular reaction, which is characterized by having one reacting substance, in this case austenite, and the rate of its decomposition being proportional to the concentration at any moment.

The mathematical expression for the reaction is stated as

$$(1) \quad -\frac{dc}{ct} = Kc$$

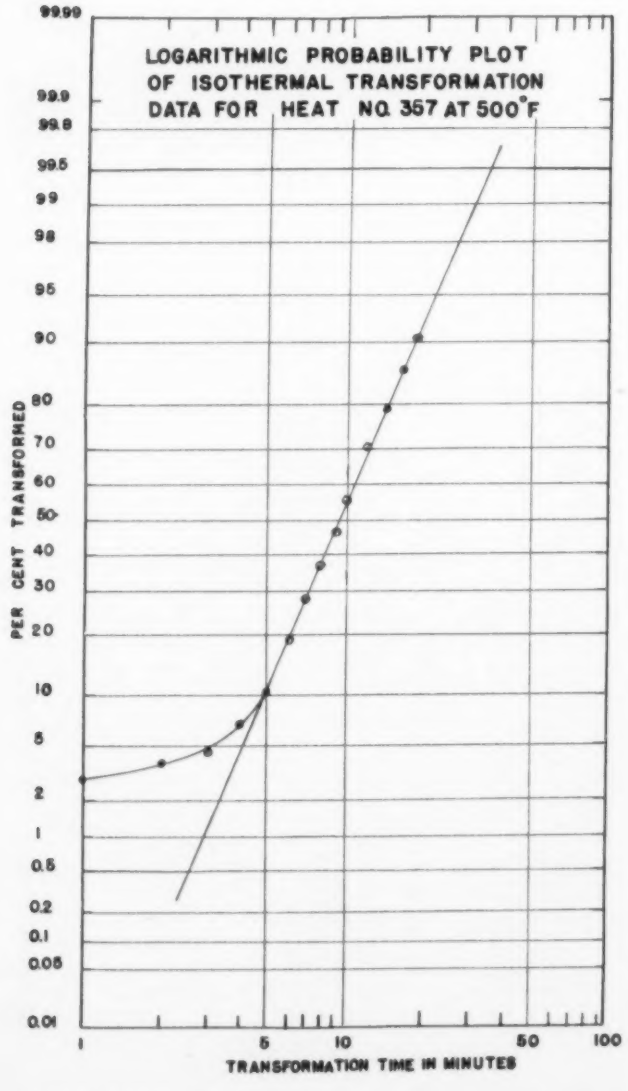


Fig. 12—Logarithmic probability plot of isothermal transformation data for Heat No. 357 at 500° F.

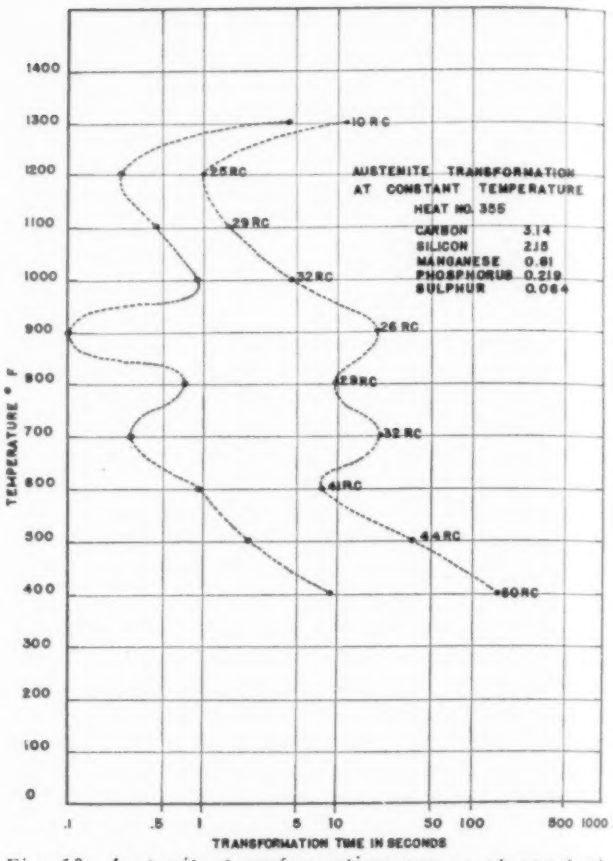


Fig. 13—Austenite transformation curves at constant temperature for Heat No. 355.

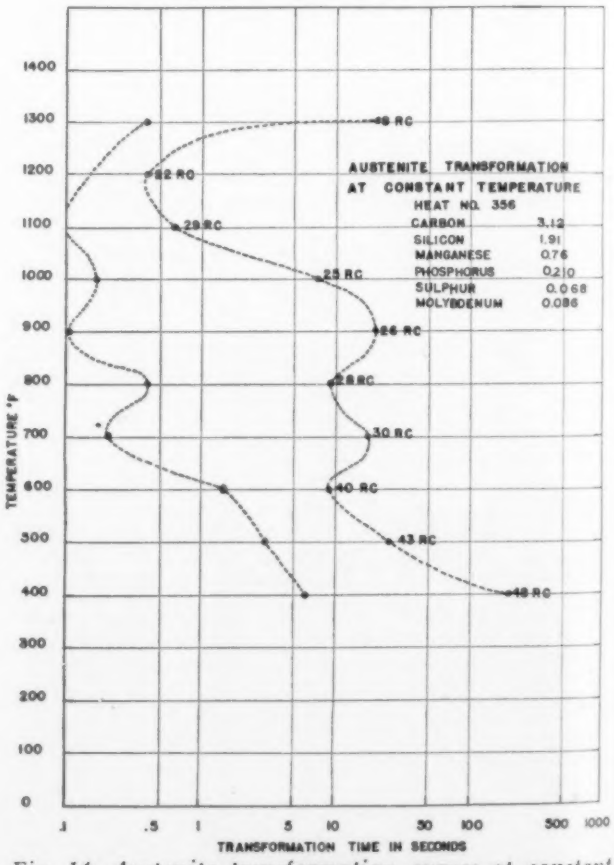


Fig. 14—Austenite transformation curves at constant temperature for Heat No. 356.

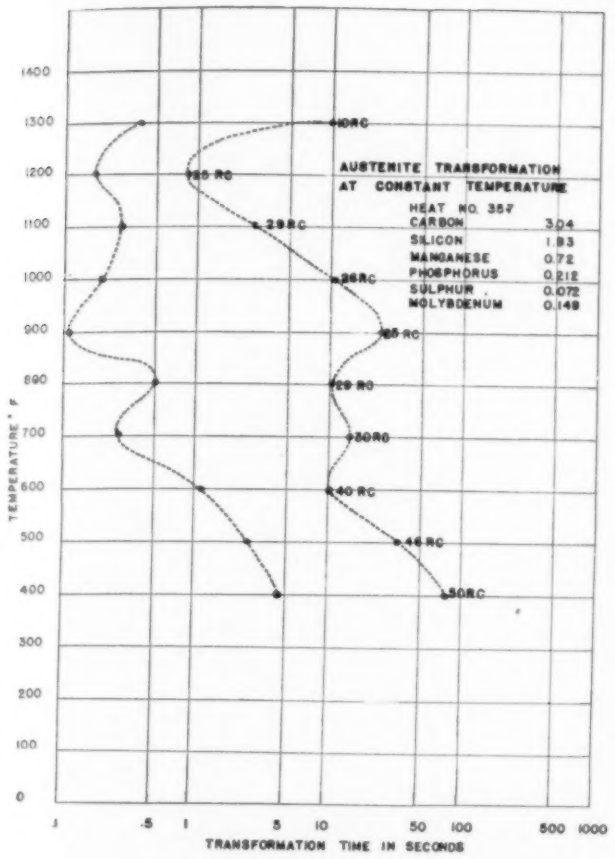


Fig. 15—Austenite transformation curves at constant temperature for Heat No. 357.

where c is the concentration of austenite, $\frac{-dc}{dt}$ is the rate at which the concentration of austenite decreases, and K is a constant dependent on grain size, composition of austenite, dissolved or undissolved carbides, temperature of reaction, and section size. Rearranging (1)

$$(2)\quad -\frac{dc}{c} = K\,dt.$$

Integrating (2)

$$(3)\quad -\ln c = Kt + \text{constant}.$$

It is evident that equation (3) is the equation for a straight line. For an unimolecular reaction a straight line results when the logarithm of the concentration is plotted versus time.

In Table 9 are found the calculated values for the ratio A_o/A_θ (concentration). These values are plotted versus the proper value of θ as shown in Fig. 11. The plot indicates that the data between the 8th and the 18th minute fall on a straight line. This seems to indicate that the decomposition of austenite at subcritical temperature may follow a first order reaction.

Integrating expression (2) between the limits of c_1 at t_1 and c_2 at t_2

$$(4)\quad -\int_{c_1}^{c_2} \frac{dc}{c} = K \int_{t_1}^{t_2} dt$$

$$(5)\quad -\ln c_2 - (-\ln C_1) = K\,(t_2 - t_1).$$

Simplifying expression (5)

$$(6)\quad K = \frac{\ln \frac{C_1}{c_2}}{t_2 - t_1}.$$

Expression (6) can be further simplified by making c_o the concentration of austenite at the beginning when time is zero and c the concentration at any time, t, as

$$(7)\quad K = \ln \frac{\frac{c_o}{c}}{t}.$$

The equation (7) may be rewritten in natural logarithms as

$$(8)\quad K_1 = \frac{\log \frac{c_o}{c}}{t}.$$

To further determine whether or not the data for the reaction studied fitted a first order reaction, the reaction constant K_1 at each time θ was calculated and tabulated in Table 9. There was found a wide variation in the constant K_1 as calculated for each time θ, a fact which is not consistent with first order reactions.

This was taken as a possible indication that the subcritical transformation of austenite did not fulfill all the requirements of a first order reaction. Reaction constants calculated on true unimolecular reactions for gases and liquids are found to give values close together, with a possible maximum variation of 5 per cent.

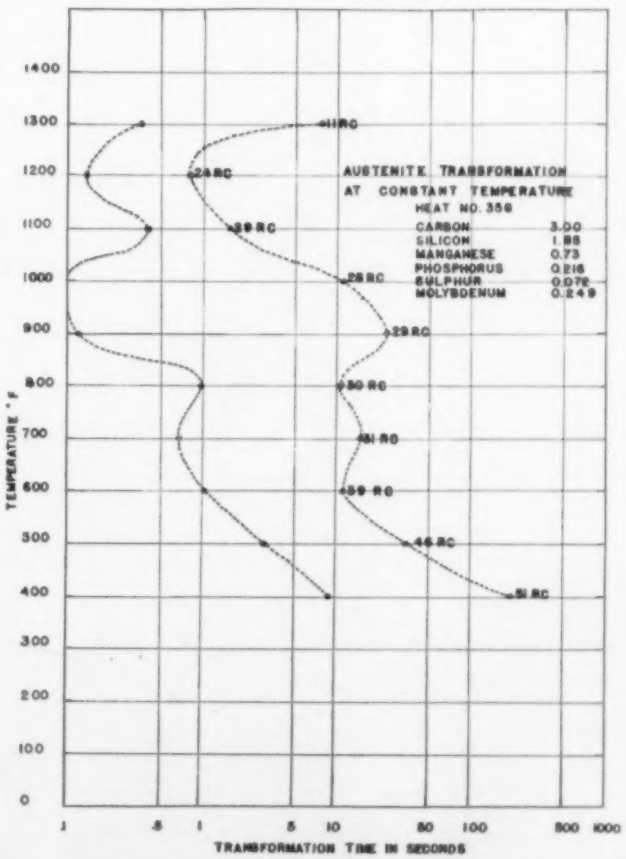


Fig. 16—Austenite transformation curves at constant temperature for Heat No. 358.

Referring to Fig. 10, the part of the curve from "A" to "B" may be considered as the incubation period, or the time interval, which is necessary for the centers, or nuclei, of the reaction to take place, or to be formed.

This statement is further substantiated by referring to Fig. 7 where it can be seen that in the first 4 min. very few black needles have been ejected from the solid solution of austenite. If the line in Fig. 10 were extended from "B" "C" down to point "D," the curve is found to be similar to that of a unimolecular reaction.

The incubation period was from "A" to "B," but the reaction did not really get under way until about the sixth minute when many black-etching acicular needles appeared. This suggests the possibility that if all the centers for the reactions had been formed instantaneously, the reaction might have followed the line "D" "B" "C."

Austin and Rickett,[3] after an extensive study of the kinetics of austenite decomposition at subcritical temperatures, suggested that transformation data be plotted on logarithmic probability paper. In making the plot, the abscissas were measured in logarithmic time and the ordinates in per cent of austenite transformed. The authors had suggested this method for plotting the data since it simplified the extrapolation in determining the beginning and end points of the transformation reaction.

In Fig. 12 is a plot using the logarithmic probability method for treating transformation data at 500 F on a sample from Heat No. 357 between the 5th and the

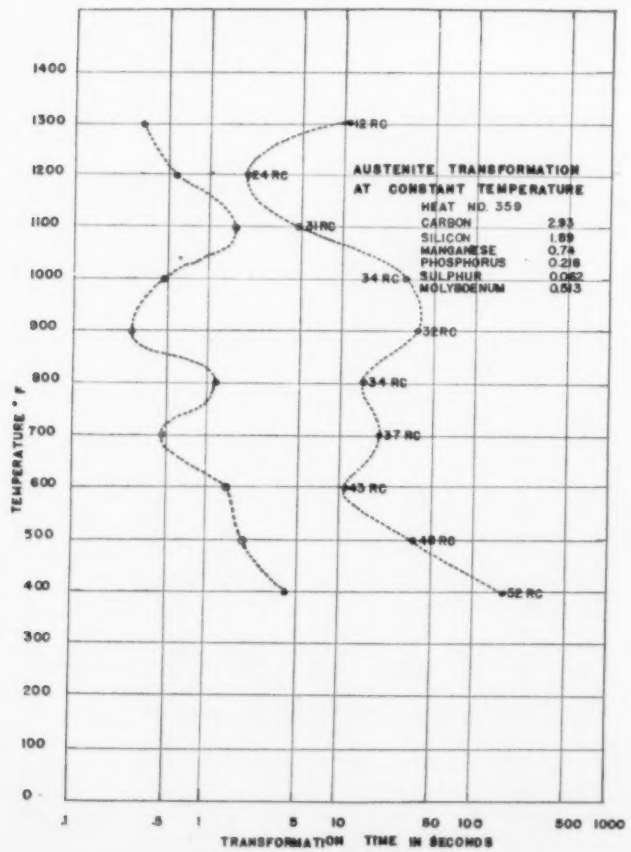


Fig. 17—Austenite transformation curves at constant temperature for Heat No. 359.

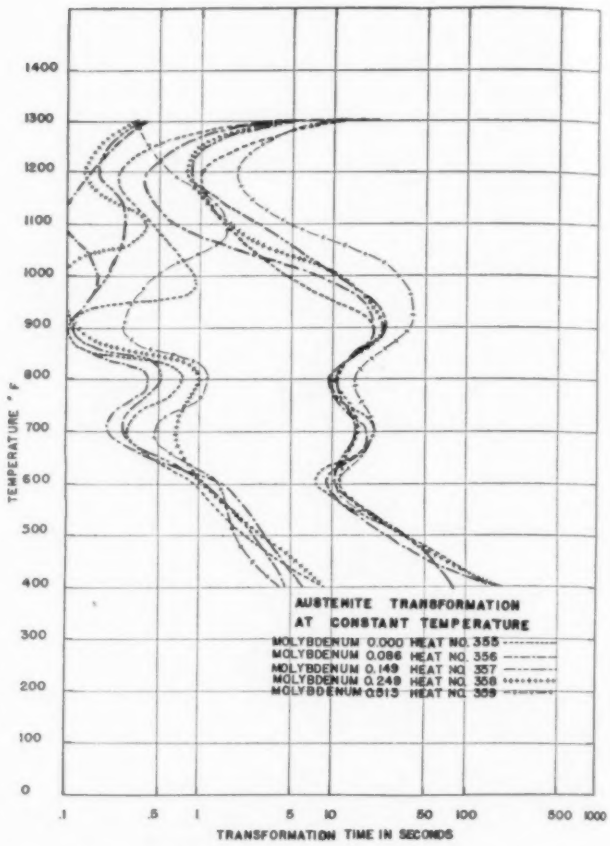


Fig. 18—Austenite transformation curves at constant temperature for five experimental heats.

20th minute. In all probability the data taken after the first minute are more accurate than those taken earlier.

Considering the reaction at 500 F, the data determined were most accurate for true isothermal transformation, neglecting incubation, between the 5th and 20th minute. For that reason the line was extended in either direction and used in determining by extrapolation at the beginning and end of the reaction. In this method of plotting transformation data, the logarithmic probability scale permits direct reading of the per cent of austenite transformation after a definite time interval.

Referring to Fig. 10, it would be difficult to state with any amount of accuracy when the reaction began or when the transformation reaction ended since the curves are asymptotic to the beginning and end of reaction lines "A" "D" and "E" "C."

For the comparative study of the effect of molybdenum on the transformation rate of austenite in cast iron, data from three previously mentioned types of plots were used, namely, millamperes versus time, Log A_o/A_θ versus time, and logarithmic probability austenite transformed versus time.

In all cases, the data used in determining the various isothermal transformation rates were an average on these different samples from the same heat isothermally transformed under identical conditions.

After the transformation data had been taken for each set of three samples from each heat at each tem-

Fig. 19—Microstructure of isothermal transformation products of austenite at various temperatures. Cast irons held at austenizing temperature of 1600° F. for 30 min. and quenched in a salt bath until transformation was completed at temperatures as indicated. Mag.—1000×.

TABLE 11.—ROCKELL "C" HARDNESS OF TRANSFORMATION PRODUCTS AT VARIOUS TEMPERATURES

Temperature °F.	Heat No. 355	356	357	358	359
400	50	48	50	51	52
500	44	43	46	46	48
600	41	40	40	39	43
700	32	30	30	31	37
800	29	28	29	30	34
900	26	26	25	29	32
1000	32	25	26	28	34
1100	29	29	29	29	31
1200	25	22	25	24	24
1300	10	8	10	11	12

Each hardness value is an average of three hardness tests made on three different samples isothermally transformed for the same length of time at the same temperature.

perature of study and the transformation plots drawn, the beginning and end point of each transformation were determined.

The logarithmic probability plot was used in extrapolation for an arbitrary determination of the beginning and end of austenite transformation. In all cases, the beginning point of each reaction was taken at 0.5 per cent and the reaction considered to be completed at 99.5 per cent.

In Table 10 are recorded the beginning and end point for each of the isothermal transformation reactions studied at 100 F intervals from 400 F to 1300 F. The data from Table 10 were plotted on a semi-logarithmic scale and are shown in Figs. 13, 14, 15, 16 and 17.

The isothermal transformation curves as determined for the cast irons studied are different from any published to date in that they do not take the form of simple "S" curves, but are made up of a series of continuous "S" curves. Fig. 19 contains a series of photomicrographs taken at 1000 diameters of the typical transformation product at each temperature. The transformation product at a definite temperature (500 F) was found to be the same for each of the heats studied, and for that reason only one photomicrograph showing representative microstructures for each temperature was included in making up Fig. 19.

At 400 F the product of transformation as shown in "A," Fig. 19 is made up of a series of black etching needles which are termed acicular troostite. The products at 500 F and 600 F were also found to be acicular, but they had a tendency to become coarse as the temperature of transformation was increased from 400 F to 600 F.

In the product at 600 F ("C," Fig. 19) there is some acicular ferrite also present in addition to the black needles. As the transformation temperature was increased from 600 F to 1000 F, the acicular ferrite plates became coarse and assumed a somewhat massive appearance.

Photos "G," "H," "J," and "K" of Fig. 19 show that the somewhat nodular back-etching constituent first appeared at 1000 F and that at 1100 F a lamellar form began to appear. As the transformation temperature was raised from 1100 to 1200 F the lamellar pearlite was more easily resolved at 1000 diameters and at 1300 F a fully lamellar pearlite appeared as the transformation product.

The pearlite, formed at 1300 F, is coarse when contrasted with the pearlite that was ordinarily formed when the cast iron cooled slowly through the freezing range and down to room temperature.

Hardness determinations were carried out on each of the samples isothermally transformed. An average value for each set of hardnesses measured is recorded in Table 11. In all cases five hardness determinations were carried out on each sample isothermally transformed, and a numerical average taken as the representative hardness value.

The hardness values recorded in Table 11 are the average of three hardness values from three identically transformed samples of cast iron. The data in Table 11 is plotted in Figure 18. Each of the hardness curves is characterized by having a dip at either 900 F or 1000 F which can be explained by referring to the photomicrographs in Fig. 19.

The hardness curves from Heats No. 355 and 359 indicate that at 900 F the first minimum in hardness is reached, at 1000 F the hardness again increases, and then beginning with 1100 F and up to the critical temperature the hardness progressively decreases. The minimum hardness at 900 F is thought due to the formation of the coarse acicular ferrite which is the transformation product at that temperature. The increase in hardness which occurred at 1000 F is presumably caused by the formation of the black-etching constituent which is thought to be an extremely fine carbide ("H," Fig. 19).

In studying hardness curves from Heats 356 and 358 (Fig. 20), it is evident that the first minimum hardness occurs at 1000 F. The hardness increases as the transformation temperature is raised 100 F and then a further increase in the transformation temperature up to the critical causes a lowering of the hardness of the transformation products.

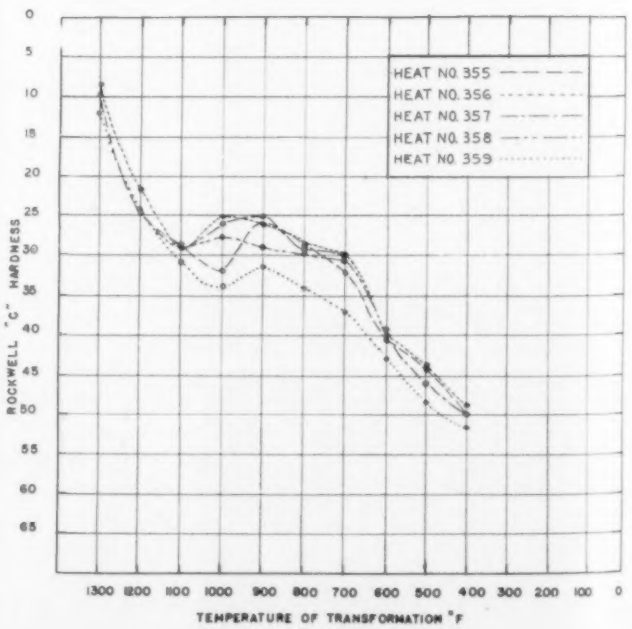


Fig. 20—Rockwell C hardness of completed transformation products.

The minimum hardness of the transformation product at 1000 F is thought due to the predominance of the acicular ferrite and the small amount of the dark etching constituent. At 1100 F there appears to be more of the dark etching constituent (primary troostite) than acicular ferrite, and therefore the hardness of the transformation product is higher than that of the 1000 F product.

The decrease in hardness from 1100 F up to 1300 F is caused by the carbides which form coarse well shaped plates of pearlite. When the carbide is in an extremely fine form, the hardness is much higher than if the carbides are arranged in well formed plates alternated with ferrite as in pearlite.

The hardness curve from Heat No. 357 is somewhat different from Heats No. 355 and 359 in that the hardness after the dip increases as the transformation temperature is increased up to 1100 F and then decreases as the temperature of reaction is increased up to 1300 F.

The explanation is much the same as in the case of the other heats and is dependent on the arrangement and the predominance of the acicular of the nodular constituent. The higher hardness at 1100 F over that of the 1000 F hardness is explained possibly by the fact that the sample had the ideal distribution of the small amount of acicular ferrite and the dark etching constituent which did not tend to revert to the lamellar form.

The dip in the hardness curve of the isothermal transformation product of cast irons in the alloyed and unalloyed condition was previously shown by a number of earlier investigators.

Figure 20 is a composite of all of the isothermal transformation curves for the unalloyed and alloyed cast irons investigated. In discussing the effect of molybdenum on the rate of isothermal transformation rate of cast iron at temperatures from 400 F to 600 F it may be stated that the molybdenum tends to delay the transformation somewhat, but to no appreciable extent.

This point was first suggested by Oakley and Oesterle.[4] In their conclusion they stated "0.5 per cent of molybdenum has no great effect upon the reaction rate of this cast iron except at the lowest temperature studied." This conclusion was confirmed in this investigation carried out on four molybdenum cast irons of various compositions within the chemical range studied by Oakley and Oesterle.

Over a temperature range of 900 F to 1300 F, the addition of increasing amounts of molybdenum slows down the transformation rate of the austenite. In other words, the alloy addition of molybdenum allows for deep hardenability of thick-section sizes. Deep hardenability is made possible by the fact that molybdenum tends to stabilize the austenite and allows the section to be quenched to the hardening temperature before any transformation takes place.

In comparing transformation curves, the most significant part to be considered is the portion of the curve which refers to the end of the reaction. In all cases, the completion of reaction curves have the same general shape. This tends to indicate that as the amount of molybdenum addition to the cast iron is increased, the transformation rate is slowed down. This is substantiated by the end of reaction curves being shifted to the right (Fig. 20).

There is some variation in the appearance of the beginning part of the transformation curves, but in most cases the variation is within a magnitude of about 20 to 30 sec. This is not appreciable considering that the time for transformation is many times 20 or 30 sec., disregarding the time necessary for transformation at 1100 F and 1200 F. At this temperature range the transformation of austenite to its decomposition product proceeds rapidly.

Conclusions

The addition of molybdenum in varying amounts up to 0.50 per cent in cast iron has little, if any effect, upon isothermal transformation rates in the temperature range from 400 F to 700 F.

The addition of molybdenum in varying amounts up to 0.50 per cent does not increase hardenability when the cast irons are isothermally transformed in temperature ranges from 400 F to 700 F.

The addition of molybdenum slows down the transformation rate in cast iron at temperatures from 700 F up to the lower critical.

The addition of molybdenum up to 0.50 per cent does not have any effect on refinement of the grain size of a Class B transverse bar as determined by fracture.

Hardenability, as determined by the end-quench type test, correlates well with hardenability as determined on a one-inch round section by quenching.

Austenite transformation rates determined magnetically correlate quite well with the transformation rates as determined metallographically.

The product of transformation in molybdenum cast iron at 800 F is acicular ferrite ("E," Fig. 19).

The dip in the hardness versus transformation temperature curves (Fig. 20) can be explained on the basis that the transformation product at 1000-1100 F is a fine pearlite, the acicular ferrite being relatively softer than fine pearlite.

The isothermal transformation curve for cast iron is not the typical "S" curve as formerly postulated, but is a series of "S" curves as shown.

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DISCUSSION

Chairman: H. BORNSTEIN, Deere & Co., Moline, Ill.

Co-Chairman: J. E. BOWEN, Chevrolet Gray Iron Foundry, General Motors Corp., Saginaw, Mich.

D. F. LUNSFORD:[1] Has the author tried, or does he believe it

[1] Perfect Circle Corporation, Hagerstown, Ind.

possible, to produce a pearlitic structure from an initial martensitic structure by isothermal heat treatment?

DR. NAGLER: In our studies, we did not attempt any tests of that type.

W. E. JOMINY:[2] The authors are to be congratulated on the work they did on cast iron. I was interested in the effect of combined carbon on hardenability and I wonder whether in addition to the readings made on the end-cooled bar, microstructural examinations were included in the study? Hardness readings as such do not mean too much if you do not know the carbon content of the steel. The percentage of martensite is rather the important thing in trying to determine how much hardenability you have. If you have an 0.80 per cent carbon steel, Rockwell C65 hardness is good but C40 is not. If you have an 0.20 per cent carbon steel, Rockwell C40 is a good hardness. In the case of the 0.80 per cent carbon steel, C40 corresponds to less than 50 per cent martensite, while with the 0.20 per cent carbon steel the same hardness indicates 95 per cent martensite. So it would seem to me that the hardenability of cast iron would be determined a great deal by how much combined carbon you have in the hardenability bars. I did not see that in the figures and believe it would add something to the value of the paper if we knew about the combined carbon.

DR. NAGLER: Referring to Table 2, you will find combined carbon content given for the cast irons used in the investigation. Unfortunately, in going over the research, our prime interest was to determine austenite transformation rates for the molybdenum cast irons. As a side investigation some of the hardenability tests were carried out. We did not run microscopic studies to determine the per cent of martensite present in the bars hardened by the end-quench test.

[2] Chrysler Corp., Detroit.

INFLUENCE OF SELENIUM ON SULPHIDE INCLUSIONS AND DUCTILITY OF CAST STEEL

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Research Laboratory
The International Nickel Co., Inc.
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ABSTRACT

In addition to refining the grain, selenium has the specific ability to coalesce the intergranular sulphides in low-oxide, well-killed cast steel, and thereby to improve its ductility. A demonstration of this phenomenon under a variety of conditions, as well as a theory for the mechanism of its occurrence, are discussed. A deoxidation practice for cast steel utilizing selenium is proposed and consists of the ladle addition of 0.08 per cent calcium and 0.05 per cent selenium.

Extensive tests in laboratory and commercial heats show that calcium selenium promoted high ductility in steels ranging from 75,000 to 180,000 psi tensile strength and melted in acid electric, basic electric, and basic open-hearth furnaces. Calcium selenium promoted better ductility and was less likely to produce heats of low ductility than other methods of deoxidation. Moreover, it appeared to accommodate a broad latitude in melting conditions and therefore should assist in the consistent production of high quality cast steel.

THE PROBLEM of obtaining globular, randomly dispersed sulphides in well-killed cast steels has been recognized for years. Low iron oxide contents which favor freedom from blowholes during solidification tend to promote thin, intergranular sulphides detrimental to ductility. High iron oxide contents, on the other hand, promote globular, randomly dispersed sulphides (a requirement for high ductility), but also increase the susceptibility to gassiness. Consequently, it is difficult to produce sound, ductile cast steel not deoxidized in any special manner, because its quality is partly dependent on the degree of oxidation at the time the steel is poured. This factor cannot readily be determined.

Sims[1] has proposed the use of large quantities of aluminum to combine freedom from porosity with high ductility, while Gagnebin[2] has suggested a combination of calcium silicide and aluminum for the same purpose. Both methods, however, require proper melting technique, including a vigorous boil to insure high quality steel. No deoxidation method yet proposed can consistently control the sulphide form in well-killed steel, regardless of the melting technique used to produce the steel.

This paper presents a new deoxidation procedure involving the use of selenium, which has been found to promote sulphide coalescence under a variety of conditions. A demonstration of this phenomenon, the mechanism of its occurrence, and tests of the proposed deoxidation procedure in both laboratory and commercial heats, are discussed.

Selenium Promotes Agglomeration

An effective demonstration of the influence of selenium on the ductility and sulphide form of a high sulphur, nickel-manganese steel, normalized and tempered is shown in Fig. 1. This heat was fractioned to show successively the influence of oxidation, aluminum deoxidation and selenium additions.

Aluminum destroyed the globular sulphides and ductility of the oxidized state, while 0.05 per cent selenium partially coalesced the sulphides, and 0.15 per cent selenium fully agglomerated them and restored the original ductility. The effect of progressive additions of selenium on the mechanical properties of aluminum-killed steel containing a normal amount of sulphur is shown in Fig. 2.

Mechanism of Sulphide Agglomeration

When selenium is added to steel, it appears to form manganese selenides which absorb the sulphides in the steel and then promote their rejection from solution before solidification is complete. The manganese or manganese-iron selenides are of medium gray color and appear to be miscible in all proportions with manganese sulphides, as no separation of the phases was noted. Figure 3 shows a typical manganese selenide-manganese sulphide inclusion in an aluminum-killed steel. The black angular constituent was absent in aluminum-free steel and therefore may be aluminum sulphide, since it varied directly with the amount of aluminum used for deoxidation.

Evidence bearing on the temperature at which sulphides were rejected from steel was developed by noting the sulphide location relative to the primary dendrite bodies and by heating steels to incipient melting to determine how much burning occurred before the sulphides disappeared. Both examinations were conducted on specimens suspended in an induction coil in a location to provide a thermal gradient, and then heated until one end melted, thus providing all degrees of overheating in a single specimen.

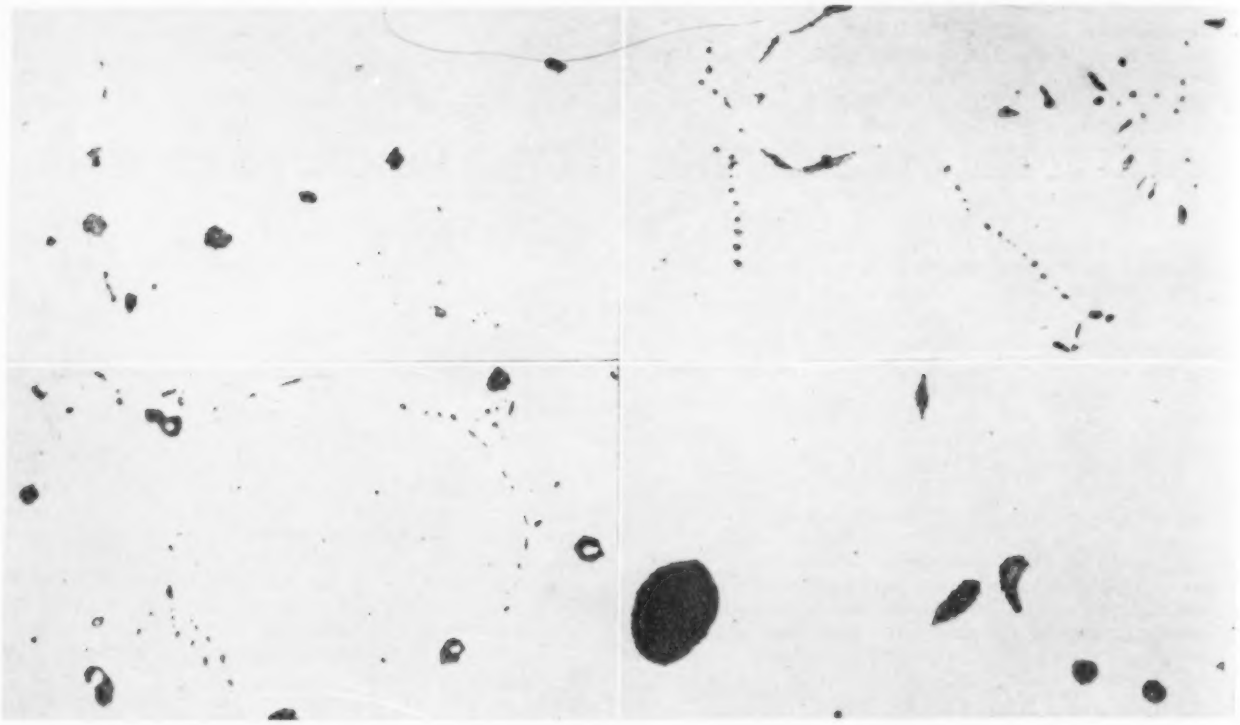


Fig. 1—The sulphide form and ductility in a Nickel-Manganese Cast Steel containing 0.07 per cent sulphur and fractioned to show the influence of various ladle additions. X500, unetched. Top, Left—Partly oxidized; reduction in area 45.0 per cent. Top, Right—0.06 per cent Aluminum added; reduction in area 16.0 per cent. Bottom, Left—0.06 per cent Aluminum and 0.05 Selenium added; reduction in area 15.0 per cent. Bottom, Right—0.06 per cent Aluminum and 0.15 Selenium added; reduction in area 47.0 per cent.

TABLE 1.—COMPARISON OF INFLUENCE OF SELENIUM ALONE VS. PARTIAL REPLACEMENT BY CALCIUM ON THE DUCTILITY OF CAST STEEL

Nominal Composition:	C	Ni	Mn	Si	S
	0.3	1.5	1.5	0.35	0.04

Heat Treatment: Normalized and Drawn.

Mark	Ladle Addition, Percent Al	Ca*	Se	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent
12261C	0.06	—	0.08	61	88	27.7	52.2
D	0.06	—	0.10	60	84	30.5	60.1
12487A	0.06	—	—	67	90	21.0	34.7
B	0.06	0.10	—	65	94	23.0	44.3
C	0.06	0.10	0.02	62	93	23.5	52.5
12437B	0.06	—	0.02	66	91	20.0	34.1
C	0.06	0.10	0.02	67	96	24.2	49.7
12289	0.06	0.10	0.02	64	89	28.0	58.3

*Added as CaSi.

Microspecimens from longitudinal sections of two steels, representing a brittle steel with intergranular sulphides and a ductile steel with coarse, manganese selenide sulphides, are shown in Figs. 4 and 5. These specimens were etched with a copper chloride etchant* similar to Stead's reagent in order to reveal the dendritic pattern of solidification.

*Dendritic etch developed by R. A. Flinn, 20 gr. $CuCl_2$, 50 cc. 12N HCl, 100 cc. C_2H_5OH, 250 cc. H_2O

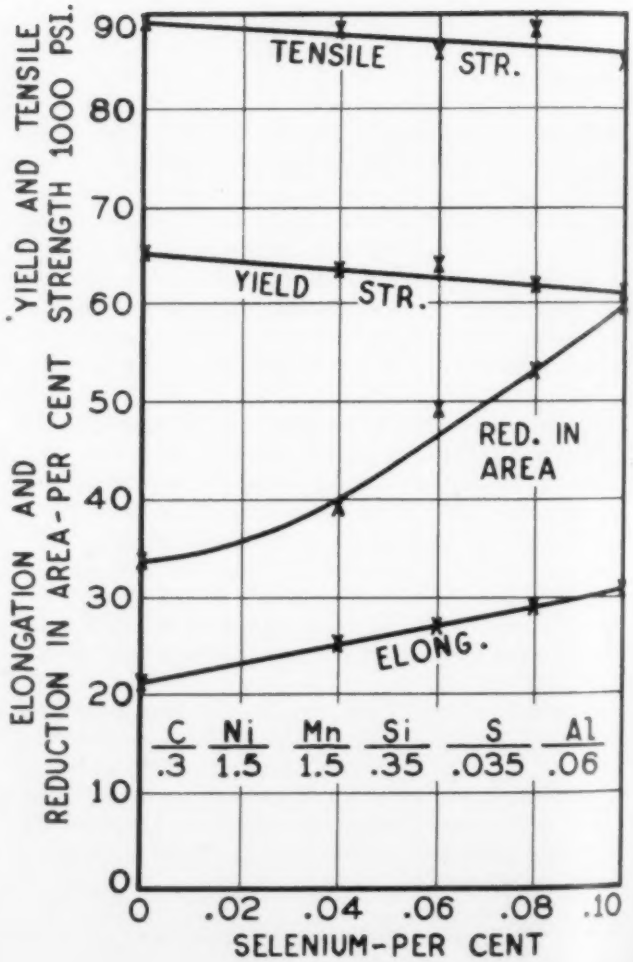


Fig. 2—The influence of Selenium on the mechanical properties of Aluminum-Killed Nickel-Manganese Cast Steel.



Fig. 3—Complex Sulphide containing Selenium in Cast Steel finished with 0.12 per cent Aluminum, 0.10 per cent Selenium. X1000.

In the steel of low ductility, the sulphides were located centrally in the inter-dendritic filling, and it was in this zone that melting was initiated. The dendritic pattern of the selenium steel was not as sharply defined as that of the brittle steel, and the sulphides usually were found at the edge of the inter-dendritic filling adjoining a dendrite body. Melting in this steel also was initiated at the center zone of the inter-dendritic filling, so that considerable melting occurred without affecting the sulphides containing selenium.

TABLE 2.—DUCTILITY OF CAST STEEL WITH TITANIUM AND ZIRCONIUM PARTIALLY REPLACING SELENIUM

Nominal Composition:	C	Ni	Mn	Si	S
	0.3	1.5	1.5	0.35	0.04

Heat Treatment: Normalized and Drawn.

Mark	Ladle Addition, Percent Al	Se	Other	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong. percent	Red. in Area, percent
12661	0.08	0.10	0.10 Zr	69	91	22.5	37.2
12560	0	0.03	0.10 Zr	65	87	22.5	34.4
12605A	0.08	—	0.10 Ti	57	100	19.5	40.3
B	0.08	0.02	0.10 Ti	53	109	19.5	29.9
12561	0	0.03	0.05 Ti	68	87	23.0	38.2
12562	0	0.03	0.10 Ti	72	96	20.5	33.5

Thus it appears that intergranular sulphides are rejected from the last remaining liquid during the solidification of steel and consequently weaken these already highly segregated zones. Selenium appears to associate with the sulphides and to promote their rejection from solution at a relatively early stage in solidification. Therefore, there is time for coalescence in the remaining liquid and opportunity for deposition in locations less damaging than the center of the highly segregated zones.

It appeared from the tests that it was necessary to



Fig. 4—Initiation and progression of burning in a Nickel-Manganese Cast Steel of low ductility containing intergranular Sulphides. X250. Dendritic etch.

Fig. 5—Initiation and progression of burning in a Nickel-Manganese, Selenium-Containing Cast Steel of high ductility. X250. Dendritic etch.

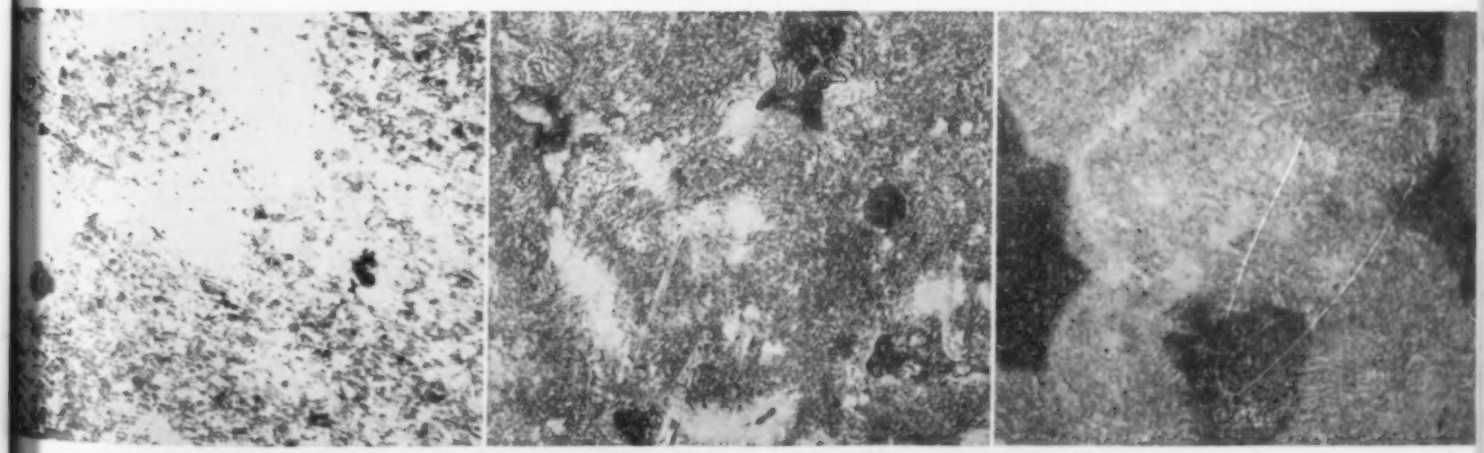


TABLE 3.—INFLUENCE OF CALCIUM SELENIUM ON DUCTILITY OF A VARIETY OF INDUCTION MELTED CAST STEEL COMPOSITIONS FINISHED WITH 0.02 PER CENT ALUMINUM

Type	Composition, percent								Heat Treatment, of (1 hr. each Air Cool)	Ladle Additions percent			Yield Str., 1000 psi	Tens Str., 1000 psi	Elong., per cent	Red. in Area, per cent	Charpy Vee Notch, ft. lb.
	C	Ni	Mn	Si	Mo	Cr	V	Ti		Al	Ca	Se					
Carbon	0.3		0.75	0.35					1650-1200	0.02			49	72	21.5	29.2	21
	0.3		0.75	0.35						0.02	0.05	0.03	49	73	30.0	50.0	42
Nickel Manganese	0.3	1.5	1.5	0.35					1600-1200	0.02			66	93	18.5	28.5	19
	0.3	1.5	1.5	0.35						0.02	0.05	0.03	67	93	27.5	50.0	38
Nickel Molybdenum	0.3	1.7	0.75	0.35	0.25				1600-1200	0.02			68	88	18.5	26.8	21
	0.3	1.7	0.75	0.35	0.25					0.02	0.05	0.03	62	87	26.5	52.5	39
Nickel Chromium	0.35	1.5	0.75	0.35		0.75			1600-1200	0.02			73	103	13.5	21.7	16
	0.35	1.5	0.75	0.35		0.75				0.02	0.05	0.03	71	100	23.0	48.5	36
Nickel Vanadium	0.3	1.5	0.75	0.35			0.10		1600-1200	0.02			73	88	17.5	24.1	16
	0.3	1.5	0.75	0.35			0.10			0.02	0.05	0.03	70	86	29.5	53.9	48
Nickel Silicon	0.22	1.5	0.9	1.2					1675-1000	0.02			67	88	25.0	36.4	
	0.22	1.5	0.9	1.2						0.02	0.05	0.03	64	88	28.0	51.4	
Manganese Titanium	0.3		1.6	0.35				0.1	1600-1200				67	94	19.0	32.5	
	0.3		1.6	0.35				0.1			0.05	0.03	66	93	22.0	36.9	

NOTE: S and P ranged from 0.03-0.04 percent.

TABLE 4.—THE INFLUENCE OF CALCIUM SELENIUM DEOXIDATION ON THE DUCTILITY OF NICKEL MANGANESE DIRECT ARC FURNACE CAST STEEL

Heat Treatment—Normalized and Drawn

Mark	Composition, percent						Ladle Additions percent			Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent
	C	Ni	Mn	Si	S	P	Al	Ca	Se				
13212B	0.22	1.39	1.40	0.23	0.035	0.06	0.04			70	87	25.0	39.4
13212A							0.02	0.07	0.04	72	90	27.2	51.4
13223-1	0.24	1.24	1.01	0.38	0.044	0.035	0.025			65	86	22.5	30.2
13223-10							0.02		0.10	61	82	29.0	50.6
13223-14								0.08	0.04	61	84	30.5	58.1
13202-A	0.30	1.27	1.35	0.25			0.025			66	91	22.0	36.0
13202-B							0.025	0.05	0.03	69	94	25.5	51.4

TABLE 5.—INFLUENCE OF LADLE ADDITIONS ON MECHANICAL PROPERTIES OF CAST STEELS IN HEAVY SECTIONS*

Type	Composition, percent							Ladle Additions, percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent	Charpy Vee Notch, ft. lb.
	C	Ni	Mn	Si	P	S	V						
Nickel Manganese	0.33	1.53	1.52	0.41	0.03†	0.03†		None	56	90	12.5	17.4	14
	0.29	1.52	1.45	0.33	0.033	0.026		Heavily Oxidized	56	84	24.5	37.9	34
	0.30	1.52	1.48	0.32	0.035†	0.025†		0.1Se	59	86	23.7	40.0	
	0.3†	1.5†	1.5†	0.35†	0.03†	0.025†		0.2Se	58	87	21.0	32.5	
	0.3†	1.5†	1.5†	0.35†	0.03†	0.025†		0.1Ca, 0.1Se	57	86	25.0	43.7	33
	0.3†	1.5†	1.5†	0.35†	0.03†	0.025†		0 06Al	64	72	4.5	7.4	17
								0.06Al, 0.1Ca	66	84	9.7	14.5	19
								0.06Al, 0.1Se	67	87	22.0	30.9	41
Nickel Silicon	0.2†	1.5†	0.7†	1.3†	0.03†	0.025†		None	57	90	9.0	—	
	0.2†	1.5†	0.7†	1.3†	0.03†	0.025†		0.1Se	62	84	28.0	46.0	21
Nickel Vanadium	0.3†	1.5†	0.7†	0.35†	0.03†	0.025†	0.1†	None	66	84	15.5	22.7	
	0.3†	1.5†	0.7†	0.35†	0.03†	0.025†	0.1†	0.05Ca, 0.03Se	65	84	24.7	41.3	40

*Heavy Sections were 3 in. keel molds with the tensile bar taken from center of section.

NOTES[2]: †Indicates estimated analysis.

All Heat Treatment: 1900°F; 1600°F; 1200°F; 3 hrs. each, air cooled.

use about 0.1 per cent of high-priced selenium to obtain sulphide coalescence in steels of normal sulphur. Accordingly, the problem was presented of discovering whether the amount of selenium required could be reduced, and whether this could be done by employing a combination of one or more other elements with selenium. As a result of experimentation, it was discovered that calcium could be employed together with smaller amounts of selenium to produce the desired results.

Table 1 shows some of the satisfactory results of using calcium, added as calcium silicide. Thus, it will be observed from Table 1 that 0.1 per cent calcium and 0.02

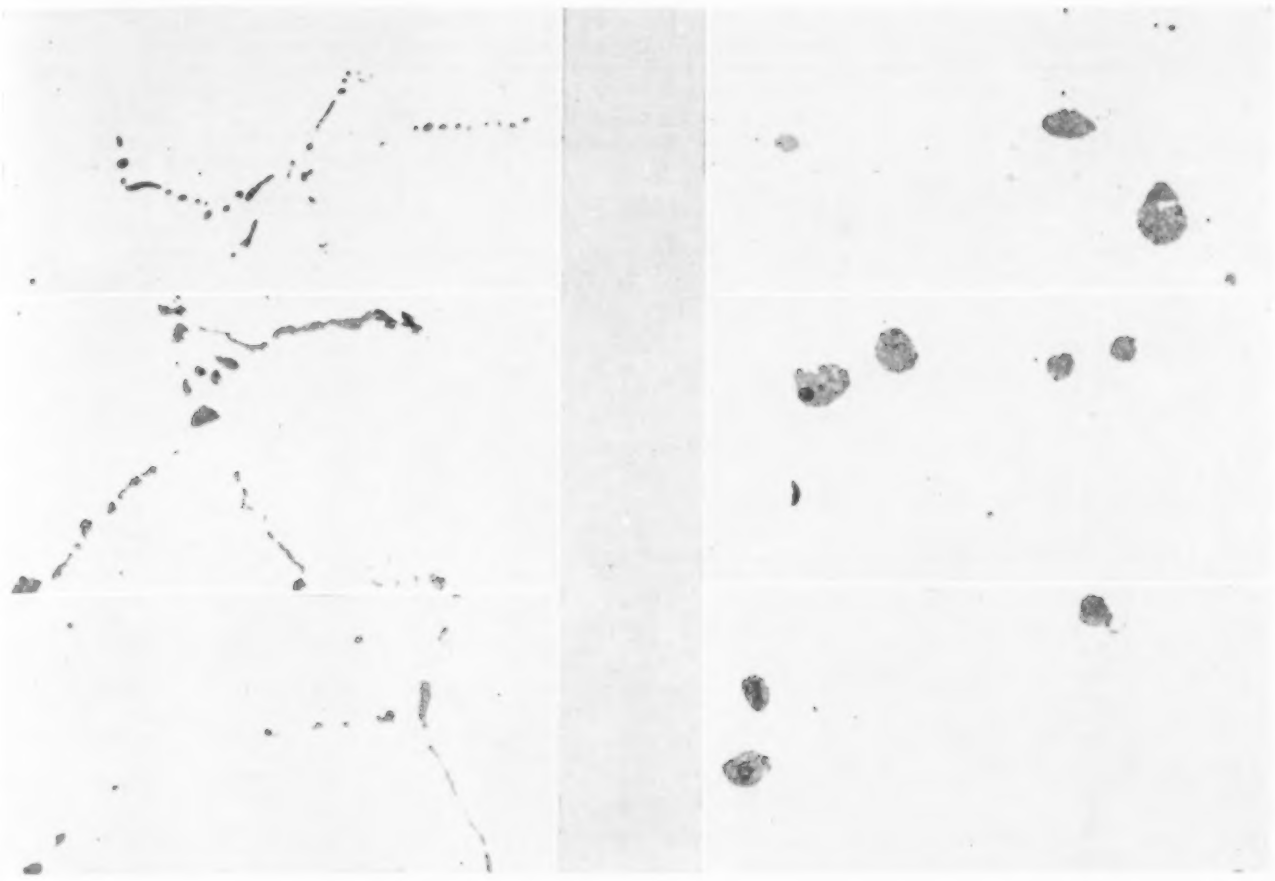


Fig. 6—Sulphide Coalescence promoted by Calcium Selenium in various Cast Steels. X500.
Top—Nickel-Vanadium Steel, induction furnace heat. (Left)—0.02 per cent Aluminum. (Right)—0.02 per cent Aluminum, 0.05 per cent Calcium, 0.03 per cent Selenium.
Center—Nickel-Molybdenum Steel, induction furnace heat. (Left)—0.02 per cent Aluminum. (Right)—0.02 per cent Aluminum, 0.05 per cent Calcium, 0.03 per cent Selenium.
Bottom—Nickel-Manganese Steel, arc furnace heat. (Left)—0.02 per cent Aluminum. (Right)—0.02 per cent Aluminum, 0.05 per cent Calcium, 0.03 per cent Selenium.

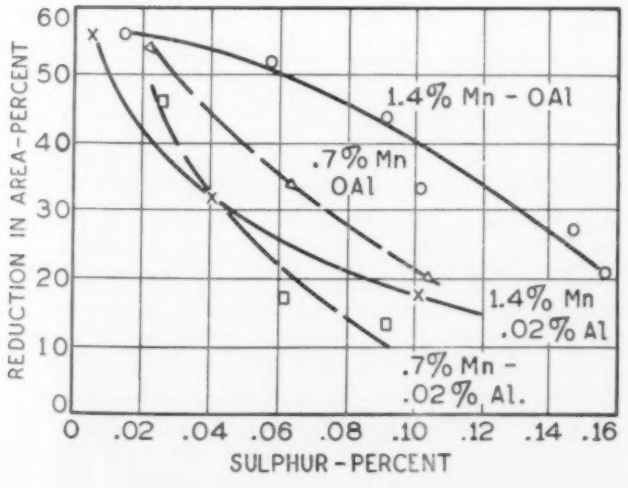


Fig. 7—Relation between Manganese, Sulphur, and Ductility in Nickel Cast Steel finished with 0.05 per cent Calcium, 0.03 per cent Selenium.

per cent selenium were as effective as 0.1 per cent selenium alone for improving the ductility of aluminum-killed steel.

It is evident also that, in these quantities, both elements are necessary for high ductility. A micro-examination showed that calcium shortened and thickened the sulphide links of aluminum-killed steel and that selenium provided the stimulus for complete agglomeration. While it is possible to combine calcium and aluminum without selenium for high ductility,[2] the calcium must precede the aluminum addition and then will function only in an oxidized heat. Only with selenium does it appear possible to agglomerate the sulphides in a steel otherwise destined to have the intergranular type.

Calcium-Selenium Deoxidation Tested

Two other sulphide forming elements, titanium and zirconium, did not appear to replace part of the selenium successfully, and the results of these tests appear in Table 2. These steels had low ductility even when no aluminum was used, and they contained intergranular sulphides.

This deoxidation procedure was tested in a variety

TABLE 6.—MECHANICAL PROPERTIES OF SELENIUM CONTAINING STEELS PRODUCED IN SEVEN COMMERCIAL FOUNDRIES

FOUNDRY "A"

Furnace: Basic Open Hearth
Heat Treatment: Normalized and Drawn

Heat No.	Ladle Addition, percent			Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent	Composition, per cent C	Mn	Si	P	S
								Aluminum Titanium				
	Al	Ti										
1	0.025	0.045		74	104	27.5	50.3	0.37	1.34	0.47	0.028	0.021
2	0.025	0.045		67	95	24.5	44.9	0.36	1.42	0.43	0.032	0.024
				64	92	25.0	46.6					
3	0.05	0.045		67	94	25.8	49.7	0.36	1.63	0.47	0.042	0.026
				67	92	27.3	50.3					
								Calcium Aluminum Titanium				
	Al	Ca	Ti									
4	0.037	0.063	0.045	69	100	17.3	26.2	0.38	1.60	0.47	0.034	0.025
				69	101	20.0	33.8					
5	0.037	0.063	0.045	66	91	8.7	13.4	0.37	1.74	0.50	0.018	0.020
				65	88	7.5	7.8					
6	0.037	0.063	0.045	67	99	19.0	28.5	0.39	1.60	0.50	0.026	0.021
				67	99	21.8	32.5					
7	0.05	0.063	0.045	59	95	23.5	53.6	0.39	1.45	0.56	0.023	0.021
				63	95	28.0	54.7					
8	0.05	0.063	0.045	63	93	28.5	52.5	0.36	1.54	0.57	0.027	0.019
				65	93	26.5	50.9					
9	0.05	0.063	0.045	66	95	28.0	47.8	0.34	1.51	0.43	0.028	0.024
				—	94	28.5	48.0					
10	0.05	0.063	0.045	70	97	22.5	36.0	0.35	1.53	0.53	0.037	0.014
				70	96	25.5	45.4					
								Calcium Aluminum Selenium				
	Al	Ca	Se									
11	0.05	0.095	0.05	61	93	30.0	56.3	0.33	1.33	0.49	0.028	0.018
				62	93	29.2	54.1					
12	0.05	0.063	0.05	59	90	27.3	51.4	0.29	1.45	0.52	0.029	0.019
13	0.062	0.063	0.05	67	96	22.0	33.5	0.37	1.56	0.46	0.038	0.024
						No test—holes in coupon						
14	0.062	0.063	0.05	67	97	24.5	47.5	0.38	1.68	0.50	0.025	0.023
				67	98	24.5	44.6					
15	0.062	0.063	0.05	68	97	27.0	52.5	0.33	1.50	0.50	0.035	0.024
				67	96	27.0	49.5					
16	0.062	0.095	0.05	62	90	28.5	53.3	0.31	1.57	0.55	0.029	0.023
				60	89	28.5	53.3					
17	0.062	0.095	0.05	60	89	29.0	53.6	0.33	1.60	0.31	0.011	0.024
18	0.062	0.095	0.05	62	92	28.5	52.8	0.32	1.38	0.29	0.016	0.022
				61	92	28.5	52.8					

TABLE 6.—*Continued*

FOUNDRY "B"

Furnace: Acid Electric.
Nominal Composition: C 0.35 0.45 Mn 1.4 1.6 Mo 0.45 0.55 Si 0.3 0.5
Heat Treatment: Double Normalized and Drawn.

Heat No.	Ladle Addition, Percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent
1	Si	93	110	20.5	40.2
2	Si	90	111	17.0	28.2
3	0.037A1, 0.1Ca, 0.05Se	85	113	20.5	42.5
4	0.1Ca, 0.05Se	83	112	21.7	44.0
5	0.1Ca, 0.05Se	87	116	18.0	35.0
6	0.037A1, 0.1Ca, 0.05Se	83	111	20.5	37.0
	Desired Properties	80	100	18.0	25.0

TABLE 6.—*Continued*

FOUNDRY "C"

Furnace: Basic Electric.
Nominal Composition: C 0.30 Mn 1.5 Cr 0.45 Mo 0.35 Si 0.50
Heat Treatment: Homogenized, Quenched and Drawn.

Item No.	Ladle Addition, percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent	Izod Impact, ft. lb.	No. Heats Tested
1	0.08Ca, 0.05Se	143	152	15.0	36.8	30.8	5
2	0.25FeTi	146	155	13.9	28.7	36.5	4
3	0.08Ca	142	152	12.5	30.4	34.9	2
4	0.15FeTi, 0.05A1	134	148	12.0	28.7	31.6	5
5	0.05Ca, 0.10A1	136	148	11.2	23.8	34.4	7
6	0.10A1	134	146	11.3	24.0	27.4	12
7	—	129	138	10.0	17.2	31.3	1

TABLE 6.—*Continued*

FOUNDRY "D"

Furnace: Basic Electric.
Nominal Composition: C 0.35 .45, Cr 0.80 1.0, Mo 0.15 .20, Mn 0.70 .90, Si 0.30 .50
Heat Treatment: Homogenized, Oil Quenched and Drawn.

Heat No.	Ladle Addition, percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent
1	—.....	165	175	5.0	11.5
		172	185	4.5	9.0
2	0.1 mill scale...	155	165	7.0	11.5
		165	163	6.0	13.0
3	0.1 mill scale...	164	179	8.5	14.5
		165	177	6.5	11.0
4	0.1 Grainal.....	177	188	6.0	16.0
		163	170	7.5	19.1
5	0.1 Grainal.....	157	165	6.0	17.0
6	0.1 Grainal.....	183	191	5.0	12.5
		166	164	6.0	9.0
7	0.1 Silcaz.....	165	179	5.0	13.5
		157	179	7.5	17.5
8	0.1 Se.....	172	179	11.0	34.0
		177	185	13.0	34.0
9	0.1 Se.....	171	176	12.5	36.0
10	0.1 Se.....	178	183	6.0	13.0
		178	184	9.5	24.5
11	0.1 Se.....	164	169	10.5	32.5
		165	170	10.0	29.0
12	0.1 Se.....	168	176	8.5	17.0
		173	181	7.0	17.0
13	0.1 Se.....	172	183	11.5	28.0
		174	184	8.0	16.5
14	0.1 Se.....	180	186	7.0	9.5
		175	183	7.0	20.2
	Desired Properties		170-180	7.0 min.	12.0 min.

TABLE 6.—*Continued*

FOUNDRY "E"

Furnace: Acid Electric.
Composition: C 0.39, Cr 1.08, Mo 0.21, Mn 0.90, Si 0.47, P 0.036, S 0.033

Ladle Addition, percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent
Normalized, Quenched and Tempered				
Plain.....	170	185	5.6	15.7
0.10 Al.....	173	186	6.4	17.9
0.10 Se.....	170	182	9.5	26.9
0.20 Grainal.....	180	190	4.0	8.0
Homogenized, Normalized, Quenched and Tempered				
Plain.....	173	188	9.3	23.7
0.10 Al*.....	156	173	3.1	4.5
0.10 Se.....	170	186	11.0	31.2
0.20 Grainal*.....	177	193	5.0	10.8

All results are an average of 4 tests, except * which are an average of 2 tests.

of compositions finished with 0.02 per cent aluminum, recognized as promoting poor sulphide dispersion and low ductility in cast steel. The tensile results in Table 3, obtained on well-fed 1-in. sections, show that 0.05 per cent calcium and 0.03 per cent selenium substantially improved induction-furnace steels.

Elongation was improved and the reduction in area raised from 22-29 per cent in the aluminum-deoxidized steel, to over 50 per cent by the selenium treatment. In most cases the impact resistance was doubled. The only steel not improved by selenium was a manganese-titanium composition, which verifies the earlier obser-

TABLE 6.—*Continued*

FOUNDRY "F"

Furnace: Electric, type not reported.
Nominal Composition: C 0.35, Ni 1.3, Cr 0.8, Mo 0.3, Mn 0.8, Si 0.45, P 0.03, S 0.0

Heat No.	Ladle Addition, percent	Yield Str., 1000 psi	Tens. Str., 1000 psi	Elong., percent	Red. in Area, percent	Izod, ft. lb.
	Normalized, Oil Quenched and Tempered					
1	0.08Al, 0.057Se	131	145	10.5	18.1	13
	0.057Se	137	151	13.5	35.7	21
	0.033Ca, 0.057Se	137	151	13.0	32.4	20
	0.08Al, 0.025Ca	135	147	12.5	30.2	20
2	0.08Al, 0.057Se	152	165	5.0	11.0	9
	0.057Se	151	163	13.0	37.0	13
	0.033Ca, 0.057Se	156	165	11.0	29.0	14
	0.08Al, 0.025Ca	149	160	9.5	22.0	13

NOTE: Calcium added as CaSi.

TABLE 6.—*Continued*

FOUNDRY "G"

Furnace: Acid Electric.
Composition: C 0.23, Mn 0.64, Si 0.39, P 0.029, S 0.031
Heat Treatment: Normalized and Drawn.

Heat No.	Ladle Addition percent	Yield Str., 1000 psi	Tens, Str., 1000 psi	Elong., percent	Red. in Area, percent
1	0.025Al.....	51	75	29.0	45.0
	0.025Al, 0.05CA, 0.03Se	58	77	32.0	53.0
	0.025Al, 0.10Ca, 0.05Se	56	78	30.0	45.0
	Induction Furnace Heats: Normalized and Drawn.				
	Composition: C .32, Mn 1.39, Mo .24, Si .44, P .034, S .038				
2	—.....	—	116	18.5	
	0.10Al.....	89	107	20.0	
	0.10Al, 0.10Ca, 0.05Se..	90	107	20.0	42.
	Composition: C .25, Ni .20, Cr .59, Mn .74, Si .19, P .03, S .037				
3	—.....	92	111	18.5	52.0
	0.03Al, 0.10Ca, 0.06Se.	85	102	23.0	53.0

NOTE: All test coupons 1 in. to 1¼ in. section.

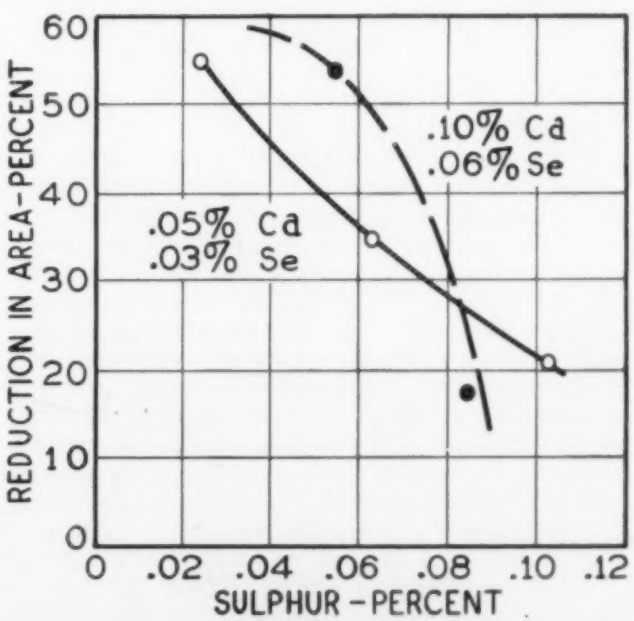


Fig. 8—Relation between Calcium Selenium, Sulphur, and Ductility of Nickel-Manganese Cast Steel.

vation that selenium cannot coalesce sulphides in the presence of titanium.

The results obtained in direct-arc-furnace heats, shown in Table 4, develop the same trend as the induction heats, although the ductility of aluminum-killed fractions is somewhat higher in the arc-furnace steels. A few examples of the sulphide coalescence provided by selenium in both types of steel are shown in Fig. 6.

Relation Between Manganese and Sulphur

It became evident during this study that manganese increased the tolerance for sulphur and that steels with high manganese content were more susceptible to improvement with calcium-selenium deoxidation than those with lower levels of manganese. The results in Fig. 7 demonstrate this point and indicate that greater ductility can be achieved with 1.4 per cent manganese than with 0.7 per cent, especially at sulphur levels above 0.04 per cent. The data also indicate that steels finished with 0.02 per cent aluminum were considerably more sensitive to high sulphur contents than aluminum-free steels.

An increase in calcium and selenium, from 0.05 per cent and 0.03 per cent to 0.10 per cent and 0.06 per cent, respectively, increased the tolerance for sulphur in low-manganese steels, as shown in Fig. 8. One point that should be recognized in connection with these data is that the ductility, especially at high sulphur levels, is probably not the maximum obtainable because of the production method. These heats were finished with calcium-selenium, and sulphur then was added to successive fractions. Oxidation losses occurring during this procedure undoubtedly reduced the ductility. The data demonstrate trends but cannot be used to predict the ductility of high sulphur steels.

Effectiveness in Heavy Sections

It is well known that increases in section size are detrimental to the properties of cast steel, particularly to ductility. Segregation and a greater susceptibility to poor sulphide dispersion contribute to deterioration of ductility. It was of interest, therefore, to make a few tests using a keel mold of 3-in. section to determine how well selenium could deal with the more severe conditions existing in heavy sections.

In order to appreciate the powerful effect of section size on cast steel it should be recognized that the nickel-manganese and nickel-vanadium steels in Table 5 which received no ladle treatment, would have developed at least 50 per cent reduction in area if cast in the regular test mold of 1-in. section. Selenium and calcium-selenium greatly improved the heavy section ductility of nickel-manganese steel and, in fact, improved it to the same extent as that achieved in the steel which was heavily oxidized. The latter was oxidized so heavily that it effervesced slightly during solidification. Aluminum sharply reduced ductility, which again was improved by selenium although not to the level achieved in the aluminum-free material.

Grain Refinement Promoted

Thus these few tests indicate that selenium is able to coalesce sulphides under the severe conditions existing in heavy sections, as shown in Fig. 9, and the author suggests that it should be especially useful in this connection.

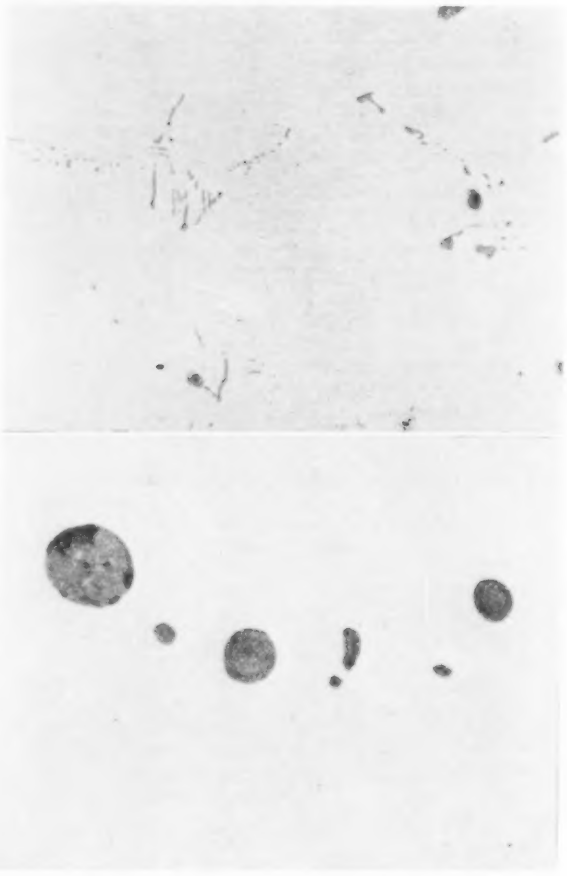


Fig. 9—Sulphide Coalescence by Selenium in 3-in. cast sections of Nickel-Manganese Steel. (Above)—As melted, X250. (Below)—0.1 per cent Selenium, X500.

Selenium refines the grain size of steel. Grain sizes of 6 or 7 were obtained in steels containing 0.1 per cent selenium, and the normalized grain size of a steel containing 0.05 per cent calcium and 0.03 per cent selenium is compared to a similar silicon-killed steel in Fig. 10. Thus the usual benefits of fine-grained steels, such as a high ratio of yield to tensile strength and good impact resistance, are obtained in selenium containing steels. Grain refinement usually is associated with strong carbide or oxide forming elements, and it was rather surprising to find that selenium, a weak deoxidizer, refined the grain in steel.

Tests with Commercial Steels

Selenium was tested in seven foundries in steels melted in acid and basic electric, basic open hearth, and induction furnaces. The steels ranged from plain carbon with tensile strengths of 75,000 psi to quenched alloy steels of 180,000 psi. Results are given in Table 6, which also includes results obtained on other deoxidation procedures that were available.

In Foundry A, only one of eight open-hearth heats finished with calcium selenium had low ductility, while three heats of seven representing another deoxidation method had low ductility. Moreover the selenium heat of low ductility was obviously of poor quality since

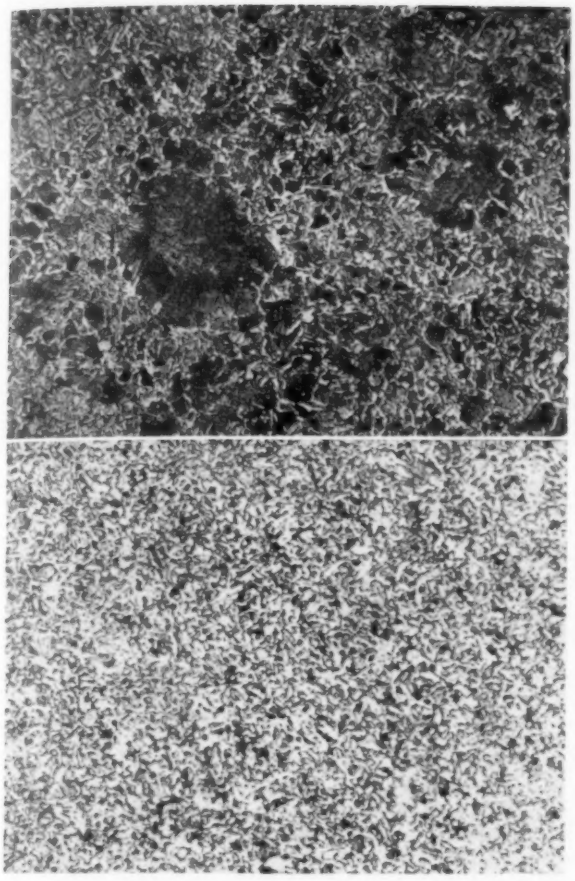


Fig. 10—Grain refinement promoted by Selenium in normalized Nickel-Manganese Steel. Heat treated 1 hr. at 1600 F. and air cooled. Micrographs at X100, nital etch. (Above)—No ladle addition. (Below)—Addition of 0.05 per cent Calcium, 0.03 per cent Selenium.

other test bars from it contained gas holes, which is unusual for a steel whose ladle additions included 0.06 per cent aluminum. All four of the selenium, acid-electric heats from Foundry B met the required properties, while one of the two heats deoxidized with silicon alone passed specification.

The tensile results on basic electric steels from Foundry C are significant because they were made under closely controlled melting conditions and the results for each deoxidation procedure are the average of several heats. Calcium-selenium deoxidation developed the best ductility and was superior to six other deoxidation methods.

Foundries D and E dealt with high-strength, quenched-and-drawn steels, although one used basic-electric and the other acid-electric furnaces. The selenium heats again showed better ductility and at the same time were more consistent in promoting ductility than steels finished in other ways.

Foundry F obtained good ductility with calcium-selenium steels of high strength, which was better than that developed by aluminum-selenium deoxidation. However, the amount of selenium used was too low and an increase from 0.05 per cent to 0.1 per cent probably would have developed high ductility with aluminum containing steels. Foundry G had no steels of low ductility regardless of the deoxidation procedure used, and consequently provided little opportunity for improvement via calcium selenium.

Methods and Amount of Additions

Selenium may be added to the tapped stream in lump form or as ferro-selenium. The recovery with pure selenium ranges from 65 per cent to 80 per cent while the ferroalloy has a greater recovery of 85 to 95 per cent. Somewhat more fuming occurs with the lump selenium, although the quantities used are so small that this is not considered an objectionable feature from a foundry point of view.

In these tests calcium always was added as calcium silicide. In using calcium silicide, care should be taken to assure that it is carried beneath the surface by the tapped stream, or else attached to a rod and submerged for its effective addition.

Calcium selenium is preferred to selenium alone for finishing cast steel because it is cheaper and as fully effective. The proper amounts seem to be between 0.05 per cent Ca–0.03 per cent Se and 0.10 per cent Ca–0.06 per cent Se, and depend on the sulphur content and another factor not clearly identified. For a long time it seemed certain that 0.05 per cent Ca–0.03 per cent Se were sufficient to promote high ductility under all circumstances. However, with one lot of melting stock that arrived at the laboratory when this study was nearly complete, 0.10 per cent Ca and 0.06 per cent Se were required for high ductility in aluminum-killed steel.

One reason for the greater amount of selenium required was that its recovery in the new melting stock was lower than it had been in the old. There were indications that the oxide content on melt down influenced the selenium recovery and that the more highly oxidized heats favored better recovery. This happened in spite of the fact that selenium was added after the aluminum and that the aluminum-killed specimens had the same low ductility regardless of the base metal used and the degree of oxidation on melt down.

The reasons for this behavior are not understood but are important only from an economic point of view, since good ductility could be obtained with all base materials. Consequently the amount of addition should be varied with a particular set of melting conditions to determine the minimum requirement. The quantities recommended for initial runs are 0.08 per cent calcium and 0.05 per cent selenium.

General Advantages

The comprehensive tests of selenium and calcium selenium in both laboratory and commercial melts develop two features of this addition agent. The ductility of selenium-containing steels is better than that of selenium-free steels, and selenium steels of low ductility occur less frequently than those deoxidized in other ways. From a commercial standpoint these features mean higher quality steel as well as fewer heats lost through failure to pass specification.

There appears to be little question that selenium steels are less sensitive to melting technique than those deoxidized in other ways. The fact that good ductility was achieved in different compositions melted in all

types of furnaces testifies to this thought. Such an advantage is important from an operating standpoint, since it allows wider latitude in furnace operation and, perhaps, requires less skill on the part of the melter. Although such an intangible factor cannot be accurately evaluated, it nevertheless is real and is a corollary to the fact that selenium-containing steels of low ductility occur less frequently than selenium-free steels.

Selenium increases the cost of deoxidation and the extent of its use probably depends on the quality of the steel desired, the specifications to be met and the frequency of low-ductility heats that can be tolerated.

Conclusions

1. Selenium has the specific ability to coalesce the intergranular sulphides in cast steel and thereby improve its ductility. In addition, it refines the grain and is a mild deoxidizer.

2. It appears to combine with the sulphides in steel and promote their rejection from solution before solidification is complete. Manganese-selenide-sulphides are located at the interface of the primary dendrites and the interdendritic filling, in contrast to inter-granular sulphides which are rejected from the last freezing liquid in the center of the inter-dendritic filling.

3. Calcium can be substituted for part of the selenium without sacrifice of the sulphide-coalescing ability, Zirconium and titanium did not successfully replace part of the selenium.

4. The addition of 0.05 per cent calcium and 0.03 per cent selenium approximately doubled the reduction in area and the toughness of a variety of experimental cast steels, with well defined intergranular sulphides intentionally produced by the addition of 0.02 per cent aluminum.

5. Selenium and calcium selenium coalesced the sulphides and greatly improved the ductility of cast steel in heavy sections. It should be especially useful in this connection, since the problem of obtaining ductility in cast steel is accentuated as the section size is increased.

6. Calcium-selenium finished steels have only slightly better ductility than silicon-killed steels at a given sulphur level. The important difference is that selenium promotes globular sulphides in over-killed steels of low oxide content, thus readily permitting the production of sound, ductile castings. Silicon-killed steels, on the other hand, require a relatively high oxide content for globular sulphides and are therefore susceptible to gassiness.

7. Manganese increased the tolerance for sulphur in calcium selenium steels, while aluminum reduced it.

8. Calcium selenium and selenium developed good ductility in a variety of commercial steels ranging from 75,000 psi to 180,000 psi tensile strength, melted in electric and open-hearth furnaces. The selenium-containing steels developed better ductility and, at the same time, fewer heats with low ductility than other methods of deoxidation.

9. Selenium appears to permit broad latitude in melting conditions and therefore should assist in the consistent production of high quality cast steel.

Acknowledgments

The author is indebted to Norman B. Pilling for helpful criticism and permission to publish the results; to John T. Eash for suggestions and cooperation during the investigation; and to the personnel of the foundries which conducted the tests on the commercial steels.

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[1] C. E. Sims and F. B. Dahle, *"Effect of Aluminum on the Properties of Medium Carbon Cast Steel,"* TRANSACTIONS A.F.A. (1938), vol. 46, p. 65.

[2] A. P. Gagnebin, *The Effect of Deoxidation Treatments on the Ductility of Cast Steels,"* TRANSACTIONS A.F.A. (1938) vol. 46, p. 133.

DISCUSSION

Chairman: C. W. BRIGGS, Steel Founders' Society of America, Cleveland

Co.Chairman: L. H. Hahn, Sivyer Steel Castings, Chicago

G. A. LILLIEQVIST [1]: I would like to congratulate Mr. Gagnebin on his splendid paper. We have done a great deal of work at our Research Laboratory with the use of selenium. We obtained such encouraging results with induction furnace heats that we decided to make several acid electric furnace heats as well as some basic open-hearth heats. In all cases we experimented with intermediate manganese steel containing carbon 0.30, manganese 1.65, and silicon 0.45. In practically all our experiments we obtained globular or Type I inclusions with resulting excellent ductility. On an average we obtained with this type of steel after a normalizing and tempering treatment a yield point of 67,000 psi, tensile strength 95,000 psi, elongation 27 per cent, and reduction of area 55 per cent. These results are satisfactory.

I would like to ask Mr. Gagnebin several questions and relate some experiences we had with these various selenium heats. In regard to Foundry D. Page 283, Ladle Addition of 0.1 per cent mill scale, what is the reason for adding this mill scale? How can you produce sound castings by adding mill scale?

MR. GAGNEBIN: I do not know the reason for the addition of mill scale as I did not witness the commercial tests.

MR. LILLIEQVIST: You mentioned the use of calcium selenium. Do you get sound castings in green sand molds by adding calcium selenium?

MR. GAGNEBIN: It is not as strong a deoxidizer as aluminum. With green sand I would think that aluminum would be necessary for sound castings.

MR. LILLIEQVIST: What difficulties did you encounter in welding the castings?

MR. GAGNEBIN: We ran no welding tests.

MR. LILLIEQVIST: I would like to caution everybody—and I do not mean to criticize this paper. We have made several heats in the acid-electric furnace as well as the basic open-hearth and induction furnace and when we came to repair welding of these castings, we had a difficult time.

MR. GAGNEBIN: Did that occur in castings containing selenium?

MR. LILLIEQVIST: Yes, we added 1½ lb. aluminum and 0.075 per cent selenium per ton, and it was impossible to weld these castings because of spattering and boiling of the weld metal. But as soon as we used lime-coated welding rods the welding operation was satisfactory. Hence, lime-coated welding rods should be used in repair welding selenium-bearing castings.

Another difficulty we ran into with selenium is in regard to nozzle troubles. We made an open-hearth heat using 1½ lb. of aluminum and 0.075 per cent selenium. We almost lost the heat, because the selenium apparently attacked the nozzle.

Another experience we had with selenium, was when we made about 25 heats in an acid-electric furnace with the addition of selenium. One day we got in a new shipmen of ferro-selenium which made the metal especially sluggish. We melted at the same temperature, around 3200° F. but just could not use the metal because of its sluggishness.

MR. GAGNEBIN: Did you ever find the reason for that?

MR. LILLIEQVIST: No. I have no idea where the sluggishness could come from. The first shipment of ferro-selenium was satis-

[1] American Steel Foundries, Indiana Harbor Works, East Chicago, Ind.

factory while the second was unsatisfactory from the same source. I wonder if you have any idea on that? What is the recovery of selenium?

MR. GAGNEBIN: I do not know. Most of our heats were made on such a small scale that we could not obtain reliable data to answer that question.

MR. LILLIEQVIST: We had an experience with an induction furnace. We made a 100-lb. induction furnace in the acid-lined furnace and we added again 1½ lb. of aluminum and 0.75 per cent selenium. We looked at the inclusions which were proper inclusions. Then we used the same scrap and melted it in and did not add any selenium at all but added 1½ lb. of aluminum and again we got the globular type of inclusion.

MR. GAGNEBIN: Did you analyze for selenium?

MR. LILLIEQVIST: Yes, we analyzed for selenium and recovered 96 per cent.

MR. GAGNEBIN: A high recovery of Se from scrap and its consistent effectiveness in dealing with sulphides would be a nice feature.

MR. LILLIEQVIST: You will have to segregate your scrap and that costs money.

CHAIRMAN BRIGGS: Mr. Lillieqvist, the author recommended the use of calcium with the selenium. Did you try that?

MR. LILLIEQVIST: No, we never used calcium. We used aluminum and ferro-selenium. I believe that calcium would reduce the amount of selenium.

CHAIRMAN BRIGGS: Mr. Gagnebin stated that he used 0.030 per cent selenium and you used 0.075 per cent. That may have some effect.

MR SIMS [2]: What is the solubility of selenium in steel?

MR. GAGNEBIN: I have not knowledge of its solubility in steel.

C. E. SIMS: There seems to be a close analogy between the effects of selenium and sulphur in many ways, but also some points of difference. I was interested in the author's description of the effect of selenium on grain size. It can be shown that sulphur has a similar effect on grain size. The selenides have a superficial appearance of sulphides and apparently selenium has a fairly high solubility in steel which must, however, reach saturation some time during freezing.

The eutectic between Fe and FeS contains about 7 per cent which explains why sulphides do not precipitate until the steel is largely frozen. The solubility of S in steel is apparently altered by other constituents. For example, the present of FeO causes early precipitation with the formation of large globules as compared to the eutectic formation when the FeO is eliminated with Al. On the other hand, excess Al will form Al_2S_3 which again reduces the solubility. Calcium seems to have a somewhat similar effect with the formation of some CaS.

Selenium obviously precipitates early enough to form large globules regardless of the state of deoxidation of the steel. Of more importance is that the sulphur precipitates on or with the selenides to form sulpho-selenides. Apparently there are no separate sulphides in steel treated with selenium.

The analogy to sulphur caries over into welding, for sulphur is potent in producing porous welds. Resulphurized steel usually must be welded with a mineral-coated weld-rod to obtain sound welds.

MR. LILLIEQVIST: Tellurium acts the same as selenium.

MR. GAGNEBIN: We thought it was not quite as effective.

MR. LILLIEQVIST: I found exactly the same results.

MR. GAGNEBIN: We did not test it extensively but the few trials we made indicated that it was not effective as selenium. This is in harmony with the fact that tellurium has an atomic weight about 50 per cent greater than that of selenium. A greater percentage weight of tellurium would be necessary to deal with a given sulphur content, assuming that both elements form the same type of compounds with sulphur.

MR. LILLIEQVIST: Where can you obtain calcium selenium?

MR. GAGNEBIN: There is no such alloy available. We used separate additions of calcium silicide and selenium. We tried to make an alloy without success. It decomposed, I think, on exposure to damp atmosphere and gave off an odor resembling hydrogen sulphide.

[2] Battelle Memorial Institute, Columbus, Ohio

A STUDY OF THE PRECISION OF SAND TEST DATA*

By Robert E. Morey and
Carl G. Ackerlind

ABSTRACT

Reproducibility or precision of tests used in the evaluation of foundry molding sands were examined by statistical methods. Shewhart's method for relatively small samples was found particularly suitable for this purpose. Variations in test values were found due to technique and due to different operators. Similar equipment in different laboratories gave different results. The assurance that apparent differences in test results represent actual differences in the sand is increased when the testing technique is standardized and the errors of sampling and testing are reduced to a practical minimum.

ONE OBJECTIVE OF A FOUNDRY is to produce the maximum amount of sound castings per dollar of operating costs. To accomplish this, various methods are used by the foundry supervisor to control the quality of raw materials and processes. Among these methods is sand control which has done much to reduce the number of castings made defective by improper sand.

Sand control measures the quality of molding and core sand in terms of certain tests. Tests for moisture content, green compressive strength and green permeability are those most commonly used. The results are expressed as numerical values.

It is the task of the foundry supervisor to determine from the test values whether or not the sand is satisfactory on the basis of his experience and to correlate sand test values with castings scrapped due to sand defects. He must know whether or not the foundry sand is under control, and, if possible, correct faulty conditions before bad castings are produced.

Such a procedure puts considerable dependence on the machines used for testing the sand, as well as on the operators of these machines. The present work was undertaken to find the effect of several variables in testing technique, and to determine the precision of the equipment and the operators.

There are two main reasons for variation in test results. First, there is the material being tested, and second, the methods of testing. If two batches or samples of sand are tested, and nothing is known of their source, the variations may be due to a real difference in the material. If the same batch or sample is tested, variations in values given by tests must be due to the testing machines or the operators. It follows that the finer the precision of the testing procedure, the greater is the assurance that the differences are due to a change in the sand.

Test Methods

In order to measure precision, statistical methods must be used. The general principles of statistics are explained in several standard textbooks.[1,2,3,4,5]

To obtain a statistical analysis, the tests must be repeated many times on a batch of sand or on different batches of sand which are supposed to be identical. The average of all the values in such a series of tests is called the arithmetic average designated by the symbol $\overline{X}$. Each test differs from the average by some amount, and when these differences are squared and averaged, the square root of this average is called the standard deviation of the arithmetic average. The standard deviation is denoted by the symbol σ.

Statisticians have determined that if the distribution of the results about the average value is approximately a normal or Gaussian distribution, the relationship between the arithmetic average and the standard deviation will be as follows:

The arithmetic average plus or minus one standard deviation will be the limits for 68.26 per cent of all subsequent tests on the same material. If the limits are $\overline{X} \pm 2\sigma$, 95.46 per cent of all tests will be included, and for limits of $\overline{X} \pm 3\sigma$, 99.73 per cent of all tests will be included.

Arithmetic Average

The arithmetic average is an estimate of the central tendency of a series of tests. The standard deviation is a measure of the dispersion of the individual test values about that average. Therefore the smaller the value of the standard deviation, the more precise is the testing. From the standard deviation, the standard error of the average may be calculated. Just as the standard deviation will give the probable limits for individual tests in a series, the standard error of the average will give the probable limits of the average of another series of tests on the same or similar material.

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In the present work, the effect of variables in testing the green compressive strength of sand was studied in an attempt to establish the best methods for reproducing test results. It is impossible to reproduce test results exactly but it is possible to reproduce test results within probable limits. When the test has been refined so that the limits are small, the test will be of greater value because small changes in test values will then indicate a real difference in the material, and some sort of action can be taken to bring the properties of the material back within the limits decided upon.

Table 1 shows the effect of using two specimen tubes in the preparation of sand specimens for testing green compressive strength. Sixteen tests were made on specimens rammed in tube No. 1, and 16 tests were made on specimens rammed in tube No. 1 and tube No. 2 alternately. From an examination of the original data one would be inclined to say that there is very little difference, due to the difficulty of comparing 32 numbers. Statistical analysis will reduce this mass of data to a few significant numbers or parameters which may easily be compared.

One Method of Statistical Analysis

By one method of statistical analysis all the data for each series of tests may be used, and the arithmetic average can be obtained by adding all the test results together and dividing the total by the number of tests made. The standard deviation is computed as described before.

These computations result in two figures which may be compared. For the data pertaining to tube No. 1 the average was 5.37 psi and the standard deviation was 0.04 psi. When tube 1 and tube 2 were used alternately, the average strength of the specimens was 5.28 psi and the standard deviation was 0.219 psi.

These figures mean that if another 16 tests of a similar batch of sand were made on specimens from tube No. 1, 99.73 per cent of the tests would fall between 5.37 psi $\pm$ 3 x 0.04 or between 5.25 and 5.49 psi, and for 16 tests on specimens from tubes No. 1 and No. 2, the strengths would fall between 5.28 psi $\pm$ 3 x 0.219 or from 4.62 to 5.94 psi.

TABLE 1.—DATA FROM 32 TESTS; 16 TESTS WERE MADE WITH RAMMER TUBE NO. 1 AS CONTROL BASIS; 16 TESTS WERE MADE USING TUBE NO. 1 AND TUBE NO. 2 ALTERNATELY

Tube No. 1		Tubes No. 1 and No. 2 used alternately
5.38 psi		5.19 psi
5.33		5.44
5.32		5.22
5.33		5.39
5.31		5.07
5.36		5.40
5.42		5.13
5.40		5.37
5.41		5.14
5.44		5.33
5.39		5.00
5.40		5.32
5.37		5.09
5.33		5.47
5.32		5.12
5.39		5.39
5.37	Arithmetic Average	5.28
0.04	Standard Deviation	0.219

If one wishes to know the probable limits for the *average* of an additional 16 tests under the same conditions the standard error of the average is computed. The standard error is found by dividing the standard deviation by the square root of the number of samples. For the specimens prepared in tube No. 1 this would be $0.04/\sqrt{16} = 0.01$. Since the average of the additional 16 tests would fall within $\pm$ 3 standard errors, the range would be from 5.34 to 5.40. For tubes 1 and 2 the standard error is $0.219/\sqrt{16} = 0.055$. Three standard errors = 0.165, hence the average would fall between 5.11 psi and 5.45 psi.

The parameters thus obtained show that the averages for either method of testing may coincide because the range of results obtained when the No. 1 tube is used is well within the range when both No. 1 and No. 2 tubes are used alternately. The deviation to be expected with both tubes is 5½ times as great as when No. 1 tube alone is used. This means that test data obtained by using the No. 1 tube are more precise than data obtained by using two tubes and that the average is a closer estimate of green compressive strength.

Another Method of Analysis

Another method was developed by W. A. Shewhart and is described in his book *Economic Control of Manufactured Product*. This method is particularly applicable to small samples and is therefore suitable for sand test data because the foundryman must rely on a few tests to tell him the quality of his sand. This method, outlined below, shows how testing methods and shop methods may be improved by finding the causes of variance in sand by testing small samples.

The steps involved in making the tests as described by Shewhart are as follows:

1. List the measurements in the order in which they were made.
2. Divide the measurements into groups of four each, starting with the first. A minimum of eight measurements are required but more dependable results are obtained with more than two groups.
3. Compute the arithmetic average, $\overline{X}$*, of the values for the four measurements in each group.
4. Compute the standard deviation, σ, of the four values in each group.
5. Compute the arithmetic average, $\overline{\overline{X}}$, of the averages, $\overline{X}$, of the subgroups.
6. Compute the arithmetic average, of the standard deviations, σ, of the subgroups. This average is denoted by $\overline{\sigma}$.
7. Compute the standard error of the average of the subgroups written $\sigma_{\overline{\overline{X}}}$ from the formula

$$\sigma_{\overline{\overline{X}}} = \frac{\overline{\sigma}}{C_2 \sqrt{N}}$$

where N is the number of values in each subgroup and C_2 is a correction factor for small samples. The value of this factor depends on the number of values in each

*Average and mean are used interchangeably.

subgroup. This factor is denoted C_2 in the table given on page 185 of Shewhart's book, and on page 134 in Simon's book, and is included here as Table 11. C_2 is a function of the number of tests and is the ratio between the average deviation, $\bar{\sigma}$, and the true standard deviation.

8. Compute the control limits from the formula

$$\bar{\bar{X}} \pm 3\sigma_{\bar{X}}$$

A subgroup average, $\bar{X}$, lying outside these limits indicates a lack of control of those factors affecting the average. This indication of lack of constancy in the averages may be a clue leading to the discovery of assignable causes of variance.

The control limits on the standard deviation, σ, are computed by the same steps as those used for the arithmetic averages. From these computations the standard error of the standard deviation, written $\sigma_{\bar{\sigma}}$ may be found from the formula

$$\sigma_{\bar{\sigma}} = \frac{\bar{\sigma}}{C_2\sqrt{2N}}$$

with C_2 and N the same as before. In this case the limits are the arithmetic average of the standard deviations $\bar{\sigma} \pm 3\sigma_{\bar{\sigma}}$

If a standard deviation of a subgroup falls outside the control limits there is evidence of a lack of control. If either the average, $\bar{X}$, or the standard deviation, σ, of any subgroup or groups fall outside the limits thus set up, there is reason to believe that some assignable causes for this variation may be discovered. As the discovered causes are eliminated the limits will decrease.

When the process is under complete statistical control the only reasons for variation will be those factors which have not been discovered. For economic or other practical reasons, the search for, and elimination of these causes, may not be profitable. Consequently their influence on the measurements must be considered chance.

Using the data from Table 1, the first 4 steps listed by Shewhart are applied and the following averages and standard deviations for groups of four are obtained.

Tube No. 1		Tubes No. 1 and No. 2	
Average $\bar{X}$	Std. Deviation σ	Average $\bar{X}$	Std. Deviation σ
5.34	0.074	5.31	0.107
5.37	0.042	5.24	0.144
5.41	0.019	5.20	0.136
5.35	0.028	5.27	0.165

Then steps 5 and 6 are followed to obtain the average of the averages, and average standard deviation, which are for No. 1 tube $\bar{\bar{X}} = 5.37$, $\bar{\sigma} = 0.041$ and for tubes No. 1 and No. 2, $\bar{\bar{X}} = 5.26$, $\bar{\sigma} = 0.138$.

Step 7 is applied to the final figures for tube No. 1. By substituting in the formula for standard error of the mean

$$\sigma_{\bar{X}} = \frac{\bar{\sigma}}{C_2\sqrt{N}} = \frac{0.041}{0.7979\sqrt{4}} = 0.025$$

In step 8, $\sigma_{\bar{X}}$ is used to compute the limits for any average of four tests if made under the same conditions on the same or supposedly identical material. These limits will be $5.37 \pm (3 \times 0.025)$ or from 5.29 to 5.45. A graph, Fig. 1, illustrates how the tests meet these requirements of precision.

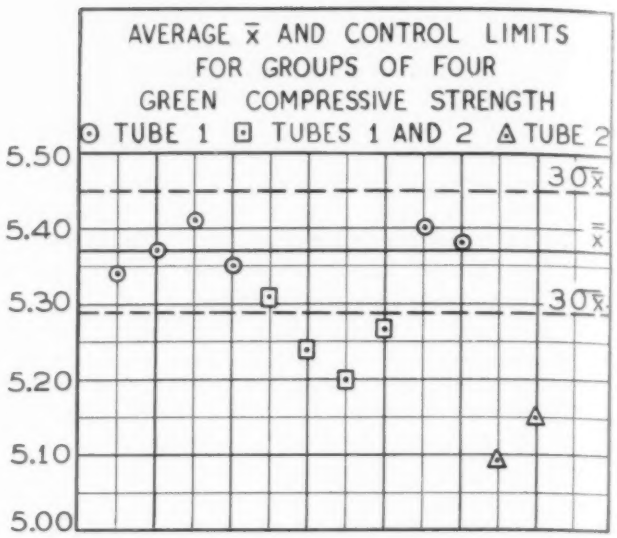


Fig. 1—Green compressive strength values.

The graph shows all the tests for tube No. 1 well within the limits but when tube No. 1 is used alternately with tube No. 2, the averages have not met this requirement in three cases out of four. The one average that does come within limits is near the lower limit.

It is obvious that tube No. 2 has had some deleterious effect on the test. Upon analyzing data for tube No. 2, it was found that it consistently gave lower green compressive strength to the test specimen. When measured with a micrometer the tube was found to be slightly out of round. To produce uniform results, all tubes should be hardened and then internally ground to two inches internal diameter. Since these tests were performed, it has been learned that other laboratories have been experimenting with ground specimen tubes.

Test Results

To test the variation to be expected between batches of sand mixed according to formula and supposedly alike, several batches made in both laboratory and shop mullers were used. Tables 2 and 3 and Fig. 2 give the data.

It is apparent from these tables that the degree of control possible in the laboratory is not approached in the shop. The work of the laboratory is precise enough to reproduce a given batch of sand within much closer limits than has generally been supposed. With proper control, shop mixing may be greatly improved in precision. While the agreement between the two laboratories is not exact, the degree of precision indicates that closer agreement could easily be attained by checking on testing machines and technique.

To determine the variation to be expected between operators in the same laboratory, testing the same batch of sand, five men were called into the laboratory. These men had never tested sand, and after being shown how to make the tests, made four tests without supervision. In Table 4 and Fig. 3 the results are given. The technician who regularly does the sand testing is designated as A.

In Table 4, all the data are in close agreement except in the case of D, although his figures fall within the cal-

TABLE 2.—AVERAGE VALUES AND LIMITS OF GREEN COMPRESSIVE STRENGTH FOR BATCHES OF SAND MADE ACCORDING TO FORMULA IN A CLEARFIELD NO. 404 SHOP MULLER

Property	Average	Limits of Precision to be Expected
Moisture Content	3.64%	±0.5%
Green Permeability	94	±5.0
Green Compressive Strength	3.96 psi	±0.68 psi

culated limits for the tests. Operator D experienced great difficulty in reading the scales of the testing apparatus. The permeability meter was especially hard for all of them to read. The scale lacks contrast and is not close enough to the meniscus. The cam arrangement is seldom used and just gets in the way. Figure 3 shows the calculated average and limits for green compressive strength based on the data from twelve tests made by A.

The same operator in a different laboratory using different equipment, will also introduce variation. In Table 5 results of tests by Operator A made at NRL and at another sand laboratory are compared. The results obtained by their technician are also shown.

Variation in Laboratories

Although the differences between average values are not large, the effect of different apparatus on precision is apparent. The results obtained by the NRL operator are poorer in precision in another laboratory than his results obtained at NRL, or the results obtained by the operator of the second laboratory. The original NRL limits are calculated from data covering six batches of sand, and 272 tests. For the second laboratory the limits are based on 24 tests, and two batches of sand.

So far it has been shown that the same technician, using the same apparatus, can work within limits of ± 2 per cent of the average value for a given test. If he is unfamiliar with the apparatus with which he is working, his results will not be too consistent at first, but his precision should improve with practice. Several different operators using the same apparatus cannot reproduce data exactly, but can easily stay within statistical limits of precision.

The next step is to examine the effects of variations in technique by one operator. One testing detail which seems to have a large effect on test results, particularly on green strength, is the practice of sieving the sand through a ¼-in. riddle before testing, as described on

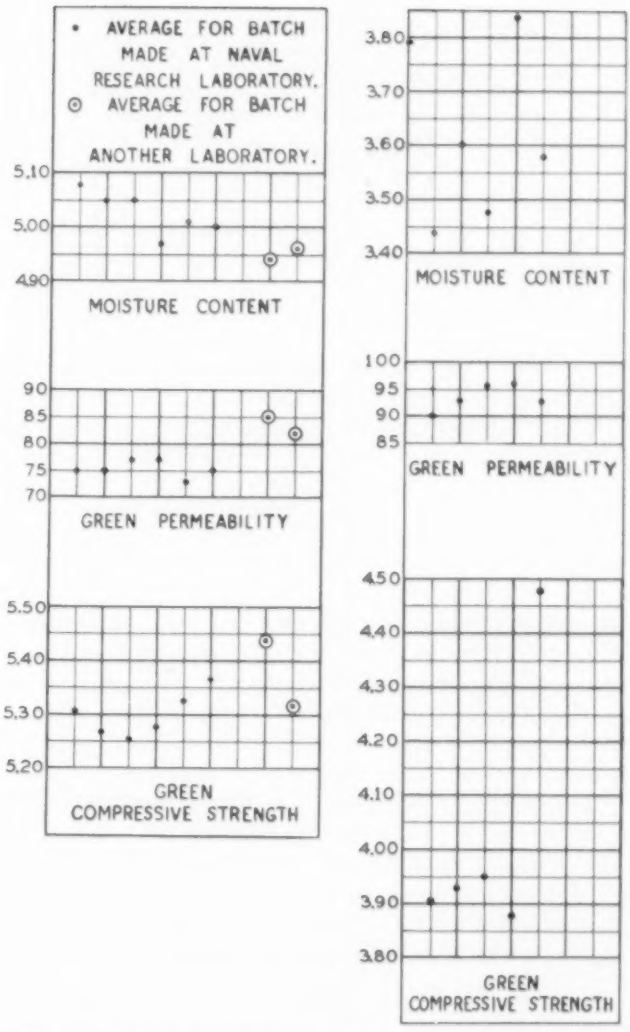


Fig. 2a (left)—Reproduction of batches made in laboratory muller. Fig. 2b (right)—Reproduction of batches made in shop muller.

TABLE 3.—AVERAGE VALUES AND LIMITS OF GREEN COMPRESSIVE STRENGTH FOR BATCHES OF SAND MADE ACCORDING TO FORMULA IN AN 18-IN. SIMPSON LABORATORY MULLER

	NRL		Another Lab		Combined	
	Av.	Limits	Av.	Limits	Av.	Limits
Moisture Content	5.04	±0.10	4.93	±0.10	5.05	± 0.24
Green Permeability	75	±5	84	±5	80	±10
Green Compressive Strength	5.28	±0.11	5.35	±0.18	5.30	±0.138

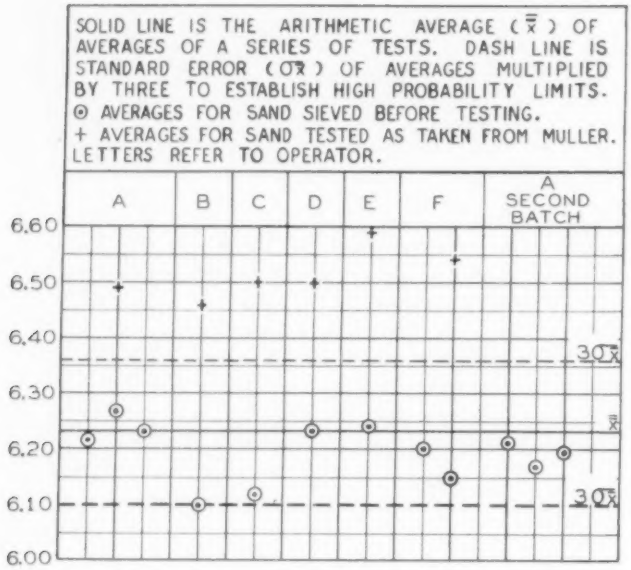
TABLE 4.—RESULT OF TESTING BY SIX DIFFERENT OPERATORS

Limits were Calculated on 12 Tests Made by A

Test Made By	A	B	C	D	E	F	A 2nd Batch
Moisture Content	5.10 ± .05	5.04	5.00	5.12	5.06	5.02	5.00
Green Permeability	75 ± 2	75 ± 2	75 ± 2	71 ± 5	73 ± 2	73 ± 2	75 ± 2
Green Compressive Strength	6.23 ± .129	6.10 ± .051	6.12 ± .160	6.23 ± .177	6.24 ± .177	6.15 ± .087	6.18 ± .081

Based on All Tests

H_2O 5.07 ± .066 Green Permeability 75 ± 5 Green Compressive Strength 6.18 ± .099



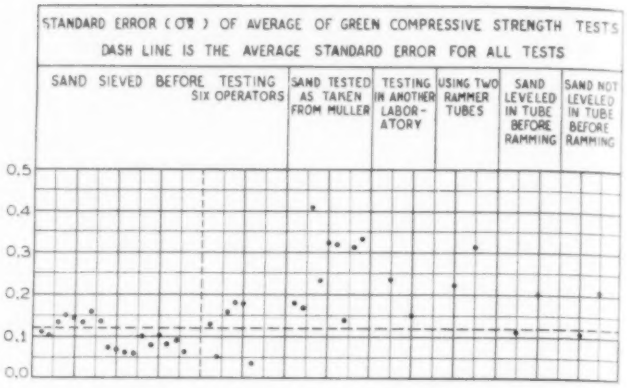


Fig. 3 (left)—Green compressive strength tests made by six operators. Fig. 4 (above)—Standard error of average of green compressive strength tests.

pages 6, 7, 12, and 13 of the AFA *Sand Testing Handbook*, 1944 Edition.[7] The sand was sieved directly into two quart Mason jars, care being taken to avoid compacting the sand in the jar. The other portion of the sand was tested as taken from the muller. There was no appreciable loss in moisture and no significant change in permeability. Therefore in Table 6 and Fig. 4, only green compressive strength is recorded.

The table shows that no matter what the sand may be, the green compressive strength of sand sieved before testing, is always lower than that of unsieved sand. This higher strength is always accompanied by a higher standard error which indicates that measurement of green compressive strength is more precise when the sand is sieved. A ½-in. sieve produced the same effect as the ¼-in. sieve. For comparisons of strength of different sands this sieving operation is essential.

On page 21 of the *Sand Testing Handbook*[7] the practice of leveling the sand in the rammer tube is described.

Summary of Results

A summary of the results of a total of 72 tests which were made in two different mixtures is shown in Table 7. The agreement in green compressive strength indicates that the practice has little or no effect on the

TABLE 6.—GREEN COMPRESSIVE STRENGTH OF SIEVED AND UNSIEVED SAND MIXED IN LABORATORY MULLER (18-IN. SIMPSON)
NO. 80 N.J. SI SAND 89 PER CENT, DEXTRINE 1 PER CENT, SI FLOUR 5 PER CENT, BENTONITE 5 PER CENT

Batch	1		2		3		4		5		6		7	
Sieved Sand S, Unsieved Sand U	S	U	S	U	S	U	S	U	S	U	S	U	S	U
Average of Averages of Groups of Four $\bar{x}$	5.30	5.38	5.27	5.40	5.26	5.48	5.28	5.48	5.32	5.51	5.38	5.57	5.30	5.47
Average Standard Deviation of Groups of Four $\bar{\sigma}$	.043	.108	.050	.135	.033	.090	.121	.105	.120	.070	.086	.124	.075	.106
Standard Error of Averages $\sigma_{\bar{x}}$	.027	.067	.031	.084	.020	.056	.076	.066	.070	.043	.053	.077	.046	.065
Standard Error of Standard Deviation $\sigma_{\bar{\sigma}}$	.031	.048	.022	.059	.014	.040	.053	.046	.053	.031	.038	.055	.035	.046

SAND MIXED IN SHOP MULLER (CLEARFIELD 404)

Batch	Si flour 5% Bentonite 5% No. 135 Sand 89% Dextrine 1%		Same Tempered 24 Hr.		No. 80 Sand 71% Si Flour 15% Fire Clay 10% Bentonite 3% Dextrine 1%		Same Dry Strength		Albany Sand Mulled Tempered 24 Hr.		No. 2 Sieve	No. 4 Sieve
Sieved Sand S, Unsieved Sand U	S	U	S	U	S	U	S	U	S	U	S	S
Average of Averages of Groups of Four $\bar{x}$	7.29	7.59	6.91	7.39	7.15	7.91	521	407.2	5.24	5.52	3.78	3.74
Average Standard Deviation of Groups of Four $\bar{\sigma}$	.091	.171	.095	.107	.090	.258	19.4	31.7	.108	.126	.033	.042
Standard Error of Averages $\sigma_{\bar{x}}$	.048	.091	.050	.057	.048	.137	10.3	16.8	.067	.078	.020	.026
Standard Error of Standard Deviation $\sigma_{\bar{\sigma}}$	.034	.064	.035	.040	.034	.097	7.3	11.90	.047	.056	.014	.018

strength and both methods are equally precise. However from the point of view of standardizing the work, inverting the tube was practiced in all subsequent tests. No change in green permeability was observed.

In order to find the effect of careless ramming, one series of tests was made in which the weight was dropped on the sand and the crank was turned rapidly. In another series, the weight was lowered carefully and the crank was turned slowly. Dropping the rammer assembly affected the average, and doubled the standard error. Since the effect on the tests was constant, the standard error for dropped rammer assembly remained small. In comparing green compressive strength reported by two laboratories, the rammer head must be lowered gently and the crank turned slowly. Table 8 shows the effect of careless ramming on green compressive strength.

One of the tubes used for ramming was out of round. Here again the error introduced was constant, but upon combining the results of the tests using both tubes, it becomes apparent that this is a source of error, and the effect on the average value is pronounced. The data are given in Table 9.

Different Testing Machines Compared

The results obtained from different strength testing machines were compared. Two of the machines were Dietert Type C.P., one at NRL and the second in another laboratory. The third machine was a shot loaded machine designed for breaking green core specimens.

The precision of the machines was comparable, but the average green compressive strength of the shot loaded machine was low. This difference may have been due to the difference in the rate of loading. The Dietert machine loaded at the rate of 29.5 lb per min, the shot machine at 2.3 lb per min. Table 10 shows the green compressive strength on two machines.

Time of tempering has little effect on the test results of mulled sand. No appreciable differences were noted in sand, whether it was tested immediately after mulling or after tempering for 24 hours. This variable would probably have some bearing on natural bonded sands that are not mulled.

The deformation attachment had some effect on the green compressive strength, but the variable introduced was constant unless adjustments of the friction shoe were made between tests. The average strength was increased or decreased when adjustments were made, since the load could be changed by increasing or decreasing the pressure on the shoe. Normal pressure required to operate the attachment increases the load about 0.2 psi and when used, the recorded strength will be low by this amount.

Permeability data cannot be analyzed according to the methods outlined because the variation is not continuous. For example, a change of 1 mm on the manometer scale can give a difference of 2 to 450 in permeability when the large orifice is used. As a result the average and the standard error cannot be given in terms of the original scale. It is best, therefore, to use the range as limiting values, that is, the highest and lowest readings for a series of tests.

From a study of the test results of 20 different batches

TABLE 5.—RESULTS OBTAINED BY ONE OPERATOR WORKING IN TWO LABORATORIES COMPARED TO RESULTS OF TECHNICIAN WORKING IN OWN LABORATORY

	NRL NRL Technician	Another Lab NRL Technician	Another Lab Their Technician
Moisture	5.05	4.94	4.90
Permeability	75 ± 2	85 ± 5	85 ± 3
Green Compression Strength	5.31 ± .072	5.37 ± .207	5.34 ± .057

TABLE 7.—EFFECT OF LEVELING SAND IN RAMMER TUBE (BY INVERTING ON AN EXTRA PEDESTAL) ON GREEN COMPRESSIVE STRENGTH

	Sand Leveled		Sand Not Leveled	
	Average	Std. Error	Average	Std. Error
Shop Mix	3.49	.039	3.45	.036
Lab. Mix	8.27	.072	8.38	.075

TABLE 8.—EFFECT OF CARELESS RAMMING ON GREEN COMPRESSIVE STRENGTH

Routine Ramming		Rammer Assembly Dropped	
Average	Standard Error	Average	Standard Error
4.39	.019	4.62	.039

TABLE 9.—EFFECT OF DISTORTED TUBE ON GREEN COMPRESSIVE STRENGTH

Round Tube		Out of Round Tube		Combined Results	
Average	Std. Error	Average	Std. Error	Average	Std. Error
5.38	.026	5.16	.028	5.28	.075
5.65	.040	5.31	.093	5.48	.066

TABLE 10.—GREEN COMPRESSIVE STRENGTH ON TWO MACHINES

	Dietert Type C.P.		Shot Loaded	
Batch	Average	Std. Error	Average	Std. Error
1	5.18	.083	4.55	.04
2	3.92	.030	3.35	.090
3	3.99	.053	3.70	.046

TABLE 11.—CORRECTION FACTORS C_2 FOR STANDARD DEVIATION

N	C_2	N	C_2	N	C_2	N	C_2
		11	0.9300	21	0.9638	55	0.9863
2	0.5642	12	0.9359	22	0.9655	60	0.9874
3	0.7236	13	0.9410	23	0.9670	65	0.9884
4	0.7979	14	0.9453	24	0.9684	70	0.9892
5	0.8407	15	0.9490	25	0.9697	75	0.9900
6	0.8686	16	0.9523	30	0.9748	80	0.9906
7	0.8882	17	0.9551	35	0.9784	85	0.9912
8	0.9027	18	0.9577	40	0.9811	90	0.9916
9	0.9139	19	0.9599	45	0.9832	95	0.9921
10	0.9227	20	0.9619	50	0.9849	100	0.9925

N = Number of Items in Sample.

of sand involving 634 tests, permeability was found to be very little affected by variations in testing technique. The same operator using the same instrument seldom varied more than ± 5 units from his first determination in the range between 50 and 126, and usually was able to work closer than that. The orifice method was used in all tests.

Figure 4, a graph of the data for green compressive strength, shows the variation in terms of standard error of the mean in psi. The degree of precision possible with the present testing apparatus and technique is also shown. For the sake of clarity the variations in the average from batch to batch are not shown. Variations which affect the average values are shown in Fig. 5.

Variations in Technique

The graph demonstrates that sand tests can be made with more precision than was expected. The causes of variation are easily found and eliminated if technique is involved. In general, any variation of more than 4 per cent of the average is probably caused by discoverable variations in technique. The A.F.A. *Sand Testing Handbook* permits as much as 10 per cent variation from the average for the same test.

The elimination of variance between laboratories is much more difficult because both the testing equipment and the human element are involved. Still, much closer agreement is possible if each laboratory does its work carefully and standards are agreed upon.

The statistical limits shown in the various tables are practical limits based on existing testing equipment. The chances are 1000 to 1 that anyone competent to make sand tests can come within these limits in his test results. These limits are not set up on carefully controlled experimental data, but on as varied conditions as were available.

Statistics point out the causes of discrepancies which may be eliminated, and also show the practical and economical limits beyond which it is not necessary to go. Careful compounding of batches and accurate laboratory testing will produce better sand in the foundry and sources of trouble may be located before scrap appears if statistical control is used.

Conclusions

1. Shop batches of sand may be duplicated within limits but the limits for moisture, permeability and strength are greater than the limits for the same mixture made in a sand laboratory.
2. Test results are reproducible within close limits in any one laboratory if made by the same technician.
3. There will be variations between tests made in the same laboratory by different technicians, and between laboratories, but these variations can be reduced by adoption of standard practices.
4. Sand test data are a reliable basis for judgments leading to closer control of molding and core sands.

Recommendations

1. Specimen tubes should be internally ground and honed after hardening.
2. The cam on the permeability meter should be replaced with an improved device for making the readings, possibly including sights so that readings can be made only horizontally. A vernier scale might be useful. Background and lighting of some of the instruments could be improved considerably.

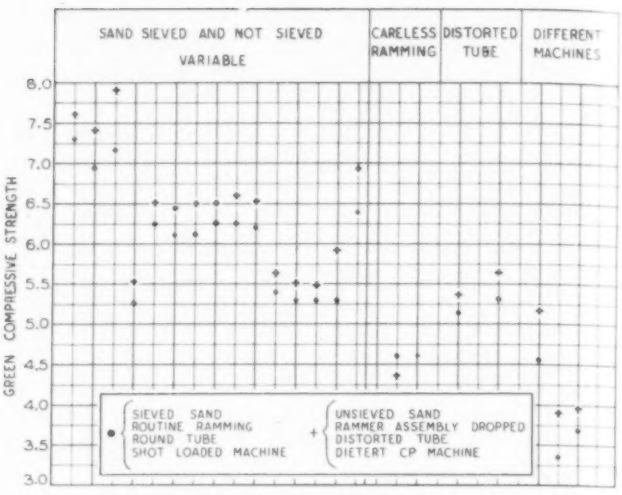


Fig. 5—Effect of variables on green compressive strength of molding sand.

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DISCUSSION

Chairman: G. R. GARDNER, Aluminum Co. of America, Cleveland

Co-Chairman: K. J. JACOBSON, Griffin Wheel Co., Chicago

MEMBER: Do you know of any work done to calibrate the sand testing equipment which is now used? You brought out very nicely the method for determining precision in a given laboratory, and you did have something to show that between laboratories, getting checks was more difficult. Have you done any work to show whether that was the operator's technique or the calibration of the equipment was not the same in each case and, therefore, that might be the assignable cause for the variation?

MR. MOREY: I have done no work to calibrate sand test equipment. I have used proving rings and devices of that sort on larger compression testing equipment, and that same technique can be carried to the sand testing equipment.

Recently I visited one of the equipment manufacturers in Detroit, and he had a device for checking compression testing machines of the size that most of us use. It is a device with a 2-in. clearance between the heads and it can be put in the testing machine and will enable us to calibrate machines like our green compressive strength machine.

There is another device which is used for calibrating permeability machines. It consists of a tube with a diaphragm across it, and a small orifice screwed into that diaphragm plate so that it will pass a certain amount of gas and give the same effect as a sand specimen in the tube when we make the permeability test.

This device should be passed around among the various users of the equipment, and then they can get an occasional check on their machine to see if it is performing satisfactorily.

DR. H. RIES[1]: Are those tests made in two different laboratories on different compression machines?

MR. MOREY: We used different sand mixers, different machines, all the way through. The make of the compression machine was the same. One was an earlier model than the other. The switching mechanisms were different, but they were the same type of machine.

DR. RIES: Are you sure the rate of application of the load was the same in both machines?

MR. MOREY: I am not sure.

DR. RIES: That will make a difference.

MR. MOREY: In general, if the load is applied more slowly, the test result will be lower.

DR. RIES: Yes.

E. E. WOODLIFF[2]: I would like to add to the thought on calibration of instruments. My work takes me into many foundries. I have yet to find two foundries whose instruments are in a good state of repair.

This is not a criticism of equipment or the initial idea in setting up sand controls. But it does have a bearing upon the foundry using the equipment, and to their own disadvantage in many cases they allowed the equipment to deteriorate. Any of us associated with sand control would do well to write into our instructions for sand testing, a closer, more detailed method of analysis and maintenance of equipment. Foundrymen lack much information on the very thing we are discussing, i.e., to give the operator a means of calibrating his equipment. Many sand testers attempt to do this but do not have a clear cut method of equipment maintenance. I do find a lot of instruments which are off calibration.

K. S. CARLETON[3]: In one of the preceding talks the statement was made that the sand laboratory should be centrally located.

Our sand laboratory is centrally located which means that it is completely surrounded by heavy machinery such as jolt machines, cranes and block crushers.

The vibrations caused by this heavy machinery would make us look rather ridiculous in trying to produce accurate test results such as shown in this paper. I feel that this is something that should be considered when locating your laboratory.

MR. MOREY: I have been in many laboratories ,and the equipment varies from poor to good. You have to find an operator who will take pride in his laboratory and in his equipment and keep it in condition. And even then he needs some kind of assistance from the American Foundrymen's Association in calibrating that equipment. If he does not have standardized calibration procedure, he will devise his own, and then each man in the various laboratories will be using different calibration procedure.

CO-CHAIRMAN JACOBSON: We had occasion to put in 11 new complete units. We found considerable variation in the new units even after they were thoroughly checked. These units never had been used, and they were put into the foundry under strict supervision.

MR. MOREY (Authors' Closure): This discussion has brought out one more very important recommendation which should be added to the paper. This is, that the American Foundrymens Association, through the Committee on Green Sand Properties, should establish methods and devices for checking the accuracy of sand testing machines as well as suggestions for maintenance of the equipment.

[1] Cornell University, Ithaca, N.Y.
[2] Foundry Sand Service Co., Detroit
[3] Buick Motor Div., Flint, Mich.

CALCULATING SIZES OF GATES AND RISERS

By
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The Centrifugal Casting Machine Co.
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THE DECISION concerning the size of gate or riser for a casting in most foundries has been and still remains, an art. An oversize riser generally is used, with the result that the metal yields obtained in foundry practice are relatively low.

During the past few years various empirical methods of gating and risering castings have appeared, intended to obtain a higher yield and still produce castings with adequate feed metal, thus producing castings of high quality. A number of means have been proposed for calculating the size of gates or risers. In general, however, these methods either have been too elaborate for general use or have not been tried in practice sufficiently to warrant their widespread use.

A number of years ago the writer adopted a method for calculating the size of gates and risers used in the centrifuge and semi-centrifugal methods of centrifugal casting. Application of this method has proved to be simple and satisfactory, and in a few cases the same general method has been applied to static casting work with equally successful results.

The method involves several preliminary assumptions. In the first place, it is assumed that all metal poured into the mold arrives there at the same temperature and that the temperature of mold surfaces is the same at all points in the mold. This naturally assumes that the mold is all of the same composition—without the use of either external or internal chills.

Uniform Factors Considered

The above conditions are most closely and usually obtained in centrifugal casting where the gate serves as a feeder, as well as in the blind-riser method of static casting, wherein gating is carried into the riser itself. Thus the connection between the riser and the casting serves both as a gate and as the feed passage. In these systems of pouring, the condition that all metal arrive in the mold at the same temperature is more nearly achieved.

The condition actually achieved, however, is that the metal farthest from the gate is lowest in temperature. The temperature rises progressively toward the gate, and the metal in the mold nearest the gate is at the highest temperature. This is an ideal condition for directional solidification.

Where such a temperature gradient exists, it will be found that the actual size of gates and risers, as calculated by the method to be described, will be comparatively small since the method assumes that all

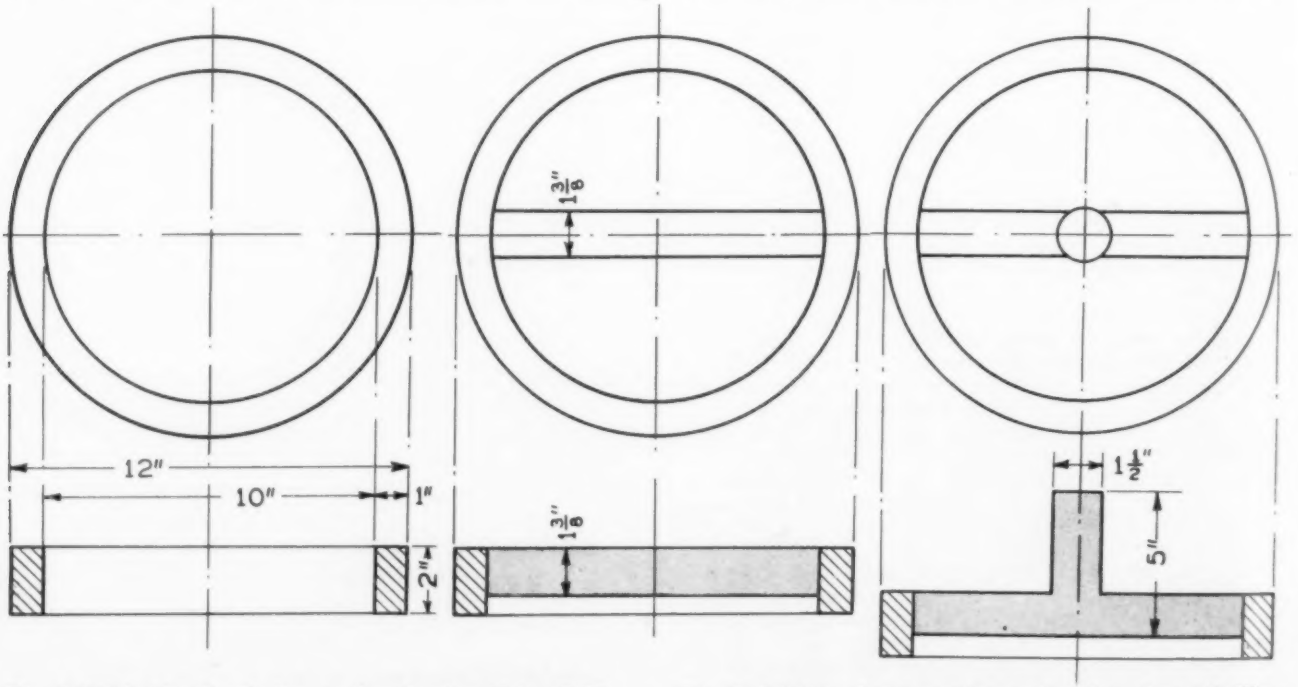


Fig. 1 (left)—Sketch of ring casting having 12-in. O.D., 10-in. I.D., 2 in. high. Fig. 2 (center)—Same ring casting shown in Fig. 1, illustrating position of two gates.

Fig. 3 (right)—Same casting as in Figs. 1 and 2 illustrating downgate determined as required for feeding side gate. Cooling factor is same as for square gate.

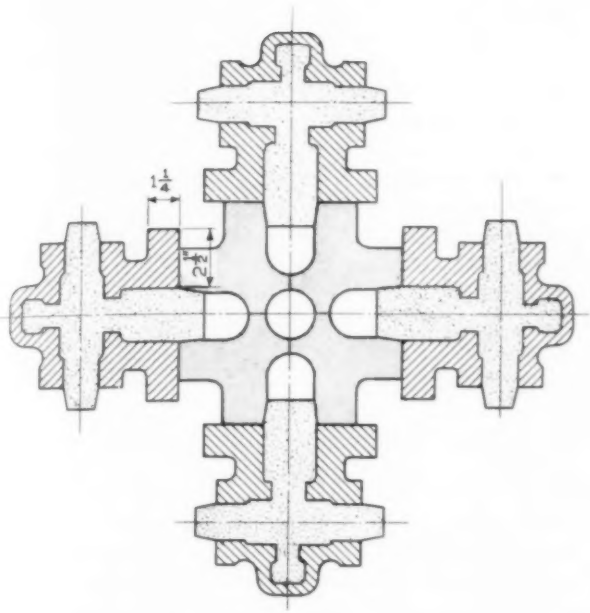


Fig. 4—Cross-section of valve body casting. Size and type of gating required can be calculated from the dimensions of flange and face.

metal arrives in the mold at the same temperature.

In describing this method a term is used which has been called the cooling factor, or C.F. The cooling factor may be calculated:

$$\text{C.F.} = \frac{\text{Volume of casting}}{\text{Surface area of casting subject to cooling}}$$

Thus, by definition, we have a measure of the rate at which the casting will solidify. The greater the cooling factor, the greater the cooling time for the casting. However, most castings do not have uniform wall sections, resulting in a condition whereby the heavier sections of the casting supply feed metal to the thinner sections. Thus, thinner sections are free of shrinkage, whereas the heavy sections and less adequately fed sections may contain shrink cavities.

The problem becomes simpler, inasmuch as it is necessary to feed only the heavier sections of the casting. For this reason risers are used on the heavier sections of castings, since these are the sections requiring feeding from external sources. In consequence, the cooling factor can be more simply defined as:

$$\text{C.F.} = \frac{\text{Area of section of casting}}{\text{Perimeter of section of casting}}$$

For demonstration purposes consider a simple example, such as a ring having 12-in. O.D., 10-in. I.D., 2-in. high, as shown in Fig. 1. The cooling factor of this ring is calculated as follows:

$$\text{C.F.} = \frac{2 \times 1}{1+2+1+2} = 0.333$$

The result is a relative measure of the time required for the casting to lose heat to its surroundings, or how quickly the metal will solidify.

During the period of solidification, the liquid metal inside the casting will decrease in volume and additional metal will be required. For a gate or riser to supply this feed metal, it is necessary that the gate or riser freeze more slowly than the casting itself. Therefore, the cooling factor of the gate must be larger than the cooling factor of the casting.

Assume for the time being that the castings desired are to be cast centrifugally. This casting would be gated with the gate contact on the inside diameter of the casting. From the definition of cooling factor and from experience, the gate or riser which will cool most slowly has a circular cross-section. However, since it is not always practicable in molding to use a gate of circular cross-section, the gate used is generally of a square and sometimes a rectangular cross-section. In this case it will be assumed that a gate of square cross-section will be used because, as will be shown later, a square has the highest cooling factor of all rectangular sections. Thus, if *a* is called the length of the side of the square, the cooling factor of the square will be as follows:

$$\text{C.F.} = \frac{a^2}{4a} = \frac{a}{4}$$

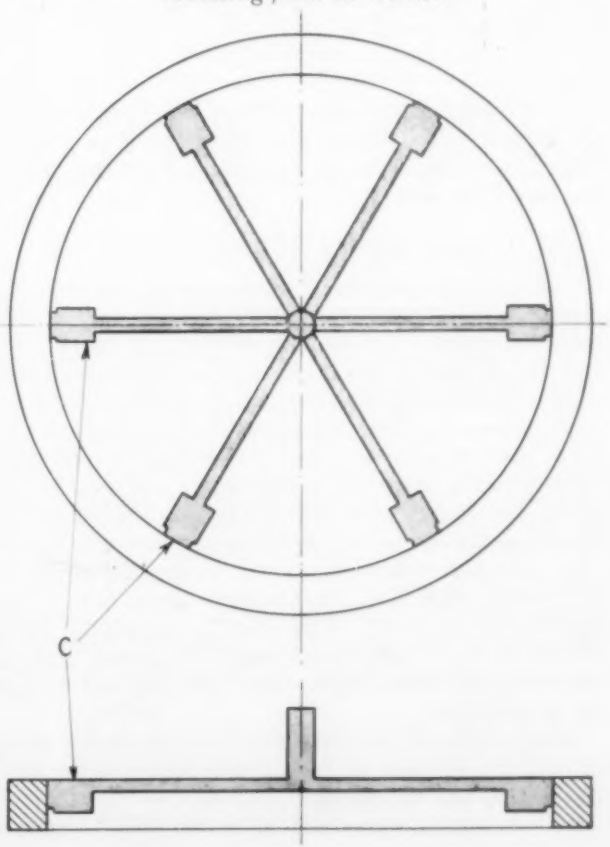
The minimum cooling factor for the gate is the same as the cooling factor for the casting. Therefore,

$$\text{C.F.} = 0.333 = \frac{a}{4} \quad \text{and,}$$

$$a = 0.333 \times 4 = 1.33$$

Accordingly, the minimum length of *a* is 1.33 in. To be on the safe side, the next larger even dimension, such as 1⅜ in. (1.375), would be used.

Fig. 5—For large castings, as in the wheel casting shown, it may prove advisable to provide the feed metal (C) in the gate itself, with small gates or runners radiating from the center.



It is evident from this equation that the cooling factor of the gate is larger than the cooling factor of the casting itself. The cooling factor of the 1⅜ in. square gate is calculated as follows:

$$\text{C.F.} = \frac{1.375}{4} = 0.344$$

Thus 0.344 is the exact size of gate required to feed this casting. It should be noted that the size of the gate is determined by the rate at which the gate cools; the gate must freeze later than the casting freezes.

Yardstick for Number of Gates

It has not been possible to find an exact method of calculating the number of gates required for a given casting, but experience indicates the following as a general yardstick:

Diam. of Casting	*No. Gates Required*
Up to 12 in.	2
12 to 18 in.	3
18 to 24 in.	4
24 to 30 in.	5
30 to 40 in.	6

Thus, for the casting under consideration, two gates are required, as shown in Fig. 2.

The actual number of gates required usually will be somewhat less than shown in the above table, since a gate will feed an area approximately nine inches from each side of its junction with the casting. This will be determined to a considerable extent by the exact shape of the casting—that is, whether square or rectangular. Other factors are section size and amount of metal in casting . . . whether the castings are large and heavy or small and light.

The experimental casting described above requires a downgate to feed the side gate decided on, as shown in Fig. 3. Naturally, a round downgate is used and the calculation is as follows:

$$\text{C.F.} = \frac{\pi D^2}{4\pi D} = \frac{D}{4}$$

This results in the same cooling factor obtained for the square gate. Thus, the downgate must have a larger diameter than 1⅜ in., and a diameter of 1½ in. would be selected.

Metal in Gates Must Be Adequate

It is entirely possible that the size of the gate and downgate have been properly selected to freeze later than the casting but may be so small as not to contain an amount of metal adequate to feed the casting.

The shrink rule allowance used in the production of patterns represents the amount of contraction that takes place in a casting during the time the solidified casting cools to room temperature. However, the metal also decreases in volume during the process of solidification. It is this shrinkage or decrease in volume that makes necessary risers or feeders to supply the casting with additional molten metal during the solidification period. The amount of solidification shrinkage is comparatively small for certain types of cast iron and brass, higher for steel, and still greater for magnesium.

The height of the downgate must be adequate to supply the amount of feed metal required for both the casting and the in-gates.

If this casting were steel, it would weigh approxi-

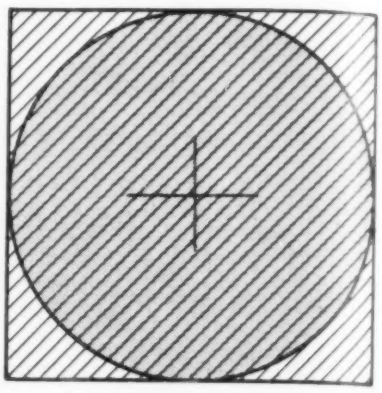


Fig. 6—Amount of metal necessary to feed a circular riser or feeder is the same as for a square feeder of same diameter. Cooling factors are also same. Thus the circular gate is to be preferred, since metal in corners of the square serves no useful purpose.

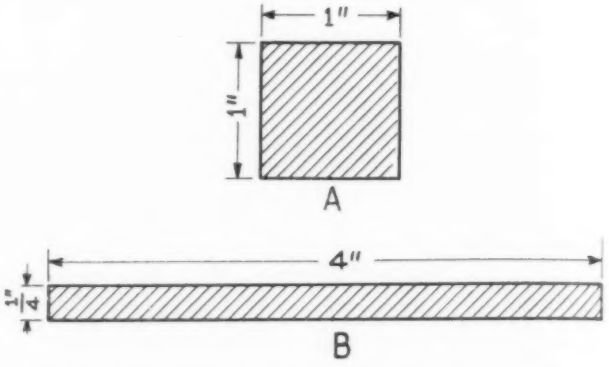


Fig. 7—Illustrating a method of determining the size of adequate gates and risers, based on relative area of the gate and the casting, which the author believes fallacious since the cooling factors vary considerably.

mately 20 lb. The volumetric shrinkage of steel is approximately 3½ per cent; but to be safe, and because many castings have irregular contours which make exact calculation impracticable, a figure of 10 per cent is used. The total weight of the side gates used in this case is 4½ lb. Thus a total of 24½ lb. requires feeding. Ten per cent of this is approximately 2½ lb., the amount of metal required in the downgate. Since each inch of the 1½ in. diameter downgate weighs ½ lb., a downgate 5 in. high would be required.

The size of the gates which contact and actually feed the casting, and the diameter and height of the downgate necessary to feed an adequate amount of metal to the in-gates and casting, have now been calculated.

In many cases the diameter of the downgate will be a figure inconvenient to use in foundry practice. For example, instead of a 1½ in. diameter downgate, a 2 in. diameter downgate could be used. However, in this case it would not be necessary to use so tall a downgate, since the 2-in. downgate need only be 2.8 in. or 3 in. tall. A larger gate may always be used, because its cooling factor will be larger and thus it will solidify later.

Calculating a Complex Casting

As another example of a somewhat more complicated casting, consider a valve body. Here the heaviest section is the flange; if the flange can be fed ade-

quately, the remainder of the valve body will be perfect. This assumes that the valve body has been properly designed from the foundry standpoint, so that directional solidification can ensue when the casting is poured; otherwise pads or chills may be required.

Figure 4 shows the cross-section of such a valve casting. It is necessary merely to measure the dimensions of the flange in order to determine the size and type of gating necessary. Assuming that the thickness of the flange is 1¼ in., with the face 2½ in. wide, the cooling factor is calculated thus:

$$\text{C.F.} = \frac{1.25 \times 2.5}{2.5 + 1.25 + 2.5 + 1.25} = 0.416$$

To be exact, it would be necessary to subtract the area of that part of the casting attached to the flange, from the amount of cooling surface which the flange encounters. However, if this step is neglected, a safety factor is provided for the size of the gate obtained and the calculation is simplified. Thus, the size of the gate required, calculating as before, would be: $4 \times 0.416 = 1.656$. The next larger convenient fractional size is 1¾ in.

The remainder of the calculations would be carried out in the same manner.

Metal Economy Considered

It is not always necessary nor economical to have the downgate equal to the calculated diameter and height to feed the castings as well as the gates. For large castings, it may be advisable to provide the necessary amount of feed metal (*C*, in Fig. 5) in the gate itself, and to use small gates or runners coming back to the center. This is a matter of economy of metal. Generally, it is possible to obtain yields of more than 75 per cent using these methods.

It is interesting to note that the amount of feeding provided by a circular riser or feeder is exactly the same as that of the smallest size square riser in which it could be placed, as shown in Fig. 6.

$$\text{C.F. for circular gate} = \frac{\text{Area}}{\text{Perimeter}} = \frac{D^2 \pi}{4 \pi D}$$

$$\text{C.F. for square gate} = \frac{\text{Area}}{\text{Perimeter}} = \frac{a^2}{4\ a} =$$

Where D = the diameter of the circle and
a = the side of the square.

For a circle of diameter D and a square of side D, the cooling factors are the same. Thus, the amount of metal in the corners is wasted.

Adequate Feeding

Various methods have been suggested for determining the size of gates or risers necessary to feed a casting, based on the relative area of the gate with relation to the area of the casting. The writer believes this a fallacy, as shown in Fig. 7. This illustrates two sections, one of which (*A*) is a 1-in. square, while the other (*B*) has exactly the same area but is ¼ in. thick and 4 in. long. From practical experience, foundrymen know that the casting or gate with the ¼-in. thickness will freeze more quickly than the 1-in. square gate. This is clearly shown by the fact that the cooling factor for the square gate is 0.25, whereas the cooling factor for the ¼ in. gate is only 0.1118.

DISCUSSION

Chairman: H. A. SCHWARTZ, National Malleable & Steel Castings Co., Cleveland

Co-Chairman: E. C. TROY, Dodge Steel Co., Philadelphia

J. B. CAINE[1]: I would like to inject caution in the use of the author's simplified formula for more complicated castings than the very simple rings that were cast centrifugally (Figs. 1 2, 3). In steel, at least, many static castings are risered at the heavy sections, and in many cases this heavy section is a junction of two sections.

The simplified formula using section area and perimeter will possibly give information as to the section of the riser required, but no information as to the volume required; and the volume in this case would be established by the height after you have a given section.

It is possible to calculate the riser size, using a more complicated formula involving surface area and volume of both casting and riser. Determine the minimum riser volume required for adequate feed metal. So that the riser remains liquid until the casting freezes. To do this I think you must bring in a value or factor denoting volume before you can riser scientifically.

Such a relation, although more complicated than the one given by the author, is applicable to all shapes, and is not too complicated to be used in a foundry. On rings (Fig. 1, 2, 3) with high surface area, a ten per cent riser volume factor may be sufficient, but the minute you get a chunkier casting, one with less surface area, the riser size required increases tremendously.

I do not think that ten per cent riser volume is sufficient for all jobs, for very rangy jobs with high surface area. When you get into a little more chunky casting, such as a gear, ten per cent, I assure you, is not enough even though it be cast centrifugally.

The table on P. 298 for the number of risers or gates required to feed a given size casting implies that a gate or a riser will feed a section four to eight inches away from that riser or gate. Does that mean that the size of the section does not mean anything? To me the size of that section becomes important, and until we know the relationship between distance of feeding, and not only section size, but section shape, I do not think any definite arbitrary statements as to how far a gate or riser will feed can be made.

C. W. BRIGGS[2]: I would like to suggest to the author, that he not use the word gate in connection with his paper. It seems to me he should use the term feeder and that the title of the paper should be "Calculating Sizes of Feeders and Risers." A study of the calculation of gates from the standpoint of flow of metal is entirely a different proposition than when the gate is used as a feeder.

Since the paper was based on a simple method of calculation, which in my opinion is not very good, I should like to suggest the use of even a simpler method suggested by the author, namely, the practice of using inscribed circles as prescribed by Heuvers and applying 15 per cent increase. Please understand that I realize that this simplified method is far from the correct way of calculating the size of risers but in my opinion neither is the one proposed by the author. Since a directed, intelligent guess is the most that can be obtained by such a formula why not use the quickest and most convenient.

MR. TROY: The thing that I find wrong with the simplified formula of either Mr. Janco or Mr. Briggs is that they are of no use on anything but the simplest of sections. Neither formula can evaluate where there is a difference in cooling rate due to a limited volume of coolant (sand) as in a sharp angle or corner.

One can produce sections such as L, T, Y. X having a like inscribed circle but all will require different riser sizes. Assuming a relationship between surface area and volume, of riser and casting sections, will serve a purpose for very simple sections such as the plate or cylinder but becomes inadequate in such cases as the T, Y and X-sections.

MR. CAINE: I think Mr. Troy is confusing two things. He is talking about inscribed circles; and using Mr. Briggs' 15 per cent increase, with Mr. Janco's formula will give the minimum section

[1] Sawbrook Steel Castings Co., Cincinnati.
[2] Steel Founders' Society, Cleveland.

size of the riser. That is one dimension of the riser. Then, what we still do not know is the volume of metal required in that riser, requiring another dimension, and that involves more complicated calculations but, again, the volume of that riser will be in proportion to the surface area and in that respect, to the cooling rate of the casting, of the individual casting and each casting is different.

MR. TROY: The freezing rate is established by the amount of sand and the amount of metal, but when you put a gate or a riser on that section, you have taken away part of the coolant, and when you remove the coolant you have more than just increased area. These inscribed circles are only part of the story.

MR. JANCO: I agree in essence with practically all of the remarks made by the discussors. At the present time most foundrymen produce castings in great tonnage by, practically speaking, hit and miss methods. Foundrymen use no gage, no method except an experience factor, which is very important, for determining the sizes of risers. Incidentally, I used the word "gates" and that was used because it is the custom. The use of the word "gate" is absolutely incorrect.

Because of the fact that that is the average situation for the bulk of the castings produced, the method for determining the size of the gate is by no means to be considered the ultimate of achievement for deciding how we should gate or riser a casting. However, if we should wait a number of years until we have developed the proper method of doing this and have put it forth for use by all foundrymen, an appreciable length of time will have passed. If in the meantime we can use, let us say, a crude method of performing the same function in possibly a crude fashion we will have done something for the average foundryman, not the foundryman who has a highly technical education, but the average foundryman who is making the castings every day. So it was for this purpose that the information was presented. This information should be used in conjunction with the experience the foundrymen have on a number of problems we have to use judgment.

I wish to point out again that the paper was not to be considered the final word on gating and risering of castings. It was to be something for the average foundryman to be able to save some money in his everyday work of gating and risering castings.

MR. JANCO *(author's closure):* Apparently the paper has not been clear in presenting the meaning of Cooling Factor. The cooling factor is a measure of the rate of solidification of a casting or of a particular section of a casting. If the same measure is used to decide upon the size of the gates or feeders it will logically give a size of these feeders in proportion to the casting itself. Particularly because both casting and feeder are subjected to the same mold material to which they lose heat. Thus heavier castings with relatively small surface cooling area will solidify more slowly and require larger feeders proportionally.

A separate and distinct problem is the amount of shrinkage of the metal during the change from a liquid to a solid. This shrinkage is a function of the particular metal. This shrinkage for steel is approximately 3½ per cent. Theoretically, we would have to provide feeders having a volumetric content of 3½ per cent of the casting weight. However feeders of such small size would not be large enough to meet the size called for by our cooling factor calculations. Also, shrinkage takes place in the feeders themselves. Hence the recommendation that the feeders weigh at least 10 per cent of the weight of the casting. This represents a 90 per cent metal yield. In actual practice metal yields vary from 65 to 93 per cent, 75 per cent being a general average.

It is normal foundry practice to locate feeders so that they do not cause a "hot spot." Accordingly, one would certainly not deliberately place a feeder connection at such a location on a casting as to cause a hot spot. In the case of cross-sections or T-sections, the feeder of the calculated size, would be placed adjacent to the hot spot in such a manner as to further directional solidification.

REDUCTION IN CHILLING TENDENCY THROUGH SILICON CARBIDE INOCULATION OF GRAY CAST IRON

By
E. A. Loria, H. D. Shephard,
and A. P. Thompson *

ABSTRACT

The normal chilling tendencies of unalloyed and alloyed cast iron can be controlled with silicon carbide inoculation. In the particular irons considered in this investigation such treatment obtains an improved chilled surface structure in which the primary carbides are much finer and uniformly dispersed. Of equal importance is the fact that silicon carbide inoculation reduces the depth of chill and provides a shortened mottled zone in both irons. The beneficial effect is clearly portrayed by photomicrographs which are representative of the types of structure obtained in the usual chilled iron as compared with the same iron containing the silicon carbide addition. The structural refinement of the white iron wear resistant surface backed by a comparatively narrow zone of finely dispersed mottled iron entails a marked improvement in physical properties.

REDUCING THE TENDENCY of cast iron to chill is one of the primary requirements of a successful inoculant. The tendency of cast iron to become hard, brittle, and unmachinable upon rapid cooling, i.e., the section sensitivity of cast iron, has undoubtedly been a major obstacle in the production of iron with optimum physical properties. Obviously, this chilling tendency of cast iron must be drastically reduced before the foundrymen can adequately consider the most suitable irons for given applications.

The effects of inoculation are attributed primarily to the action of the inoculant upon the graphite in the iron. Inoculants are not used generally for control of the amount of graphite, except in cases where it is employed to control chill. The amount of graphite usually is regulated by adjustment of chemical composition and control of cooling rate. The main effect of inoculation is upon the size, shape, and distribution of the graphite flakes. By controlling the graphite, inoculation also tends to eliminate ferrite and thus promotes development of a uniform pearlitic matrix.

Before any attempt can be made to utilize cast iron designed to give optimum physical properties, assurance must be had that thin sections or rapidly cooled sections will not chill, with consequent lack of machinability and decreased shock resistance. The chilling tendency exhibited by the untreated and inoculated irons is therefore of primary interest.

* Of the authors, E. A. Loria and A. P. Thompson are Fellow and Senior Fellow, respectively, Multiple Fellowship on Abrasives sustained at Mellon Institute, Pittsburgh, Pa., by The Carborundum Company, and H. D. Shephard is Metallurgist, Kerchner, Marshall and Company, Pittsburgh, Pa.

Silicon a Potent Chill Reducer

Massari [1] has discussed the effects of various elements in reducing the depth of chill. Silicon is the most widely used element in this respect, and, although the reduction in the depth of chill with increase in silicon is not a straight-line relation beyond 0.90 per cent silicon, it is practically so when the silicon content of the iron is less than 0.90 per cent. Silicon has no apparent effect on the grain structure of the chill, except that as the silicon is increased the depth of chill becomes less, and coincidentally therewith, the columnar grain structure is minimized. This result, however, is not the direct effect of the silicon on the grain growth but rather is attributable to the fact that, with lesser depth of chill, the dendrites are unable to grow as long.

Within the limits of chill formation on a casting, silicon does not appear to influence either the hardness of the chill or its wear resistance when subjected to abrasion tests. Carbon exerts a pronounced influence on the depth of chill produced in a given casting and is almost as effective in this respect as silicon.[2] Comparatively small quantities of amorphous graphite, added to the ladle just prior to filling, will substantially reduce the depth of chill in the resulting casting. The reaction with the molten metal is probably twofold: first, one of deoxidation, and secondly, but more important, it supplies the nuclei for subsequent graphite precipitation during solidification.

Silicon Carbide Used

The marked beneficial effects of specially processed briquetted silicon carbide as a deoxidizer and as an inoculant have been recognized.[3] In fact, many foundries have employed it successfully in the production of cast iron with uniform physical properties, improved machinability, and good (random) graphite distribution. A paper describing its effect on the quality of iron used for automotive castings has been published.[4] The purpose of the present paper is to provide evidence of its effectiveness in producing uniform chilling char-

acteristics, resulting in improved physical properties and machinability.

Experimental Procedure

In order to reveal the chilling tendency in cast iron, two general methods may be used. First, the fracture of chill test wedges will show the influence of any element on the chilling tendency and will provide a cross-section for the measurement of the overall chill. Secondly, a study of the microstructure of chilled iron at various positions below the chilled surface can be employed as a means of predicting the probable physical properties as in the case of steel. This test is more sensitive in that it will provide quantitative evidence of the type of chill existing at the surface and the extent of the mottled zone between the chilled surface and the gray iron interior. Thus it becomes evident that improvement in the properties of chilled iron entails the modification and coincident improvement of not one but three different microstructures within the same casting, and that the use of an inoculant must be directed so as to modify beneficially the entire microstructure simultaneously and not one to the detriment of the others.

The present investigation considers both methods with particular emphasis on the second. In order to evaluate the effect of silicon carbide on unalloyed iron, a comparison has been drawn between two commercially made chilled iron wheels, the actual cross-sections of which are shown in Fig. 1. The ladle analysis of the untreated iron was 3.68 per cent total carbon, 1.44 per cent silicon, while that of the silicon carbide inoculated iron was 3.83 per cent total carbon, 1.40 per cent silicon. It is evident that the ladle analysis of the inoculated iron is 0.15 per cent higher in carbon and 0.04 per cent lower in silicon, but this is not considered to be of practical significance in the microscopic

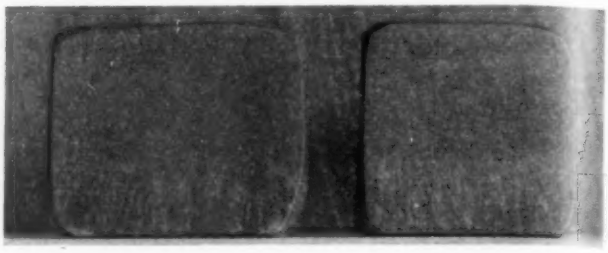


Fig. 1—Etched cross sections of unalloyed chilled iron wheels. Left, treated with silicon carbide, right, untreated. Actual size.

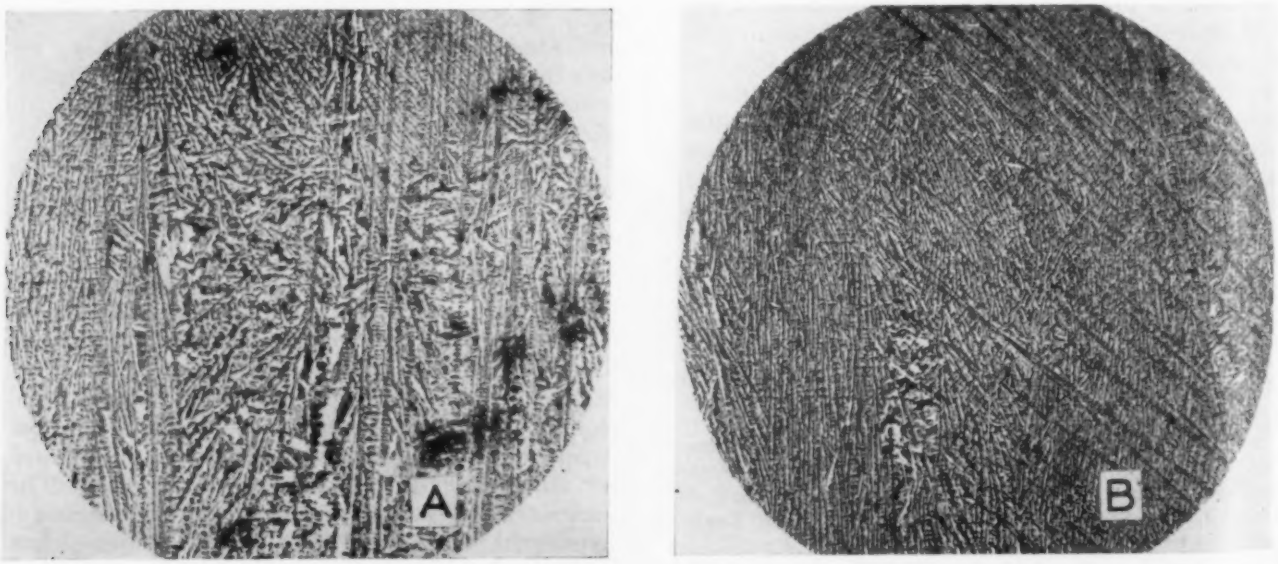
examination of the 1⅛ x 1⅜ in. cross-sections in view of the known fluctuations in chemical analysis of cast iron.

Both types of iron were made in the same cupola by the usual melting practice, except that, in the case of the silicon carbide treated iron, briquettes in the amount of 1 per cent of the 2000 lb. charge were added in the cupola, and some adjustment, such as the addition of more scrap or the use of a lower silicon pig iron, was made in the charge to allow for any silicon pickup.

Charge Contains Silicon Carbide

In using silicon carbide in the cupola melting of iron, the addition is made directly to the charge because solution is more readily effected than where the addition is made to the metal in the ladle. If the silicon carbide is properly briquetted, it remains intact until it reaches the melting zone. There the briquettes disintegrate and the silicon carbide goes into solution in the molten iron, the subsequent reactions resulting in the deoxidation and inoculation of the iron. The mechan-

Fig. 2—Comparison of chilled structures in untreated and silicon carbide inoculated unalloyed iron at 50 diameters. Left, untreated iron. Analysis: 3.68 per cent T. C., 1.44 per cent Si. Right, silicon carbide inoculated iron. Analysis: 3.83 per cent T. C., 1.40 per cent Si. Fig. 2a, 2b—chilled surface; Fig. 2c, 2d—⅛ in. below chilled surface; Fig. 2e, 2f—¼ in. below chilled surface; Fig. 2g, 2h—⅜ in. below chilled surface.



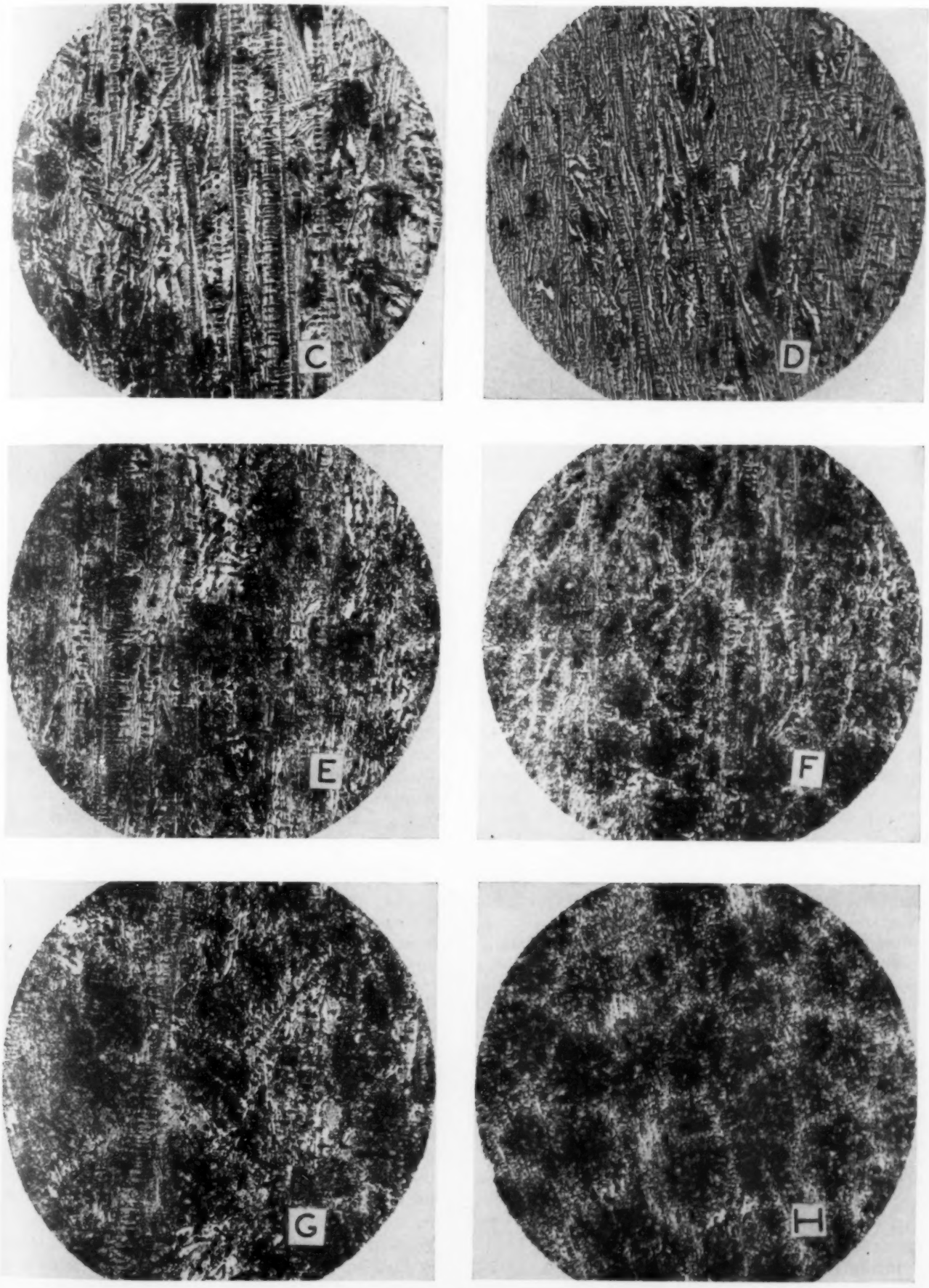


Fig. 2–(Continued).

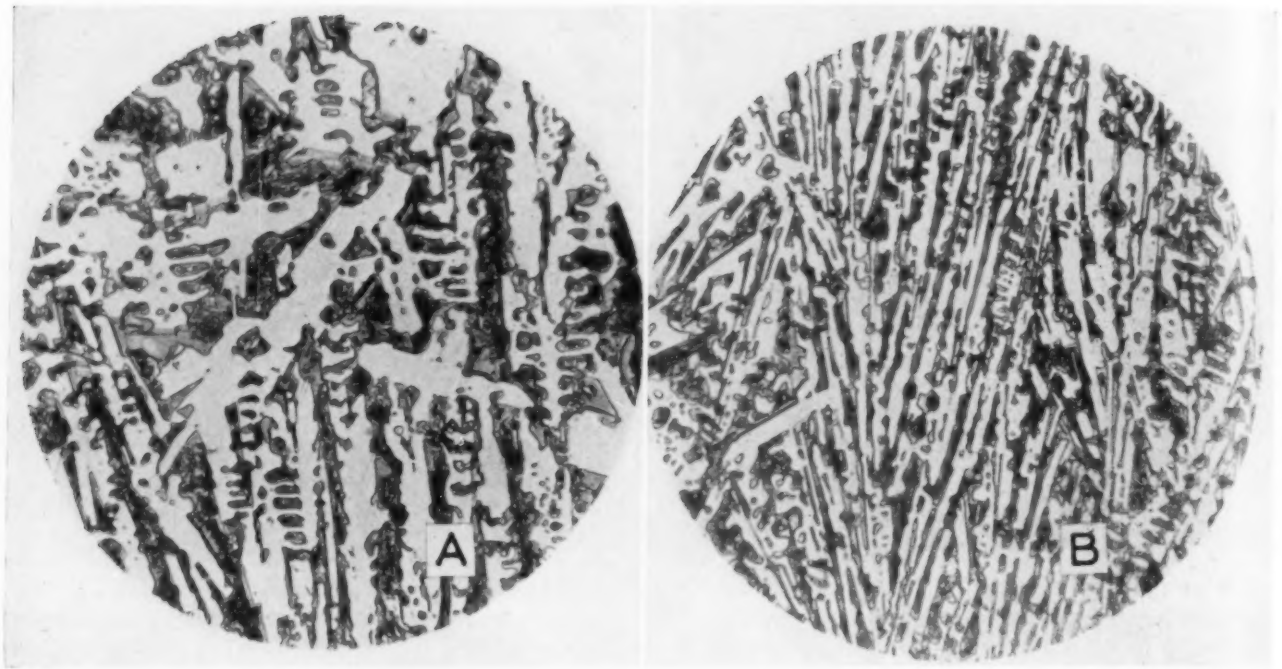


Fig. 3—Microstructure of unalloyed chilled iron at 500 diameters. A, untreated, B, inoculated with silicon carbide.

ism of silicon carbide inoculation is beyond the scope of this paper; suffice it to say that fine graphite nuclei in the molten metal are produced which promote the formation of the random type graphite. Due to the large number of nuclei formed, the graphite flakes are of moderate size and uniform distribution. A discussion of the thermodynamics involved is contained in another paper.[5] More complete details of the melting process and cupola operations will be found in a paper to be published later.

Next, to study the effect of silicon carbide inoculation on alloyed chilled iron, examination was made of several chill test wedges and tensile bars of brake drum iron possessing a nominal chemical analysis: 3.50 per cent total carbon, 2.10 per cent silicon, 0.90 per cent manganese, 0.70 per cent molybdenum, 0.72 per cent chromium. In this particular application, a comparison was drawn between the cupola addition of silicon carbide briquettes (in the amount of 1 per cent of the charge) and the combined ladle additions of 75 per cent ferrosilicon and a proprietary alloy containing silicon, manganese and chromium. This alloy was used in both iron mixtures to meet the specified chemical analysis, but the amount was cut in half when the silicon carbide addition was made in the cupola. The two iron mixtures were made according to identical practice except for the inoculating procedures specified above. The chemical analysis of the untreated iron was 3.51 per cent total carbon, 2.11 per cent silicon and that of the silicon carbide inoculated iron was 3.49 per cent total carbon, 2.12 per cent silicon.

In order to justify the presentation of the microstructural data as being representative of the sections considered, six cross sections each of both types of unalloyed iron, taken at widely spaced intervals in the respective castings, were subjected to the same metallographic examination. The same differences in structure were evident as those shown in Figs. 2 to 4. Similarly, a total of twelve chill test wedges were examined and exhibited the same differences in structure as are shown in Figs. 5a to 5f.

Discussion

Chilled iron wheels and brake drums, although being poured from metal having a uniform composition may, in their finished form, contain metal of widely divergent composition and physical properties. When a casting is poured against a chiller, the contact surface is first to solidify, then the metal immediately under it, namely, the mottle, and finally the gray iron interior. Differences in the time of solidification in the various parts of the same casting result in three types of metal: the chill proper, in which practically all the carbon is in the combined state (largely in the form of iron carbide); the mottled iron immediately under the chill, containing some graphitic or free carbon; and the interior of gray iron possessing the usual ratio of combined and graphitic carbon characteristic of ordinary gray iron castings.

A comparison of the representative chilled iron surfaces of the unalloyed iron wheels is made in Figs 2 and 3. It is obvious that the silicon carbide inoculated iron has a much finer primary carbide structure than the untreated iron, which contains extremely coarse massive primary carbide and a highly dendritic structure with marked planes of weakness along which failure can propagate. Failure of chilled iron castings due to impact blows struck in a plane parallel with the grain of the metal is much more prevalent in coarse grained metals than fine grained ones.[6] The refining action provided by silicon carbide inoculation is such as to produce a carbide structure which is known to

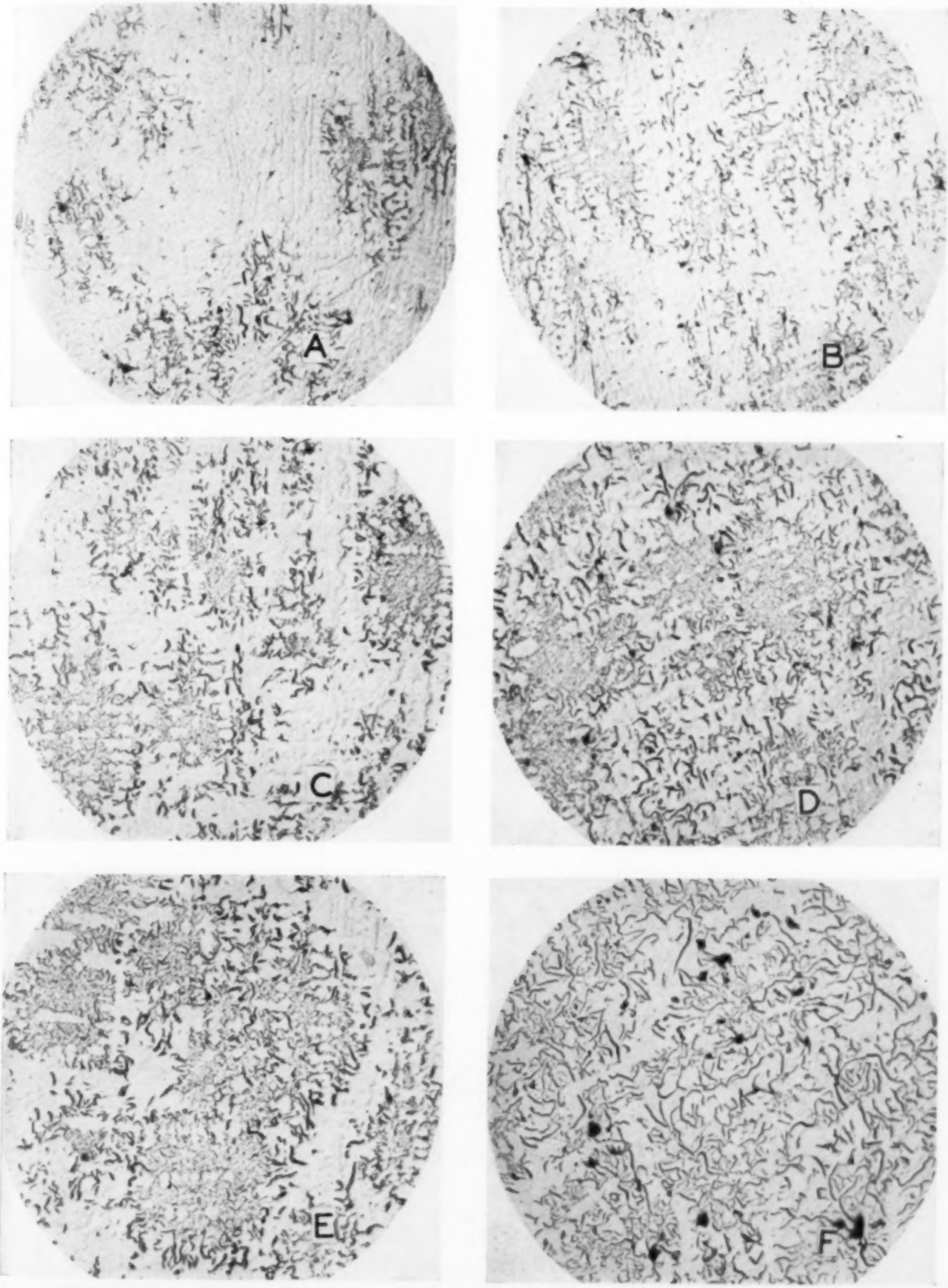


Fig. 4—Comparison of graphite structures in untreated and silicon carbide inoculated chilled iron at 100 diameters. Left, untreated iron. Analysis: 3.68 per cent T. C., 1.44 per cent Si. Right, silicon carbide inoculated iron. Analysis: 3.83 per cent T. C., 1.40 per cent Si. Fig. 4a, 4b—1/8 in. below chilled surface; Fig. 4c, 4d—1/4 in. below chilled surface; and Fig. 4e, 4f—3/8 in. below chilled surface.

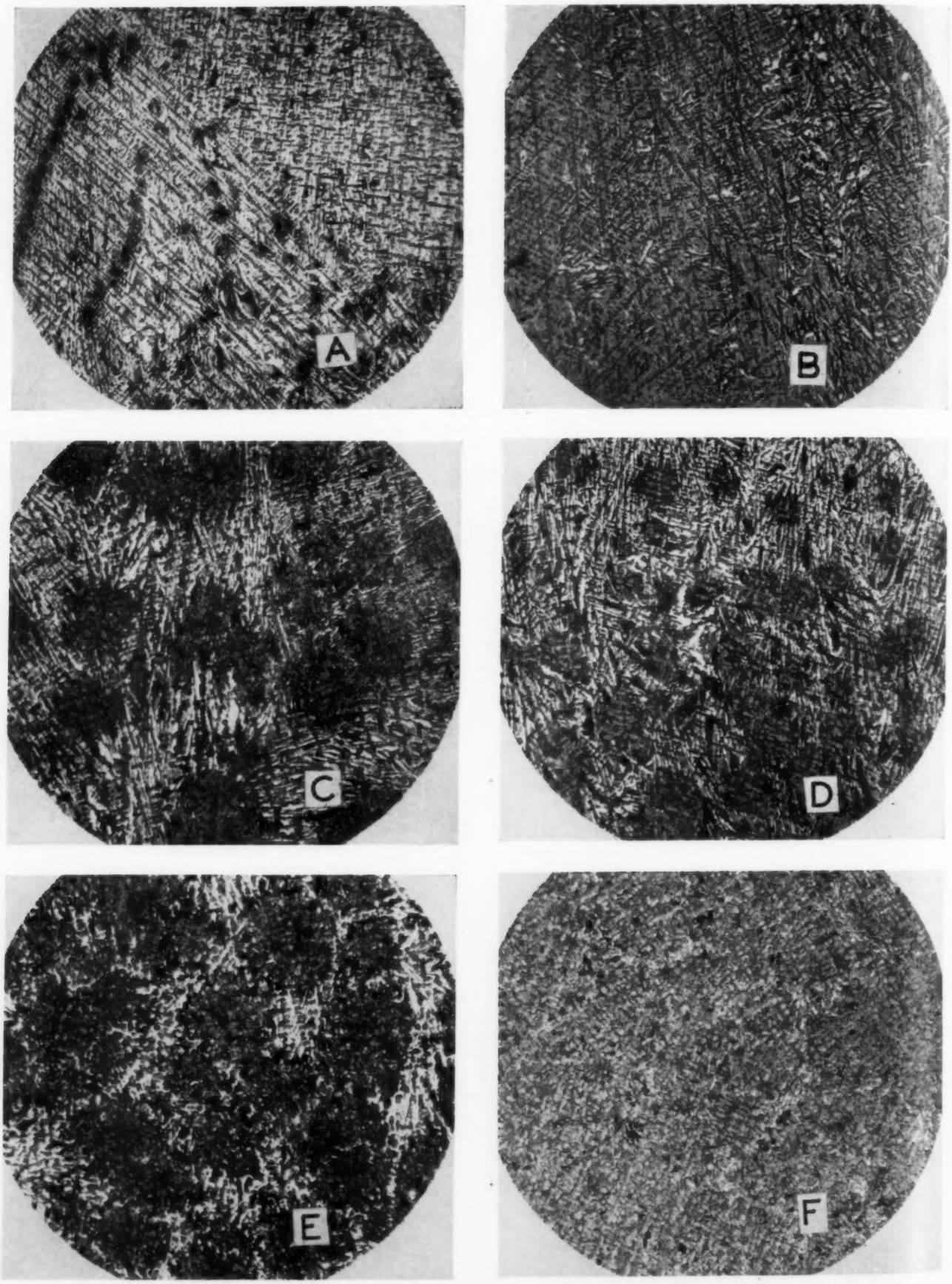


Fig. 5—Comparison of chilled structures in untreated and silicon carbide inoculated brake drum iron at 50 diameters. Analysis: 3.50 per cent T. C., 2.10 per cent Si, 0.90 per cent Mn, 0.70 per cent Mo, 0.72 per cent Cr. Left, untreated iron; Right, same iron inoculated with silicon carbide. Fig. 5a, 5b,—chilled surface; Fig. 5c, 5d—beginning of mottled zone; and Fig. 5e, 5f—1 in. below chilled surface.

shown a marked improvement in physical properties with but a very small loss in hardness or loss in wear resistance.[6]

Short Mottled Zone

In Figs. 2c to 2h comparative photomicrographs taken from identical relative positions as to distance from the chilled surface are shown. The significant aspect to be noted here is that the mottled zone of the silicon carbide inoculated iron is short compared to the untreated iron, thus providing a good backing for the chill. Inasmuch as the mottled zone in the latter is long and coarse, it will not provide the chill with as good a backing as in the case of the silicon carbide inoculated iron.

A shortened mottled zone is important because it is the weakest area in the structure and the most likely area where cleavage can occur. Therefore, by decreasing the mottled zone, an overall stronger iron in the three zones, chill, mottled and gray iron, is insured. At each distance below the chilled surface, the amount of primary carbide is less in the case of the silicon carbide inoculated iron and its tendency to form in a much finer and uniformly dispersed manner rather than in a dendritic columnar structure is quite evident. At the same time, the beginning of graphitization in the form of small clusters of graphite carbon is observed.

A comparison of the graphite patterns of the two irons at a magnification of 100 diameters is presented in Fig 4. The greater amount of graphite and its better distribution in the inoculated iron are evident at each point of the comparison—particularly at the distance ⅜ in. below the chilled surface where the untreated iron still possesses a fine interdendritic graphite pattern, rating 7D, 6E, while the silicon carbide inoculated iron has attained a more random type of graphite with uniform distribution, rating 5A.

The influence of undercooling on the graphite pattern of gray cast iron has been adequately described by Schneidewind and D'Amico.[7] Their data prove that the lower the solidification temperature, the finer will be the graphite and the greater the tendency for a eutectiform pattern. Still greater degrees of undercooling will inhibit the precipitation of graphite to form mottled and white irons. It is obvious from the study of the microstructure of the unalloyed chilled iron wheels that silicon carbide inoculation has actually improved the overall physical properties owing to its ability to forestall undercooling and to promote a better distribution of the graphite pattern. In other words, it has reduced the tendency to form a deep chill with its attendant undesirable graphite distribution pattern.

Alloy Chilled Iron

The effect of silicon carbide inoculation on the chilling tendency of an alloyed chilled iron is illustrated in Fig. 5. The securing of hard, wear-resistant surfaces in comparatively heavy sections by control over the iron composition is difficult because of the close limits required. Even under excellent operating conditions, variations in composition produce marked variations in depth of chill and sometimes even mottling of the wear-resistant surface, which may then subject the piece to rejection. In this particular brake drum iron considerable amounts of chromium and manganese were used to stabilize the pearlitic carbide so that in the heavy sections the cementite of the pearlite would not decompose into graphite and ferrite. Also, additions of molybdenum and chromium were made materially to refine the as-cast columnar structure of the chilled surface.

Figure 5a is typical of the chilled surface of the untreated iron. The structure contains primary carbides in a highly dendritic structure possessing definite freezing lines along which failure can propagate. Similarly, and at the same magnification, Fig. 5b is typical of the silicon carbide inoculated iron. It is quite apparent that the primary carbides are much finer and uniformly dispersed. The structure is devoid of marked columnar tendencies and planes of weakness along which failure can readily progress. Furthermore, such structural refinement is known to impart improved physical properties in the iron.

In the case of regular chilled iron possessing a coarse dendritic structure, the practice to improve physical properties has been to heat treat at relatively high temperatures (1500-1600 F) to accomplish the partial re-solution of coarse primary carbides with their subsequent re-precipitation on cooling so as to produce a fine and more uniform grain structure in the chill.[6] Figures 5c and 5d represent the structures of the two irons at the beginning of the mottled zone, and Figs. 5e and 5f the structures obtained 1 in. below the chilled surface in each iron. The importance of silicon carbide inoculation is readily evident in a comparison between these photomicrographs. The mottled zone of the untreated iron is long and coarse and persists even to a point 1 in. below the chilled surface, whereas the silicon carbide inoculated iron, at the same relative position, is devoid of a mottled structure.

It is known that when the mottled zone is long and coarse it will not provide the chill with a good backing. In the silicon carbide inoculated brake drum iron, the gradual transition from white to gray iron is a relatively short one which does not involve the presence of a long coarse mottled zone between the chilled surface and the gray iron interior. Failure of chilled iron castings because of impact blows struck in a plane parallel with the grain of the metal is much more prevalent in coarse-grained irons than in fine-grained ones.[6] Furthermore, the need of heat treatment for the refinement of structure and improvement of the physical properties is not necessary in the case of the silicon carbide inoculated iron.

In order to compare the relative qualities of the two alloyed iron mixtures tensile test bars were prepared and tests made according to ASTM standard specifications. The results of these tests indicated that the inoculated iron possessed an average tensile strength of 39,500 psi. compared to 33,900 psi. for the untreated iron; the Brinell hardness was 220 compared to 230. A significant area of each bar was observed microscopically in order to justify a comparison between the respective microstructures. It was observed that, while the graphite distribution in both irons was similar, a very noticeable difference in free

carbide separation was evident. The untreated iron had large areas of coarse free carbide spread throughout the entire cross-section, while the silicon carbide inoculated iron had only a few isolated, comparatively small free carbides spread at random. The preponderance of free carbide in the untreated iron provided the higher hardness which was obtained.

Summary

1. In the present paper, the ability of silicon carbide inoculation to reduce the chilling tendency, to improve the overall physical properties, and to produce a final cast iron of uniform character is discussed with particular reference to a commercially made unalloyed iron possessing a total carbon content in the range 3.68 to 3.83 per cent and to an alloyed brake drum iron analyzing 3.50 per cent total carbon.

2. The normal chilling tendencies of unalloyed and alloyed chilled irons can be controlled with silicon carbide inoculation. In the particular irons considered in this investigation, silicon carbide inoculaton produced an improved chilled surface structure in which the primary carbides are much finer and uniformly dispersed. The chill is devoid of a highly dendritic structure with its attendant planes of weakness along which failure can propagate. The refining action provided by silicon carbide inoculation is such as to produce a carbide microstructure which is known to possess improved physical properties. In chilled iron this means maximum toughness, strength and resistance to impact.

3. Of equal importance is the fact that silicon carbide inoculation has reduced the depth of chill and provided a shortened mottled zone in both irons. Thus resistance to spalling from thermal or mechanical shock has been materially increased. Furthermore, the need of heat treatment for structural refinement and improvement of physical properties is not necessary in the silicon carbide inoculated irons.

Acknowledgement

Grateful acknowledgment is made to Mr. M. J. Kirsch of Kerchner, Marshall & Company for supplying the irons and to the Carborundum Company, sponsor of the investigation, for permission to publish the results.

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DISCUSSION

Chairman: H. BORNSTEIN, Deere & Co., Moline, Ill.

Co-Chairman: J. E. BOWEN, Chevrolet Gray Iron Foundry, General Motors Corp., Saginaw, Mich.

DR. A. E. SCHUH[1]: Will you please define the term "inoculation" again. It is unusual to define as inoculation a procedure of melting in which an addition agent is melted with the charge in the cupola which is then directly cast off. This is contrary to generally accepted ideas.

CHAIRMAN BORNSTEIN: Dr. Schuh brought up a good point on nomenclature. According to the A.S.T.M. definition, inoculants are added to molten cast iron.

D. F. LUNSFORD[2]: The carbon content in the unalloyed iron was 3.68 per cent while the carbon content in the inoculated iron was 3.83 per cent. What, in the opinion of the authors, would be the affect of higher carbon content in the unalloyed iron toward producing the same chill tendencies as that obtained in the inoculated iron?

MR. LORIA: As a general rule, all other conditions being the same, the tendency to chill increases with decreasing total carbon content. The figures quoted for the unalloyed cast iron are ladle analyses and do not really represent the actual carbon contents in the several metallographic samples at the point of examination.

We believe that the addition of a strong deoxidizer such as silicon carbide to a cupola iron mixture can be correctly termed inoculation for deoxidation and inoculation are interrelated phenomena. In any event, the significant aspect of the inoculation process is not really manifest in the time or manner of the addition but in the beneficial results achieved.

The addition of silicon carbide during melting of the charge in the cupola effects a marked decrease in the dissolved oxygen in the iron which is a carbide stabilizer and therefore a chill inducing element. This reduction in chilling tendency is accompanied by the elimination of fine undercooled graphite for normal cooling rates and its replacement by flake graphite with significant improvement in physical properties of the iron. The general viewpoint of the authors with respect to the relationship of deoxidation and inoculation is substantiated by the recent contributions of Somers and Gunther[8] and Morrogh and Williams.[9]

The reduction of chill depth by the use of silicon carbide can then be explained partially on the basis of deoxidation. In addition, silicon carbide as a graphitizing inoculant can also function as a nucleating agent in the melt. Nucleation o fthe melt merely affects the number of centers at which eutectic crystallization begins, and as this determines the size of the eutectic cells it has an indirect effect on graphite size. Boyles[10] has demonstrated that the eutectic cell size is greater and that these cells are much less numerous in undercooled irons than in flake graphite irons. The number of eutectic cells in a given sample precisely indicated the number of centers at which eutectic crystallization begins. If undercooling occurs owing to the absence or deficiency of nuclei, then fewer eutectic cells are to be expected.

[1] U. S. Pipe and Foundry Co., Burlington, N. J.
[2] Perfect Circle Corporation, New Castle, Ind.

MANAGEMENT LOOKS AT TRAINING

Richard S. Falk
The Falk Corporation
Milwaukee, Wis.

ABSTRACT

The author discusses the need for and desirability of continuing the conventional apprenticeship training programs. "Learner" or "trainee" programs cannot supplant the well-rounded indentured apprenticeship program. It should not be altered by the varying day-to-day economic situation.

EVERY WAR, in which our nation has been involved, has called the apprentice away from his bench and has brought short-cut and substitute training methods into play.

After each war period there has been doubt raised by more than a few that apprenticeship would ever recover, and many reliable and informed businessmen expressed their feelings that the quick training methods, utilized during the war period, would prove better adapted to meet the manpower needs of a more highly mechanized postwar industry.

Yet, after each war, apprenticeship not only recovered but increased in numbers, quality, and importance. World War II's post-era will be, in the author's opinion, *no* exception.

American industry swung into the late '30s spearheaded by a full, quality apprenticeship program, and a foreman group heavily made up of apprentice program graduates. But onto this apparently sound situation certain foreboding shadows fell. Upgrading of recently graduated apprentices to foremanships was proceeding too rapidly; the important 5 to 10 years of "seasoning after training" was skipped—these new foremen were not ready and many failed; some relied on cockiness to cover inexperience and hurt their prestige in the eyes of their men; others were so timid and not-sure that their departments ran away with them. The rapidly increasing business volume allowed plants to add many apprentices, as each month passed, and the long work hours shortened the training period and opened up many more places for new apprentices. This also had the bad effect of graduating boys with the proper number of job-hours but not the proper number of growing-up hours. Finally, the pressure of getting out the work and the scarcity of manpower did not allow the usual careful selection of apprentices and the pressure of unqualified young employes in plants to get on an apprenticeship was considerable.

The end-result of these unsound events was suspended for five years, as most of the principle characters went to war.

As 1941 moved into '42, and '42 pushed into '43, an entirely new training program first supplemented and then replaced the pre-war apprenticeship program.

Machine operators, and foundry technicians, usually furnished from the apprenticeship ranks had to be made in greater volume, and more quickly than the formal plan could supply them. Companies installed the "learner" or "trainee" program. Operators were trained for one type of operation in 6 to 18 months.

During this period, all the basic fundamentals of true apprenticeship were missing. The ages of these learners varied from 16 to 40; while the ages of our pre-war apprentices was 18 to 22. Only a verbal understanding existed between trainee and employer; absent was the formal contract and a supervising state agency. Pay scales were close to the bracket minimum of the job, instead of an independent wage scale set up strictly as a training compensation. School hours were at a minimum and usually handled by an in-plant program, instead of being a large and important part of the program conducted by a community educational institution. Strangest of all differences—our new "employes in training" wore slacks or dresses.

No one can doubt that the war program did its job—people were at least trained well enough to turn out a record production. Some will say it was due to "force of numbers" rather than skill or quality. Some will say it was the repetitive nature of the work that saved the day. There may be some truth to these statements, but a good job was done.

During the war period, many well-respected men of industry and training made quite definite statements that we would never go back to apprenticeship training as we knew it; and they gave their reasons:

1. The short-term training program had produced operators capable of doing the expected job.

2. Industry, and its mechanisms, had become so standardized and simplified that the need for initiative and decision was limited.

3. Production-line type operations and standard "long-run" jobs gave little opportunity for mechanics to move from one operation to another, or to handle a variety of work, or the need for this ability.

4. Young men, with the necessary schooling to enter apprenticeships and trained so thoroughly in them, would never be willing to remain "just machinists, molders, pattern makers," etc.

5. The restless spirit of the postwar era, and the age of the returning veteran, would work against a settling-down to the serious business of, and temporary sacrifice of, apprentice training.

Still, there were some who disagreed with this fatalistic viewpoint and saw a return to actual pre-war apprenticeship programs.

Neither group was entirely correct, but this latter view was more nearly accurate. We are now nearly 18 months from V-J Day and we seem headed for a completely normal period in the training field and a re-establishment of pre-war apprentice standards: Training directors moved towards the re-establishment immediately after peace—they were aided by government subsidies to the veterans (something which was later to rebound); they were further aided by healthy industrial activity and overcrowded schools; on the other hand, they were handicapped by the "52-20 Club" spirit and the rise of the fake programs and conspiracies between some employers and veterans to obviate the G.I. Bill of Rights. The latter resulted in restrictions and cuts in benefits which retarded the return to normal apprenticeship by forcing many veterans to drop out of the training programs. Besides those who entered on training programs for the first time, there were those boys whose programs were disrupted by the war, and those high school graduates who could not get into colleges and hence turned to apprenticeship to further their education. These groups were sufficient in number to fill most plants' needs.

In December, 1946, some 50 training directors of leading American industries met in New York to discuss apprenticeship, to take stock of the program, and to discuss its future. No one winked at the problem we faced, but still all expressed complete faith in the apprenticeship program and confidence that the war had done nothing to seriously alter our conception and "plan of training." There was some concern over the attitude of those returning veterans who seemed to prefer the "52-20" clubs to work, some concern over the young men who failed to see the value of the apprenticeship future in view of high wage jobs which were so plentiful; some concern over the rapidly rising cost-of-living which was working great hardship on the fixed-income training programs.

Against these obstacles, there were many bright rays. Industry needed many well-trained men and the opportunities in top administrative and supervisory positions were many. The number of good young men applying was quite satisfactory. Management was more interested in, and more solidly behind, these training programs than ever before; and training directors were eager and enthusiastic.

In passing it is interesting to note that this conference group was almost unanimously opposed to shortening the standard programs, but instead favored making their programs better, more intensive, and to include new subjects and hours devoted to management problems, business economies, etc.; and in this way take advantage of new training methods (i.e., visual education, technical short-cuts, war procedures).

The writer previously mentioned that the 50 training directors saw certain obstacles endangering the future of apprenticeship. These obstacles took the form of "things to prevent" and "things to be done" in the interest of training programs. Apprenticeship is surely on its way to normalcy, but in order to make sure that it reaches this healthy goal, we must take some affirmative action and avoid dangerous errors of the past:

1. Continue to keep apprenticeship distinct from learner programs and regular shop jobs—holding strictly to the plan of moving the apprentice from one operation or phase of work to another and giving the boys plenty of attention and keeping them always aware of their favored position. Some companies have allowed their apprentices to become mere production workers under the department foreman.

2. In view of the earnings' sacrifices made by the apprentice, we must give other compensations—opportunities for learning and for information not available to other employes.

3. Selection must be strict, maintenance of standards by the apprentice must be watched, and those who fail to stand up to the program must be removed before graduation.

4. Apprentice graduates must *really* have opportunity in our companies so the proper incentive is there for those in training—we must do everything possible to hold on to the boys we trained, both from the selfish angle (the great cost to us to put them through), and from the angle of confidence in the program.

5. Never cease in our efforts to improve our programs and the caliber of our boys in training whether economic conditions force reductions in numbers, or allow full programs to operate.

These points refer to apprenticeship in general, including foundry operations, but there are some special points which are tied to foundry alone:

1. Safety and health

2. Elimination of antiquated systems which call for human back-breaking

3. An interesting apprentice program and the guarding against using these "boys in training" for just extra foundry hands

4. Selling program to high school and college graduates—movies, talks, tours.

Apprenticeship is the cornerstone of Industry's *future* planning, it is an investment by the young apprentice in his future—within the realm of absolute ability it must not be altered or influenced by the day-to-day situation in our business. Such an attitude is neither sound nor fair.

DISCUSSION

Chairman: **F. C. CECH, Cleveland Trade School, Cleveland**

Co-Chairman: **B. D. CLAFFEY, General Malleable Corp., Waukesha, Wis.**

P. M. SANDERS[1]: In various parts of the United States today we are confronted with two particular problems: First, the high-productivity industry, which has hit the foundry to a great extent, and the industry which makes 1, 2, or 50 or so of one particular type of casting or part or whatever it is.

[1] Consultant in Metallurgy, Detroit

In the high-productivity industry, the apprentice holds the position of being a trainee for supervisory jobs. That particular factor is overlooked in some cases in various foundries in the Detroit area, where most of the work is high-productivity. The trainee, on the other hand, fits in well in the high-productivity industries because the short-term training which is given does have enough in it to permit an individual to go into a shop where most of the work is mechanized, for instance, molding.

All of the molding machines are highly productive and the engineering department or supervision tell the workers how many rams to give a mold, how to turn it over, how to draw the mold and how to put it together. They are told possibly how to set cores, which will be repetitious from one day to the next, possibly changing on some days to different patterns, but then the same patterns keep on recurring.

However, the apprentice program is still a necessity in order to train foremen and supervisory individuals. All supervision cannot come from engineering schools. There are some engineers, as has been shown through aptitude tests, who would be absolutely worthless in supervisory jobs, because they are of the type which must have constant problems presented to them, and the supervisor on a molding job or on a pattern-making job should know the skills which go into those particular lines to be able to direct the activities of the individuals who are actually doing the work, and also to keep things running so smoothly that there are not too many problems.

It seems to me, therefore, that both the apprentice training and the trainee programs are both necessary in the industry as it is shaping up today.

HENRI LOUETTE[2]: Where a pension plan is in force is it necessary to have the apprentices indentured?

MR. FALK: Whether you have an apprenticeship or not, management should look to the feasibility, within their pocketbook, of establishing some kind of security plans, regardless of the apprenticeship. In plants where you have those things, it is an added incentive for somebody to put in their time and their effort and sacrifice some money for training, if they know that after they have moved up through the line, they are going to have some security ahead. I think that is true. It is an added incentive.

A. W. GREGG[3]: The mechanized foundry is the father of the so-called trainee. I wonder if Mr. Falk would comment differentiating between the trainee and the indentured apprentice. My idea of the indentured apprentice is that we must have some mechanics, that you cannot take a man who has worked on a molding machine for maybe 10 or 12 years and make a foreman out of him. He understands one particular class of work, but for the all-around foreman, we must have a man who is trained from every angle of molding rigging or sand. What do you do with your so-called trainee, and what do you do with your indentured apprentice, that you do not do with your short-time training man?

MR. FALK: The programs have to go side by side and I did not mean to leave any feeling that the learner program should be cast aside, because we have moved out of the wartime period. For industries of our type, large job shop type, the indentured apprentice is more valuable than the trainee. The all-around trained man is possible timber for supervision; that, we all agree on. Let us say he does not attain supervision. I still think that a man who is informed is a lot happier than a man who is not informed or only partly informed. In other words, an apprentice graduate still can be just as good a man staying on the job as a Class A molder.

All jobs become routine after a certain length of time. They may look good before you get into them, but when you finally get there, you must all admit there is a certain grinding routine that all of us go through, and it is only by the releases of hobbies and the releases of our own intelligence and training and the grasping of increased knowledge that we make ourselves happy in our productive activities. So I see no great harm in some all-around training apprentice program, all-around training program, even for those men who perhaps must stay on one job later on.

MR. GREGG: What is the length of time allotted for these so-called trainees, these speeded-up apprenticeships? Is it a matter of months or weeks?

MR. FALK: It varied considerably. On some small core-making jobs, I suppose it was six weeks. We started with about 18 months, or a year, as against the four-year period. Then we kept cutting it down to almost nothing.

MR. CLAFFEY: Mr. Falk referred to two conditions. One is the crowded condition of our schools at the present time, which makes it difficult for some of our youth to go on to further education and makes them likely timber for the regular indentured apprenticeship program. On the other hand, we have the returned serviceman, who may be anywhere from 18 to 30 years of age. These fellows are coming into our plants to study and learn through on-the-job training programs in some cases, and in other cases to come in and be learners.

In the case of the service men, they usually are interested primarily in absorbing as much knowledge as they can in the shortest possible time, because they are confronted with their economic problem. In many cases they have families. That presents a serious problem in the program of training people for our industry.

A. L. GREDE[4]: The program we have had under way for about 18 months now has to do with selection of men for top positions in management and not for the supervisory staff. We decided that it was time we were considering selecting some high type individuals, young men who might be trained to take over not the superintendent's job, not the foreman's job or the department head's job, but the president's job, or perhaps as low down as the works manager, or the treasurer or the comptroller or some of those jobs.

We decided that we would select veterans who had been officers in the Army, Navy or Marine Corps and that we would select those men by aptitude tests. In checking these test methods, we decided to have our executives tested first. So all of those on the executive staff were first tested by a firm that makes a specialty of doing that.

After we had seen the results of those tests, we decided to go further and test our department heads and superintendents. We decided that the tests were so promising and showed up some things that we were able to correct to advantage and we have not had our foremen tested. In connection with the new plant that we opened early this year, we decided to try the testing program for the entire personnel of the plant and did so, with very satisfactory results, so much so that we are now giving very careful consideration to the advisability of using this complete testing program on all applicants for all jobs in all of the divisions that we operate.

In connection with this special training program, we selected eight men. We rejected many, but selected eight men. The specifications were that they were to have been officers, and preferably higher officers, and that they were to have at least a college degree or to be college graduates. Of the eight men, one man had only one year of college but he was a captain in the Army, in charge of maintenance of equipment for a regiment which finally reached the South Seas. We selected him as an exception to our rule of having only college graduates.

We decided to put these eight men through all the operations, especially the plant operations, in the sales department for some training and for brief training in the accounting department. We moved them about in what were then our four divisions, two steel foundries and two gray iron foundries. These men actually worked. They were given hard manual jobs to perform. They did laboring, molding, coremaking, chipping and grinding. They were moved about in the departments under the supervision of our regular staff. The entire program was under the supervision of one of our works managers.

As can be expected in a program of that kind, the really difficult job was to keep men in the training program for as long as it was originally intended that they should be, because it was not long before one department head or another was reaching for one or the other of these men and we finally decided that because of the urgency and the need for men in supervisory positions, we would shorten the program, if necessary and where the need was urgent. We did shorten it to between 12 and 15 months. There are some men who will go through for the whole 18 months, but most of the men have now been selected for positions and we are pleased, proud and satisfied with ourselves about the type of men we have selected.

[2] Warden King, Ltd., Montreal, Que., Canada
[3] Whiting Corp., Harvey, Ill.
[4] Grede Foundries, Inc., Milwaukee

There was only one casualty and that was not because of lack of ability but because this one man, who has had one year's work in college, decided, after fraternizing with these other seven men and seeing what he might have missed in school, decided to leave and go to school and finish his schooling. That was the captain in the Army.

We had a man who was a lieutenant, senior grade, in the Navy, who is now the superintendent of our Liberty Division, gray iron foundry. We have another man who was a lieutenant, junior grade, who is the personnel director at our Spring City Division, gray iron. We have another man who is now the production manager at one of our steel foundries. We have another man who was a metallurgist who is on the sales force.

There have been some rather interesting developments out of this. Men who thought that they would like to be in manufacturing have in one or two cases taken a very great liking for sales. Some who thought they would much prefer to be salesmen when they started the course have thought now that they would like to be in manufacturing and have shown certain aptitudes in that direction.

We have a man now who is in sales. He has not completed his training and has not been selected for any particular place, but this particular man has had metallurgical training and thought he would like to be in metallurgy, but now he has decided if there is any possible way of doing it, he would like to be on the sales force. He likes selling.

We have one man who is an accounting major and who was a lieutenant, senior grade, who is now managing our new Iron Mountain Division, a gray iron foundry. We have a man who was a lieutenant colonel who is in charge of standards and production. That is, he manages and schedules the production and is in charge of the standards, time study, motion study, etc., at our Iron Mountain Division.

Since the origin of the program, we thought that eight would be quite a few for our organization and that perhaps we might not have the places for these men, and we even expected to have some casualties, but we are sorry now that we did not start the program with about twice as many. Since then we have put two more men in the program, both with the same qualifications, one rather young fellow who was graduated from college at an early age. He is only 21. The other men were in the age group of about 25 to 29. But we are again looking for a few more trainees, because we find that this has worked out so well that we would like to continue with the program with another group of trainees.

We are definitely thinking that these men, with the possible exception of two of the remaining seven, that five men out of the eight originally selected are definitely candidates for positions in the top management of our organization.

MR. CLAFFEY: Has there been any thought given to the problem of how you will differentiate or how you could qualify between the college man and the graduate apprentice in establishing qualifications for a supervisory job?

MR. FALK: I think there are two important qualifications that a supervisor must have. The first is an ability to know people, to live with them and to help them help themselves. With all due respect to your college graduate coming into the industry, I think you will find some of your top-notch apprentice graduates are going to meet that bill a little closer.

Your college graduate is naturally going to have the advantage when it comes to moving into a more technical position. Generally, I would say that the college graduate will move into your more specialized and technical fields, although we expect several of ours to remain as supervisors. We want some of them in supervisory capacities. But they will probably have to learn what may come more naturally to the indentured apprentice, and that is this very important subject of men. I think that the two can go along side by side and that one can make up what he lacks by hard work, as can the other. You will find perhaps the indentured apprentice may be working in the research division of your plant and a college graduate winding up as a supervisor over a cleaning floor, and in the same breath you will find the college graduate more normally moving into the technical field and your apprentice graduate moving in as a supervisor of men. So I think they can move simultaneously and both of them have a good opportunity because they are two important elements.

D. TEMPLETON[5]: We are contemplating starting an apprenticeship program in our pattern shop.

V. J. SEDLON[6]: Our experience is all that I can draw from and I know that it has been working out satisfactorily. It is a program that is sponsored by the Federal Apprentice Training Commission, in cooperation with the local union, and the jobbing industry in our city; these are all coordinated.

A program has been drawn up at the Cleveland Trade School by Frank Cech. That program worked out satisfactorily, because we have so many people interested in it. We have the cooperation of the union, which is definitely interested in the boy's welfare and progress, and then the shop committee, too, is an active participant in the training of the boy, seeing that he goes to school nights. Then there is our own interest as employers, watching that boy's progress, contributing to the school program by talks. These talks are very important in getting the boy closer to the industry. The group sets up the program and, of course, the instructor's past experience helps a great deal. The boy gets a very good, rounded-out education under a four-year program. He gets all the related subjects, such as shop mathematics and human relations and many other things that are essential to the boy's growth.

CHAIRMAN CECH: Our problem in the pattern-making industry is that we have so few apprentices. It is a highly specialized industry and, consequently, we do not have 30 or 40 apprentices as in the machine shop or the foundry and it is hard to concentrate the program. If you have a sufficient number of apprentices, it will pay you to put in a special program of your own. If not, and you have job shops and probably some captive shops that are interested in apprentice training, it will pay you to try to get a cooperative plan functioning.

Your Apprentice Training Service is ready and willing to do whatever they can. Their primary interest is in drawing up the contract with the boy and the owner or the jobber and then to see that he attends school and to see that he gets the proper progress in doing the operations that are included within the pattern-making industry.

H. K. SWANSON[7]: In the Chicago area we have the same procedure as you have in Cleveland and in Detroit, only we work ours just a little bit differently. Our boys are indentured by the union and the jobbers of Chicago and the government. These boys go to school one day a week. They must put in eight hours and they are taught in this school, mathematics, blueprint reading, blueprint layouts, mechanical drawing, and foundry work but they are not taught pattern making.

The reason we say they do not teach them pattern-making is because we want to teach that in the shop. We tried several years ago incorporating pattern-making in the schools, but when we brought them back into the shop, we found we always had an argument over methods of pattern-making. We soon took that out and stopped that in the schools, but we do have them in the schools teach them everything in school but pattern-making. We give them a lot of mathematics. You never can get too much of that into them. They get mathematics and they make molds and they make foundry work.

They go to the Washburn Trade School, which is operated by the Board of Education of the City of Chicago, and they go to that school from the entire Chicago district. Regardless of whether you have one boy or five boys in your shop, they all go to that school.

MR. GREDE: Perhaps it depends upon the teacher and the school as to whether the boys turn out to be satisfactory pattern-makers after having attended a school where they have taken up pattern-making. It has been our experience that the boys that we have secured from the Boys' Technical High School in Milwaukee have been the most satisfactory pattern-makers that we have had.

MR. SWANSON: I have been a foreman in a shop for 22 years and have had men call themselves pattern-makers and they did not know what a sweep core box was. They saw a sweep core box in the shop and asked me what it was. They were supposed to be pattern makers who had served their apprenticeship in shops in the Detroit area where much big work is not made, and all of their patterns are for production. So they had no experience in jobbing foundry practice where sweep core boxes are used for one or two castings.

[5] Allison Bedford Foundry, Bedford, Ind.
[6] Master Pattern Co., Cleveland
[7] Swanson Pattern & Model Works, East Chicago, Ind.

EVALUATION OF CORE KNOCKOUT

Report of A.F.A. Subcommittee on Physical Properties of Iron Foundry Sands at Elevated Temperatures

ABSTRACT

The subcommittee has completed a series of foundry and laboratory tests of cores to study the relationship between the behavior of cores during shakeout and known laboratory tests made at elevated temperatures. The first correlation completed is the relationship between time required to knock out a core from a hollow test casting and the retained compressive strength under own atmosphere test conditions of a core test specimen. Correlation between hot compressive strength and core knockout time was also found.

Introduction

THE PROBLEM of selecting or devising a laboratory test which will predict the difficulty likely to be encountered in removing cores during casting shakeout seemed most likely to be solved by a comparison between foundry tests and laboratory tests. Accordingly, the Subcommittee devised a hollow cylindrical test casting to be molded in green sand and cored by a dry sand core. In addition to pouring castings of two wall thicknesses, the Subcommittee simulated floor type foundry practice and mechanized foundry practice. This was done for floor practice by allowing the molds to stand two hours after pouring before removing the castings. The cores were knocked out 5½ hr. after pouring. To simulate mechanized practice the castings were removed from the mold one-half hour after pouring and the cores were knocked out immediately thereafter.

Test Casting

The design of the test casting selected to measure the time in seconds of the core knockout is shown in Fig. 1. A light weight cylindrical casting was selected due to uniformity of heat input to the test core inside of the casting and ease of observation when core was completely removed from the casting.

Castings having ¼ and ½-in. wall thicknesses were made. The former represented light casting practice, while the latter gave data showing the effect of a heavier metal wall, by subjecting the core to greater heat.

Core Mixtures

The Subcommittee was confronted first with the task of selecting a group of core mixtures which would be representative of iron foundry practice from light to heavy castings and also give a wide range of core knockout time. The core mixtures selected are tabulated in Table 1. The core mixtures, in addition to yielding a wide range of knockout time, also employ many of the core binders commonly used in the iron foundry. All core mixtures were prepared in a 36 in. muller type mixer.

Pattern and Molding Equipment

The foundry test cores were made with the following equipment: wooden core box with a metal sand hopper, an 18.5 lb. ramming weight, aluminum dryer plates, and a jolt machine as shown in Fig. 2. All cores were baked in a gas fired core oven with automatic temperature control and mechanical ventilation. In Fig. 3 is shown a view of a baked foundry test core. The drag pattern was of wood attached to an aluminum drag plate with mounted metal gate as shown in Fig. 4. Two patterns were available, one for the ¼-in. and the other for the ½-in. metal section. The cope pattern and plate were of aluminum and aluminum flasks 13 in. x 13 in. were used. Drag flasks were 10 in. deep and cope flasks were 4 in. deep.

Core Making

All of the steps in core making were detailed in an outline which was adhered to in the foundry. The procedure followed is described below.

Core Mixing

1. Weigh all materials used in mixture.
2. Mix as follows:

No. 1 Core Mixture

Dune sand and oil. Mix 4 min., add water, mix 6 min.

No. 2 Core Mixture

Dune sand, bank sand and cereal. Mix 2 min., add water, mix 4 min., add oil, mix 4 min.

No. 3 Core Mixture

Dune sand, silica flour, western bentonite and cereal. Mix 2 min., add water, mix 4 min., add oil, mix 4 min.

No. 4 Core Mixture

New Jersey gravel, heap sand and pitch. Mix 2

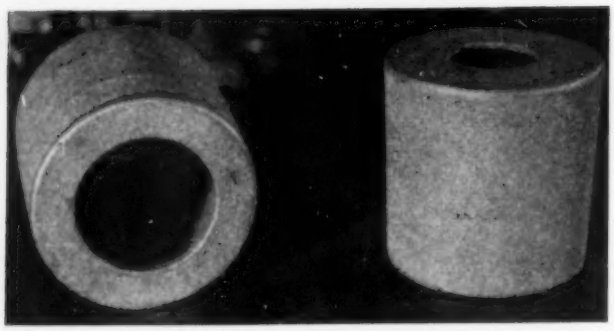


Fig. 1—Drawing foundry test casting (right) and photo of foundry test casting (above).

min., add water, mix 6 min.

No. 5 Core Mixture

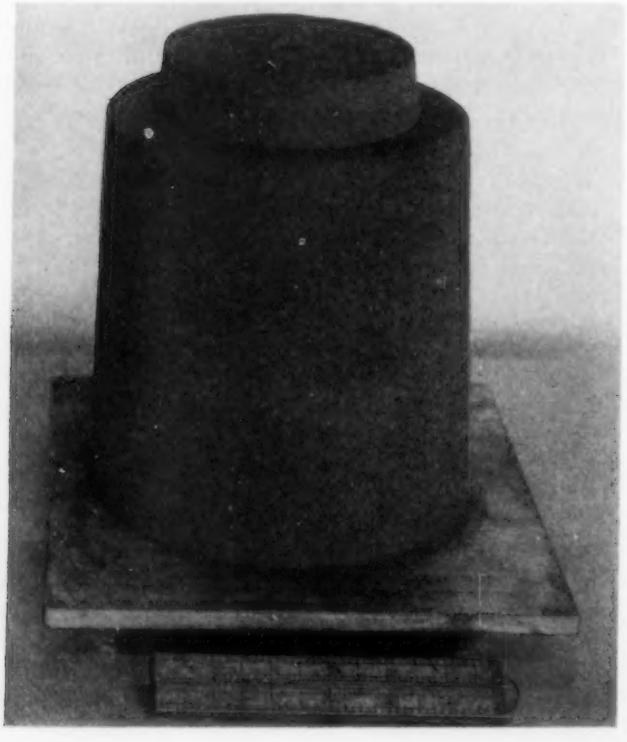
Dune sand, heap sand, cereal and rosin binder. Mix 2 min., add water, mix 6 min.

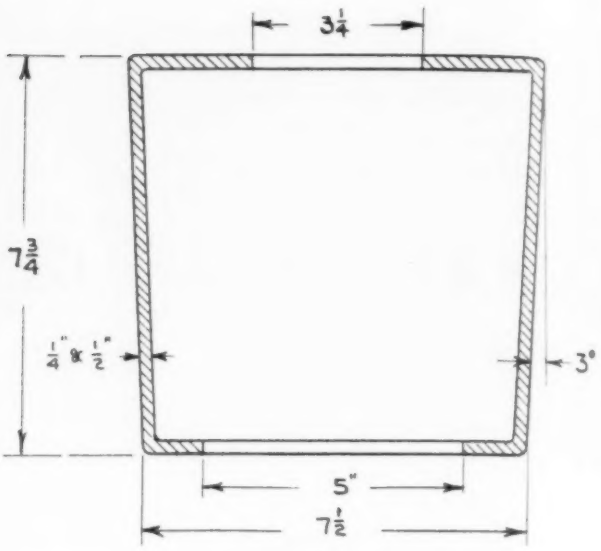
3. Place mixed sand in wheelbarrow and cover with damp sack.
4. Fill 5 gal. container half full with mixed sand representing all test batches of one type and deliver to the sand laboratory for test cores.
5. Make foundry cores within one hour after mixing.

Core Ramming

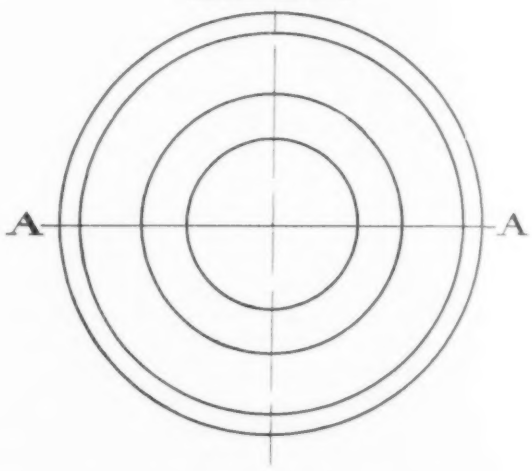
1. Clean core boxes.
2. Apply parting to core boxes, avoiding excess.
3. Break up lumps in all mixtures.
4. Fill core box, strike off sand hopper and place ramming weight on top of sand.
5. Jolt cores 25 times, holding core box down on jolt table by hand.

Fig. 3—Core used in the foundry tests.





SECTION A-A



6. Vent each core with ½-in. diameter rod.
7. Do not rap excessively.
8. Use air vibrator and drawing device.
9. Place cores on portable oven rack.
10. Make not less than 24 cores from each mix, at least 12 for each casting thickness.

Core Baking

1. Place cores in heated core oven (425 F) within one hour after they are rammed with the exception of mixture No. 5.
2. Maintain core oven temperature at 425 F.
3. Bake cores for 5 hr. with the exception of mixture No. 5.
4. Remove cores from oven immediately after baking period.
5. Core mixture No. 5 is to be baked at 400 F for 4 hr.

Core Storage

1. Place baked cores on cooling rack.
2. Allow cores to cool for at least 12 hr. before setting in molds.

Molding Floor

The operations at the molding floor may be divided into molding, core setting and closing, pouring, and

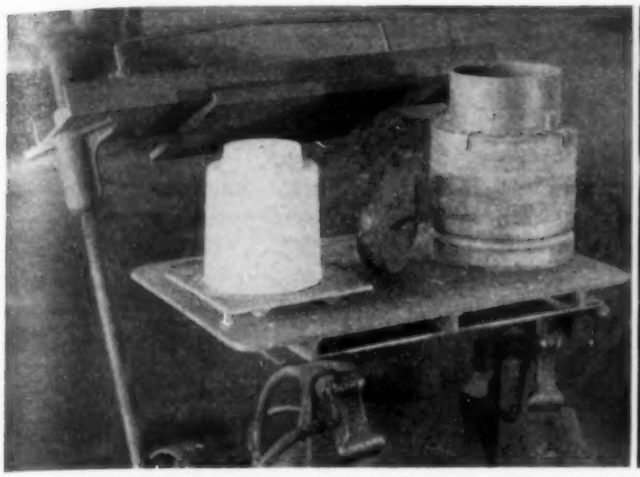


Fig. 2—Equipment used for making foundry test cores.

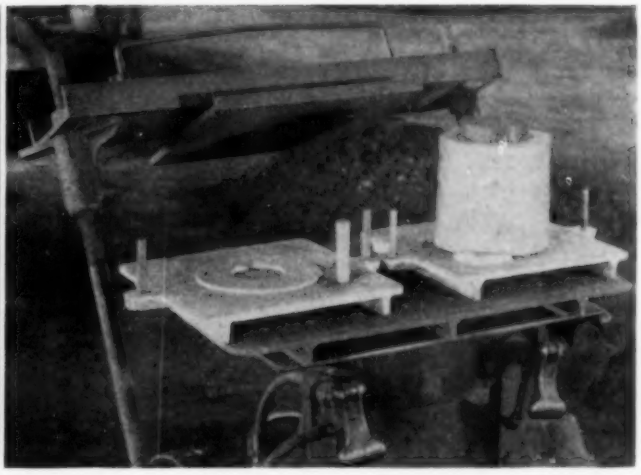


Fig. 4—Pattern and molding equipment used in the foundry tests.

mold shakeout. The steps followed in the foundry by this sub-committee are outlined as follows:

Molding

1. Temper sand to 8.5 per cent to 9.0 per cent moisture. Test for moisture.
2. Riddle sand through No. 4 screen.
3. Test sand for green permeability and green compression. Green compression is to be 5.5 to 6.5 psi.
4. Clean pattern equipment.
5. Dust parting on pattern.
6. Riddle sufficient sand in flask to cover pattern.
7. Fill flask and hopper with sand. Peen cope around inside edge of flask.
8. Jolt 25 times. Butt ram drag and cope with hand rammer.
9. Use a pattern to form a pouring basin.
10. Make five molds for each core mixture of each casting thickness and foundry type (floor and system), a total of 20 molds for each mix.
11. Vent cope and drag with a ½-in. diameter vent rod for core vent.
12. Number copes consecutively.
13. Spotcheck mold hardness.

Core Setting

1. Check drag vent hole.
2. Lay flour core print seal.
3. Clean vent hole in core.
4. Thread string through vent and catch string with a nail.
5. Set core. Check uniformity of cavity thickness.
6. Lay flour core print seal.
7. Check vent hole in cope.
8. Close mold. Place dry sand on cope.
9. Place flask weight, gently rub weight in place and set on a second weight.
10. Dust flour on sprue opening to aid in pouring.

Pouring

1. Cool metal in ladle from the electric furnace to 2650 F.
2. Measure metal temperature with Pt-Pt 10 per cent Rh thermocouple.
3. Measure pouring time of each mold with a stop watch.

Mold Shakeout

1. Shake out molds as gently as possible to avoid disturbing core.
2. Shake out first 5 molds of each core mixture for each metal thickness two hours after pouring to simulate floor type of operation.
3. Shake out the second five molds of each core mixture for each metal thickness (¼ and ½ in.) one-half hour after pouring to simulate system type of operation.
4. Shake out molds by rapping each side of flask with a mallet. Then lift cope and drag separately from the casting. Lay casting with axis horizontal on a 1-in. layer of sand to cool for 3½ hr. in the case of the 5½ hr. casting cooling program for floor type foundry operation. In the case of the system type foundry operation, place the casting immediately in core knockout unit.

Core Knockout

The first method of removing the cores from the

Fig. 5—Core knock-out unit.



TABLE 1.—COMPOSITION OF CORE MIXTURES USED IN THE CORE KNOCKOUT INVESTIGATION.

Mixture	Percentage by Weight		
	No. 1		
160 lb.	Lake Michigan Dune Sand, average fineness 55.4	99.0	1 Dummy batch
1.616 lb.	Linseed base core oil	1.0	4 Test batches
5.818 lb.	Water	3.6	
	No. 2		
114 lb.	Lake Michigan Dune Sand	70.0	1 Dummy batch
46 lb.	Michigan Bank Sand, Average fineness 89.1	28.25	4 Test batches
0.814 lb.	Cereal Binder	0.5	
6.272 lb.	Water	4.1	
2.036 lb.	Linseed base core oil	1.25	
	No. 3		
140 lb.	Lake Michigan Dune Sand	87.0	1 Dummy batch
16.1 lb.	Silica Flour	10.0	4 Test batches
0.805 lb.	Western Bentonite	0.5	
1.61 lb.	Cereal Binder	1.0	
8.85 lb.	Water	5.5	
2.414 lb.	Linseed base core oil	1.5	
	No. 4		
58 lb. 13 oz.	New Jersey Gravel, average fineness 19.5 clay 17.4%	49.0	1 Dummy batch
58 lb. 12 oz.	Heap Sand, average fineness 71.7 clay 14.1%	49.0	5 Test batches
2.40 lb.	Pitch	2.0	
9.0 lb.	Water	7.5	
	No. 5		
24 lb.	Lake Michigan Dune Sand	15.0	1 Dummy batch
130.4 lb.	Heap Sand	81.5	4 Test batches
0.800 lb.	Cereal Binder	0.5	
1.926 lb.	Rosin Type Binder	3.0	
13.60 lb.	Water	8.5	

TABLE 2.—CHEMICAL ANALYSIS OF METAL USED IN TEST CASTINGS.

Elements Per cent	May 20	May 21	May 22	May 23	May 24	May 25
Silicon	2.45	2.36	2.45	2.33	2.45	2.37
Sulphur	0.10	0.10	0.10	0.10	0.10	0.10
Phosphorus	0.50	0.23	0.30	0.30	0.34	0.35
Manganese	0.51	0.61	0.63	0.64	0.65	0.65
Combined Carbon (by color)	0.56	0.57	0.50	0.56	0.48	0.56
Total Carbon	2.86	3.04	3.25	3.08	3.41	3.30

test casting was to jolt the castings while they were held in a fixture on the table of a jolt molding machine. It was soon found that on a long continued jolting that the energy imparted by each jolt was not uniform. It became apparent that cores which were exceedingly difficult to knock out required too much time to remove, since only a limited number of jolts can be imparted per minute. Also, this machine was found to be erratic.

A knockout unit, as shown in Fig. 5, equipped with fixture bars to hold the test castings in place, proved satisfactory.

The procedure followed in the core knockout was:

1. Place cleaned castings in all of the casting holding spaces of the shakeout unit, except one.
2. Place a casting containing a core in the remaining space. Have the large opening down and the knockout unit running.
3. Use a stop watch to measure the time required to remove all of the core from the casting. Express results as time in seconds.
4. More than one casting can be in the knockout unit providing separate observers are used to time each core being knocked out.

Sand Laboratory

Sufficient elevated temperature test specimens had to be made to insure that enough specimens would be available for all possible laboratory tests that might be required after the foundry test. The procedure followed in the sand laboratory was:

Laboratory Sand Testing:

1. Use sand from foundry sand mixtures stored in covered 5-gal. containers. Make core specimens within 2 hr. after mixing.
2. Ram not less than 200 1⅛ x 2-in. core specimens from each mixture. Use three drops of rammer weight with mold floating type specimen container (double end ramming). Note: Fifteen

TABLE 3.—PHYSICAL PROPERTIES AT ROOM TEMPERATURE OF CORES USED IN CORE KNOCK-OUT INVESTIGATION

Core Mixture Number—in Order of Knockout Effort Required	Physical Properties of Core Mixtures						
	Green Properties				Baked Properties		
	Moisture Percentage of Sand as Mixed	A.F.A. Green Permeability	Green Compressive Strength, Psi	Flowability, Per cent	A.F.A. Baked Permeability	Baked Tensile Strength, Psi	Baked Compressive Strength Psi
1	3.75		.3	88	252.5	113.0	321
2	4.00			91	163.0	181.0	644
5	8.35	35.3	4.5	92	61.5	25.2	153
3	5.35	80.0	2.4	86	85.0	186.0	680
4	8.8	14.3	12.1	70.5	39.2	18.0	280

TABLE 4.—PHYSICAL PROPERTIES AT ELEVATED TEMPERATURE OF CORES USED IN CORE KNOCK-OUT INVESTIGATION

	Collapsibility Time in Sec. 90 Psi Load		Hot Compressive Strength Psi 12 Min. Heating Time				Expansion in/in. 4 oz. Load	Gas Pressure in. Water
Atmosphere	Own	Air	Own	Own	Own	Air	Air	Own
Furnace Temp°F	1500	1500	800	2000	2500	2500	2500	2500
Mix. No. 1	58	30	50	9	3	0	0.080	3.8
Mix. No. 2	85	33	60	23	5	4	0.087	4.2
Mix. No. 5	18	4.1	36	33	55	3	0.052	26.0
Mix. No. 3	121	43.7	198	91	78	9	0.082	5.7
Mix. No. 4	215	474	116	230	250	9	0.041	50+

Table 5.—Retained Strength of Cores Used in Core Knock-out Investigation

	Retained Compressive Strength—Psi											
Heating Time Minutes	8	12	40	12	12	20	30	12	10	30	30	30
Atmosphere	Air	Own	Own	Own	Air	Own	Own	Own	Own	Own	Own	Air
Test Temp. °F	Room	200	Room	Room	Room	Room	Room	Room	Room	200	300	Room
Furnace Temp.°F	800	900	600	800	800	800	800	900	1000	800	800	800
Mix. No. 1	23.8	0	167.68	31.93	0	28.74	26.91	17.82	7.98	27.4	25.30	Zero
Mix. No. 2	102.1	2.5	379.35	74.43	23.0	73.82	70.05	56.49	11.55	60.1	61.43	Zero
Mix. No. 5	188.4	6.7	179.07	118.44	144.5	113.35	143.28	125.75	116.23	138.75	129.05	62.2
Mix. No. 3	293.1	80	511.30	254.47	187.9	210.46	210.15	192.63	137.62	204.99	216.30	28.6
Mix. No. 4	349.6	243	314.79	362.39	278.9	368.68	342.59	419.19	396.14	323.98	340.24	227.2

Table 6.—Foundry Test Data for No. 1 Cores, ¼ and ½-in. Metal Section With Floor Type Operation.

Floor Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
3	2650	7.0	11:10	1:10	4:40	13.0	
5	2545	—	11:26	1:26	4:56	10.0	19.25
1-A	2650	5.8	10:50	12:50	4:20	9.0	19.0
2-A	2650	5.6	10:54	16:54	4:24	10.2	—
3-A	2650	5.6	12:08	2:08	5:38	8.0	19.75
					Average	10.0	

Moisture 8.9%
Green Permeability 34.0
Green Compression 5.5 psi
Mold Hardness 50 to 60

Floor Type Foundry Practice
Metal thickness ½-in. thick

6	—	11.6	12:50	2:50	6:20	—	36.25
7	2530	12.6	12:53	2:53	6:23	1.7	37.0
8	2570	10.6	12:58	2:58	6:28	1.6	38.25
9	2595	11.6	1:00	3:00	6:30	1.5	37.5
10	2590	11.2	1:02	3:02	6:32	1.4	37.75
					Average	1.5	

Moisture 9.3 8.7%
Green Permeability 43 45
Green Compression 5.5 6.0 psi

Table 7.—Foundry Test Data for No. 1 Cores, ¼ and ½-in. Metal Section With System Type Operation.

System Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
16	2650	5.6	11:55	12:25	12:26	20.0	18.4
17	2650	5.0	11:56	12:26	12:27	26.6	20.0
18	2650	6.8	11:58	12:28	12:29	25.2	19.2
19	2650	6.8	11:59	12:29	12:30	22.0	19.25
20	2650	10.6	—	—	—	—	—
					Average	23.4	

Moisture 8.4%
Green Compression 5.75 psi.
Green Permeability 45

System Type Foundry Practice
Metal thickness ½-in. thick

11	2650	12.2	10:49	11:19	11:20	5.0	37.8
12	2650	11.2	10:50	11:20	11:22	4.4	38.0
13	2650	10.2	10:52	11:22	11:24	5.2	37.0
14	2650	10.2	10:54	11:24	11:26	5.2	37.2
15	2650	11.2	10:56	11:26	11:28	4.0	37.6
					Average	4.7	

Moisture 8.4%
Green Compression 5.75 psi.
Green Permeability 45

Table 8.—Foundry Test Data for No. 2 Cores, ¼ and ½-in. Metal Section With Floor Type Operation.

Floor Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
26	2650	5.2	2:43	4:43	8:13	32.0	19.4
28	2630	5.4	2:53	4:53	8:23	23.1	20.25
30	2650	5.6	2:58	4:58	8:28	14.0	20.4
30-A	2650	5.8	12:09	2:09	5:39	12.0	—
60-A	2650	—	—	—	—	12.0	—
					Average	18.6	

Moisture 8.6%
Green Permeability 60
Green Compression 6.0 psi.
Mold Hardness 45–53

Floor Type Foundry Practice
Metal thickness ½-in. thick

21	2650	15.0	1:32	3:32	7:02	3.0	37.8
22	2650	13.0	1:35	3:35	7:05	3.2	38.8
23	2650	12.5	1:37	3:37	7:07	3.0	39.6
24	2650	12.0	1:39	3:39	7:09	3.0	38.4
25	2650	12.5	1:44	3:44	7:14	2.9	38.8
					Average	3.0	

Moisture 8.4%
Green Permeability 45
Green Compression 5.7 psi.

Table 9.—Foundry Test Data for No. 2 Cores, ¼ and ½-in. Metal Section With System Type Operation.

System Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
36	2650	6.0	1:46	2:16	2:17	41.6	18.5
37	2650	5.4	1:48	2:18	2:19	48.6	19.5
38	2650	5.4	1:50	2:20	2:21	49.8	19.0
39	2650	5.8	1:52	2:22	2:23	43.0	19.5
40	2650	5.7	1:54	2:24	2:25	45.0	20.0
					Average	45.6	

Moisture 8.6%
Green Permeability 60
Green Compression 5.5 psi.

System Type Foundry Practice
Metal thickness ½-in. thick

31	2650	11.2	4:41	5:11	5:11	20.0	37.0
32	2650	7.9	4:44	5:14	5:14	15.6	37.25
33	2650	9.1	4:47	5:17	5:17	14.0	37.25
34	2650	9.2	4:48	5:18	5:18	20.0	37.5
35	2590	9.4	4:49	5:19	5:19	21.0	38.0
					Average	18.1	

Moisture 8.6%
Green Permeability 60
Green Compression 6.0 psi.

TABLE 10.—FOUNDRY TEST DATA FOR NO. 3 CORES, ¼ AND ½-IN. METAL SECTION WITH FLOOR TYPE OPERATION.

Floor Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
41	2650	6.2	11:10	1:10	4:40	450	20.0
42	2650	5.9	11:12	1:12	4:42	292	18.75
43	2650	5.8	11:14	1:14	4:44	991	19.25
44	2650	6.2	11:16	1:16	4:46	399	18.5
45	2650	6.0	11:19	1:19	4:49	1116	20.0
42-A	2650	5.8	11:08	1:08	4:38	270	19.5
					Average	586.3	

Moisture 8.6%
Green Permeability 60
Green Compression 5.75 psi.
Mold Hardness 43–50

Floor Type Foundry Practice
Metal thickness ½-in. thick

46	2650	12.4	12:43	2:43	6:13	35.4	36.5
47	2650	—	12:46	2:46	6:16	51.8	36.5
48	2650	10.2	12:47	2:47	6:17	35.2	37.0
49	2650	10.0	12:49	2:49	6:19	32.2	36.5
50	2650	9.2	12:51	2:51	6:21	39.6	37.25
					Average	38.8	

Moisture 9.2%
Green Permeability 65
Green Compression 5.75 psi.

TABLE 11.—FOUNDRY TEST DATA FOR NO. 3 CORES, ¼ AND ½-IN. METAL SECTION WITH SYSTEM TYPE OPERATION.

System Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
56	2650	5.9	4:20	4:50	4:51	1545	17.0
58	2650	6.0	4:24	4:54	4:55	915	19.25
59	2650	5.6	4:25	4:55	4:56	1080	18.5
57-A	2625	6.0	12:05	2:35	2:35	630	19.5
60-A	2650	6.0	4:13	4:43	4:43	1005	18.5
					Average	1035	

Moisture 9.6%
Green Permeability 31
Green Compression 5.0 psi.
Mold Hardness 40–55

System Type Foundry Practice
Metal thickness ½-in. thick

51	2650	11.0	3:15	3:45	3:46	1215	37.0
52	2650	11.4	3:17	3:47	3:48	778	37.0
53	2650	10.6	3:21	3:51	3:52	1115	37.5
54	2650	11.4	3:24	3:54	3:55	910	36.75
55	2650	11.2	3:25	3:55	3:55	1320	36.25
					Average	1068	

Moisture 9.6%
Green Permeability 31
Green Compression 5.0 psi.
Mold Hardness 40–55

TABLE 12.—FOUNDRY TEST DATA FOR NO. 4 CORES, ¼ AND ½-IN. METAL SECTION WITH SYSTEM TYPE OPERATION.

System Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
61	2650	5.0	12:36	1:06	1:07	3150	20.0
63	2650	6.2	12:40	1:10	1:11	2910	19.25
64	2650	5.2	12:41	1:11	1:12	6100	21.0
65	2640	6.8	12:45	1:15	1:16	3542	19.50
62-A	2640	7.0	12:06	12:36	12:36	2055	—
					Average	3551.4	

Moisture 8.7%
Green Permeability 50.9
Green Compression 5.8 psi.

System Type Foundry Practice
Metal thickness ½-in. thick

76	2650	11.6	4:58	5:28	5:28	3060	36.75
77	2650	10.0	5:00	5:30	5:30	3600	38.5
78	2650	11.8	5:02	5:32	5:32	2880	37.5
79	2650	9.8	5:04	5:34	5:34	4200	38.0
80	2650	9.8	5:05	5:35	5:35	2640	37.0
					Average	3276	

Moisture 8.8%
Green Permeability 43.0
Green Compression 7.5 psi.
Mold Hardness 44–50

TABLE 13.—FOUNDRY TEST DATA FOR NO. 4 CORES, ¼ AND ½-IN. METAL SECTION WITH FLOOR TYPE OPERATION.

Floor Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
71	2650	6.2	3:30	5:30	9:00	1395	19.25
72	2650	6.6	3:34	5:34	9:04	1625	19.50
73	2650	6.0	3:35	5:35	9:05	1590	19.25
74	2650	5.8	3:37	5:37	9:07	3165	19.25
75	2650	6.4	3:41	5:41	9:11	2500	19.75
					Average	2055	

Moisture 8.8%
Green Permeability 43.0
Green Compression 7.5 psi.
Mold Hardness 44–50

Floor Type Foundry Practice
Metal thickness ½-in. thick

66	2630	13.8	2:16	4:16	7:46	120	37.0
67	2650	11.4	2:19	4:19	7:49	105	38.0
68	2650	11.8	2:24	4:24	7:54	238	37.75
69	2655	11.6	2:26	4:26	7:56	138	38.5
70	2630	11.4	2:27	4:27	7:57	206	37.0
					Average	161.4	

Moisture 8.7%
Green Permeability 50.0
Green Compression 5.8 psi.

hundred 1⅛ x 2-in. test core specimens of each mixture actually were made.

3. Use core dryers for all specimens.
4. Make three 2x 2-in. core permeability specimens. Use three rammer weight drops.
5. Make five 2 x 2-in. baked compression specimens.
6. Make six A.F.A. baked tensile core specimens.
7. Bake all test specimens with foundry test cores at same temperature but only for 3 hr.
8. Place test cores in oven 2 hr. after foundry cores with the exception of mixture No. 5, which is placed in the oven one hour after the foundry cores.
9. To check uniformity, test cores for moisture and green compression strength.
10. Make the following room temperature tests on an average sample of each mix: percentage mois-

Table 14.—Foundry Test Data for No. 5 Cores, ¼ and ½-in. Metal Section With Floor Type Operation.

Floor Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec.	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
81	2650	5.8	10:51	12:51	4:21	34.5	18.5
82	2650	6.8	10:52	12:52	4:24	18.5	—
83	2650	5.8	10:56	12:56	4:26	32.4	19.5
84	2650	6.5	11:04	1:06	4:36	45.0	19.5
85	2650	6.6	11:06	1:08	4:38	38.4	18.75
					Average	33.7	

Moisture 9.0%
Green Permeability 48.0
Green Compression 6.4 psi.

Floor Type Foundry Practice
Metal thickness ½-in. thick

86	2650	8.4	1:30	3:30	7:00	9.0	
87	2650	11.6	1:33	3:03	7:03	10.0	
88	2650	12.2	1:35	3:05	7:05	9.0	
89	2625	12.6	1:45	3:15	7:15	10.0	
90	2650	14.4	3:58	5:38	9:28	14.0	
					Average	10.4	

Moisture 9.0%
Green Permeability 48.0
Green Compression 6.4 psi.

Table 15.—Foundry Test Data for No. 5 Cores, ¼ and ½-in. Metal Section With System Type Operation.

System Type Foundry Practice
Metal thickness ¼-in. thick

Mold No.	Pouring Temperature	Pouring Time in sec	Time Poured	Shake-out Time for Molds	Knock-out Time for Cores	Seconds Knock-out	Weight of Casting
91-A	2570	11.4	3:25	3:55	3:55	48.0	—
92-A	2650	9.5	3:28	3:58	3:58	44.5	—
93-A	2650	9.3	3:31	4:01	4:01	31.0	—
94-A	2465	9.4	4:08	4:38	4:38	59.0	—
					Average	45.6	

Moisture 8.6%
Green Permeability 47.0
Green Compression 6.3 psi.

System Type Foundry Practice
Metal thickness ½-in. thick

96	2650	14.5	1:07	1:37	1:37	55.0	38.0
97	2650	14.0	1:09	1:39	1:39	36.0	40.0
98	2650	16.5	1:10	1:40	1:40	78.0	—
99	2650	21.0	1:13	1:43	1:43	88.0	40.0
100	2630	16.5	1:14	1:44	1:44	—	—
					Average	65.2	

Moisture 8.6%
Green Permeability 47.0
Green Compression 6.3 psi

Table 16.—Foundry Core Knock-out Test Data

Core Mixture Number in Order of Knockout Effort Required	Knockout Time in Sec.				Knockout Time in Sec.			Relative Foundry Knockout Effort For Test Casting
	Floor Type		System Type		Average Data Compiled Floor Type	Average Data Compiled System Type	Composite Average Foundry Knockout Data	
	Section ¼-in.	Section ½-in.	Section ¼-in.	Section ½-in.				
1	10.05	1.55	23.5	4.7	5.8	14.3	9.9	Fast
2	18.6	3.0	45.6	18.1	10.8	31.9	21.3	Good
5	33.8	10.4	45.6	65.2	22.1	55.4	38.9	Fair
3	586.3	38.8	1035.0	1068.0	312.6	1051.5	682.0	Difficult
4	2055.0	161.4	3551.4	3276.0	1108.2	3413.7	2260.0	Very Difficult

ture, green permeability, green compression strength, flowability, baked permeability, baked tensile strength, baked compression strength and core gas test.

11. Carefully wrap the 1⅛ x 2-in. elevated temperature specimens in soft paper, and place in desiccator cans for shipment to the laboratory for tests.
12. Elevated temperature tests consisting of hot strength, expansion, gas pressure and retained compressive strength were made.

Chemical Analysis of Metal

The analysis of the gray iron used in the series of foundry tests is shown in Table 2.

Room Temperature Test Data of Cores

The room temperature physical properties of the five foundry test core mixtures are tabulated in Table 3. A study of the test data in Table 3 reveals that the physical properties of cores used in this investigation cover a wide range of values. For example, the baked permeability of the No. 1 core mixture is 252 and that of the No. 4 core mixture is 39.2. The tensile strength also covers a wide range from 186 psi. for the No. 3 core mixture to a low of 18 psi. for the No. 4 core mixture.

Elevated Temperature Core Tests

The elevated temperature core test data tabulated in Tables 4 and 5 show that each core mixture used was not a duplication of another mixture.

The atmosphere under which a molding material is tested may greatly influence the physical properties of a core at elevated temperatures. In a test furnace, the atmosphere is oxidizing providing no special precautions are taken to exclude air. The term "air atmosphere" in Table 4 thus describes an atmosphere that is oxidizing. Under such an atmosphere many of the core binders will burn and lose their strength rapidly.

A core within a casting would be subjected to an atmosphere at the metal-sand interface that consists of gases produced by the core, core wash, mold wash, metal and molding sand. The preponderance of the gas would come from the core, particularly after it was covered with metal. To attempt to reproduce this condition within practical limitation, a platinum or ceramic hood of restricted size is placed over the core test specimen within the furnace. A thermocouple was placed on the outside of the hood for hot compressive strength and on the inside of the hood for retained

strength.

Under this condition the core is subjected to essentially its own atmosphere. From information at hand, own atmosphere seems to be the better method of testing cores at elevated temperatures since it most closely reproduces conditions within a mold.

Foundry Casting Data

The foundry casting data for the meetings of May 20 through 25, 1946, are shown in Tables 6 to 15 inclusive. In these tables are found the pouring temperatures, pouring time, time of day molds and cores were knocked out, weight of castings, and number of seconds required to knock out cores from the castings. Data are given for both metal wall thicknesses.

Summary of Foundry Core Knockout Data

The foundry test data tabulated in Tables 6 to 15 is summarized in Table 16. The core mixtures are tabulated in order of core knockout time. The order from fast to slow knockout time is mixture Nos. 1, 2, 5, 3 and 4. On the left hand side of Table 16 is tabulated the average core knockout time in seconds for the floor and system type foundry. Subdivisions for each type of foundry are core knockout time for ¼ and ½-in. metal sections.

In the case of a floor type foundry, increasing the metal section from ¼ to ½ in. will cause a great reduction in core knockout time because for all practical purposes the ½-in. metal section possesses twice as much heat as the ¼ in. Doubling the heat around the core reduces the knockout time to one-sixth in the case of the two linseed-base oil bonded cores Nos. 1 and 2. For core No. 5, containing a cereal-rosin bond, a reduction to $\frac{1}{3}$ is experienced. A reduction to $\frac{1}{15}$ is experienced for the No. 3 core when the metal wall of the casting is doubled. This core contains a bond of silica flour, Western bentonite, cereal binder and linseed base oil. The pitch bonded core No. 4 knocked out in $\frac{1}{12}$ of the time when ½-in. metal was against it in place of ¼-in.

On an average, increasing the metal thickness from ¼ to ½ in. reduced the time of core knockout to $\frac{1}{12}$ of the time in a floor type foundry.

In a system type foundry, one cannot expect the same degree of reduction in knockout time for all types of cores as metal thickness of the casting is increased. A straight linseed base oil bond type of core, for example, No. 1, showed a reduction to $\frac{1}{5}$. When cereal binder and finer sand is present as in core No. 2, increasing the metal wall of the casting from ¼ to ½ in. caused a reduction greater than ½. However, when the core contains pitch, or a combination of a silica flour, Western bentonite, cereal and oil base bond, increasing the metal thickness from ¼ to ½ in. does not materially reduce core knockout time. These types of binders, such as silica flour, Western bentonite, require a long exposure to heat which they do not obtain in a system foundry. This is recognized, and in our case it provided some cores which were difficult to knock out.

The right hand portion of Table 16 groups the knockout time for the ¼ and ½ in. castings and shows the average knockout time obtained from the two metal thicknesses. These data indicate that cores as used in this investigation may be knocked out in one-third of the time for floor type as for system type operation. This is due to the fact that cores were only subjected to the heat of casting for one-half hr. for the system as compared to 5½ hr. for the floor type. The second to the last column on the right hand side of Table No. 16 gives the composite average in core knockout time. The range of core knockout time is from 9.9 to 2260.9 sec. This wide range of core knockout time was one of our aims when the core mixtures were formulated. A surprising correlation is that the order of core knockout time for the five selected core mixtures is the same for floor and system foundry and for ¼ and ½-in. metal wall thicknesses of the casting.

Core Knockout Time vs. Baked Permeability

The core permeability undoubtedly affected the core knockout time of the five cores under consideration. The general rule that the higher the baked permeability, the easier the knockout, does not specifically apply since the cores were not of identical mixtures. However, the cores with higher permeability knocked out faster than cores with lower permeability in all cases except core No. 5.

Core Knockout Time vs. Baked Core Strengths

As the strength of core decreases, the knockout effort required to remove it from a casting decreases, providing other conditions are similar. In the case of the five core mixtures under study, the strength of the cores may have had some influence on the core knockout time. Thus, no one should use these data to show that certain types of binders knock out more easily than others, since the strengths of the five cores were not of equal value. The objective of this committee was not to test the value of core binders. The objective was to obtain cores that would show a wide range of knockout effort in the foundry, and then endeavor to find a laboratory test that could be used to evaluate core mixtures as to ease of knockout.

Core Knockout Time vs. Collapsibility

The collapsibility of a core may be defined as the time in seconds that a baked core specimen will sustain a given load when exposed to an elevated temperature. The test procedure followed in obtaining the collapsibility of the five core mixtures used in this investigation was as follows:

Core test specimens 1⅛ x 2 in. baked in the same foundry oven with the foundry cores were used. A dilatometer with moving furnace, possessing a hearth 2 in. ID and 16 in. long, was used to heat the specimens to 1500 F. Own atmosphere was obtained around the core specimen by placing a platinum hood over the specimen. The time that the core specimen would sustain a 90 psi load was recorded in seconds. A noble metal thermocouple was placed outside the platinum hood to measure the temperature.

The core collapsibility tests in own atmosphere at 1500 F show some correlation with core knockout time in seconds with the exception of core mixture No. 5.

which possesses a very low compressive strength. The core collapsibility tests at a 90 lb load stressed the No. 5 core mixture to such an extent that it failed to sustain the load for a reasonable length of time. The 90 lb preload as used in this investigation for the core collapsibility test undoubtedly was high for some of the core mixtures under consideration. It is possible that a lower preload may have caused a better correlation between core collapsibility and knockout time.

Under core collapsibility testing, a load is applied at the time the specimen goes into the furnace so that the specimen is loaded at room temperature or near room temperature, and it has not been heated before any work has been done on the specimen as is experienced by a core in a casting. The subcommittee agrees that further work on this subject matter is in order.

Core Knockout Time vs. Hot Compressive Strengths

The hot compressive strength of a core in this investigation refers to the compressive strength which a core specimen possesses at an elevated temperature.

The test procedure followed in the hot strength test of the five core mixtures was the same as for the collapsibility test with the exception that the load was not applied to the core specimen until it was in the furnace for a period of 12 min. The load was applied by a motor driven hydraulic system having a table rise of 1 in./min. Hot strength determinations were made in own atmosphere at 800, 2000 and 2500 F. A test was also made at 2500 F in an air atmosphere.

Hot strength determinations made in an air atmosphere do not show any correlation with core knockout time. When own atmosphere is used, the hot strength determinations at elevated temperatures were found to related to the core knockout time for the core mixtures under study. Both the own atmosphere hot strength determinations at 2000 and 2500 F show good correlation. The 2000 F test is slightly the better in correlation with core knockout time. This correlation is surprisingly good whether comparison is made with core knockout time for ¼ or ½-in. metal section for either the floor or system type foundry operation. Figures 6 and 7 show the relationship between own atmosphere hot strength at 2500 F and core knockout time in seconds. Figure 8 shows the correlation for ¼-in. metal section, and Fig. 9 for ½-in. metal section.

In Fig. 10 is shown the correlation between hot strength at 2000 F for 12 min. soaking time with own atmosphere and the composite average of ¼ and ½-in. castings in both floor and system type foundry operation. The hot strength test values place the cores in the same order of core knock-out time as determined by foundry operations.

It was noted in the core knockout operation that when the core became loose within the casting due to a low or zero strength layer of core, then the core was quickly broken by the mechanical shakeout unit. Should the outer layer of the core be strong and hold the core solidly to the casting, then it required a long time to knockout the core. This may explain why hot compressive strength tests under conditions as specified can be used to evaluate core knockout time.

Core Knockout Time vs. Retained Compressive Strength

A study of the retained strength test data (Table 5) and core knockout time (Table 16) reveals that the retained strength with core specimens heated at 800 F in own atmosphere for 12, 20 and 30 min. all show correlation with core knockout time. Compare Figs. 11 to 15 inclusive. The difference between heating time is not significant enough to make one time of heating the better. However, considering the time required to make the test, the retained strength with 12 min. heating is preferable. Retained strength tests at higher temperatures such as 900 and 1000 F do not show as good a correlation. Their low strength also makes it difficult to secure reproducible results due to their low strength values.

Retained strength tests made in own and air atmosphere do not give similar results. The air atmosphere tests give results that are low for all easily oxidized binders when the heating time is from 12 to 30 min. When the heating time is short, for example, 8 min., then the retained strength values for the cores of long knockout time are too low for good correlation. See Table 5 for test data and Figs. 13 and 14 for graphical presentation. The retained strength test values with air atmosphere do not correlate as well with core knock-out time as own atmosphere retained strength test values.

In another form of retained strength test, the specimen is cooled to 200 or 300 F and placed in compression load Table 5 gives data. This test method requires added precautions in testing, and no improvement in correlation was found. Offhand, one would expect much better correlation since cores in the foundry are near a temperature of some 300 F. Breaking the retained strength specimens at room temperature is much easier to perform and should yield more consistent test values, for it is difficult to hold the core specimen at 300 F during the compression test, unless special equipment is available to perform the test.

The present dilatometer could be used for this purpose, but cooling the furnace to 300 F after heating core specimens to 800 F would be time consuming. Considering all the points involved, it is recommended that the retained strength test with furnace temperature at 800 F for heating the core specimens 12 min. in own atmosphere be used as one means of evaluating the ease of core knockout. The core specimens upon being removed from the furnace are allowed to cool to room temperature under an inverted tall form 200 ml beaker. The specimens are to be loaded in compression as soon as room temperature is reached. The retained strength is reported in psi., compressive strength at room temperature.

A comparison of the retained strength after holding at 800 F for 12 min. in own atmosphere, with the specimen cooled to room temperature before compression test, and core knockout time in seconds in the floor foundry is illustrated in Fig. 13 for ¼-in. metal section and in Fig. 14 for ½-in. metal section. The correlation is good, regardless of whether cores are in the casting for ½ or 5½ hr, or for ¼ or ½-in. metal wall section.

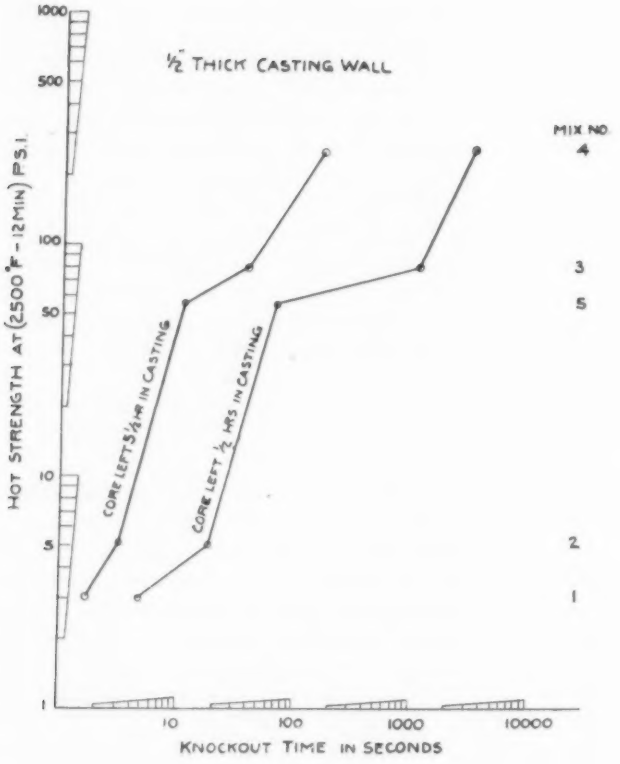


Fig. 6—Comparison between hot compressive strength at 2500° F., 12 min. heating time, own atmosphere, and the core knock-out time for ½-in. metal section.

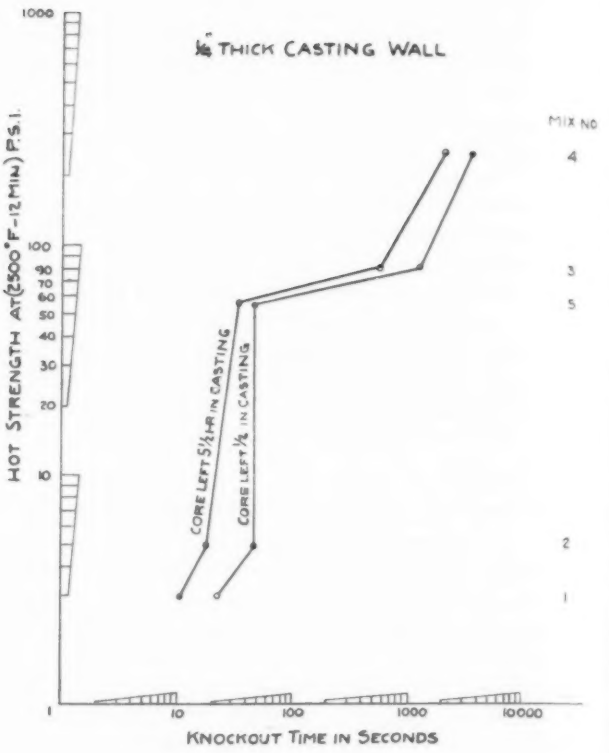


Fig. 7—Comparison between hot compressive strength at 2500° F., 12 min. heating time, own atmosphere, and the core knock-out time for ¼-in. metal section.

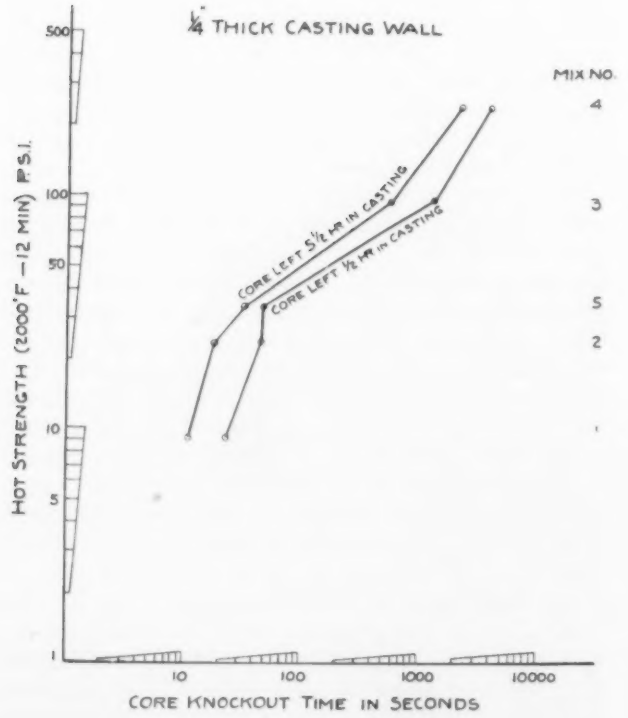


Fig. 8—Comparison between hot compressive strength at 2000° F., 12 min. heating time, own atmosphere, and the core knock-out time for ½-in. metal section.

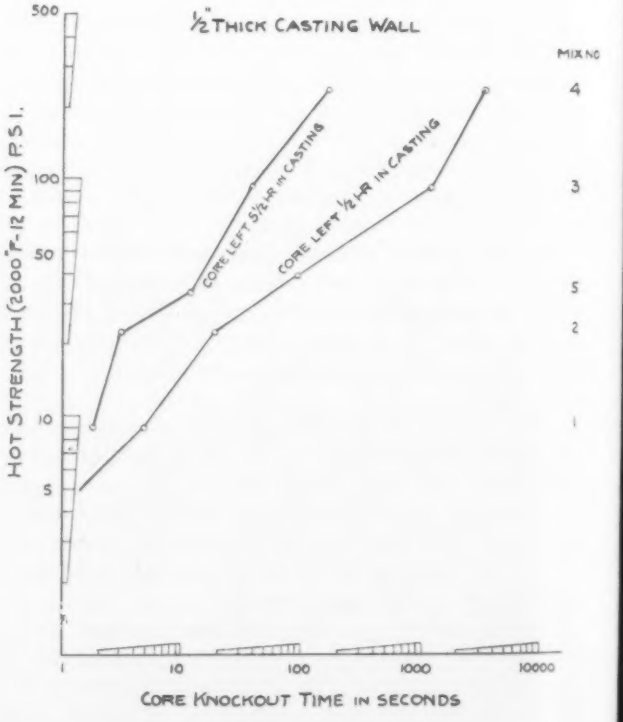


Fig. 9—Comparison between hot compressive strength at 2000° F., 12 min. heating time, own atmosphere, and the core knock-out time for ¼-in. metal section.

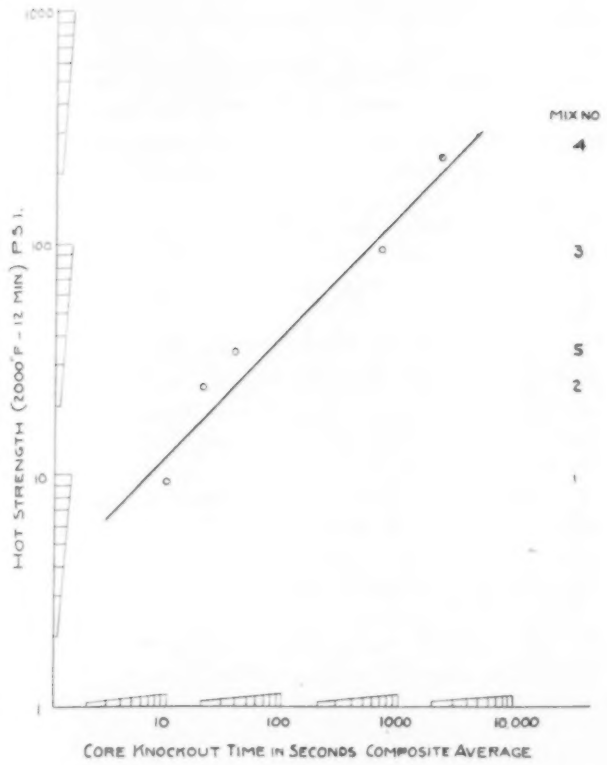


Fig. 10—Comparison between hot compressive strength at 2000° F., 12 min. heating time, own atmosphere, for the composite average core knock-out time of both ¼ and ½-in. thick casting wall and for both floor and system foundry operation.

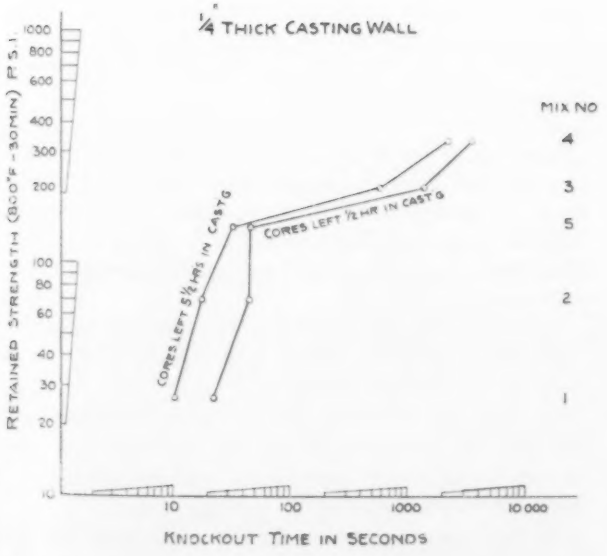


Fig. 11—Comparison between retained compressive strength at 800° F., 30 min. heating time, and the core knock-out time for floor and system foundry operation with ¼-in. metal section.

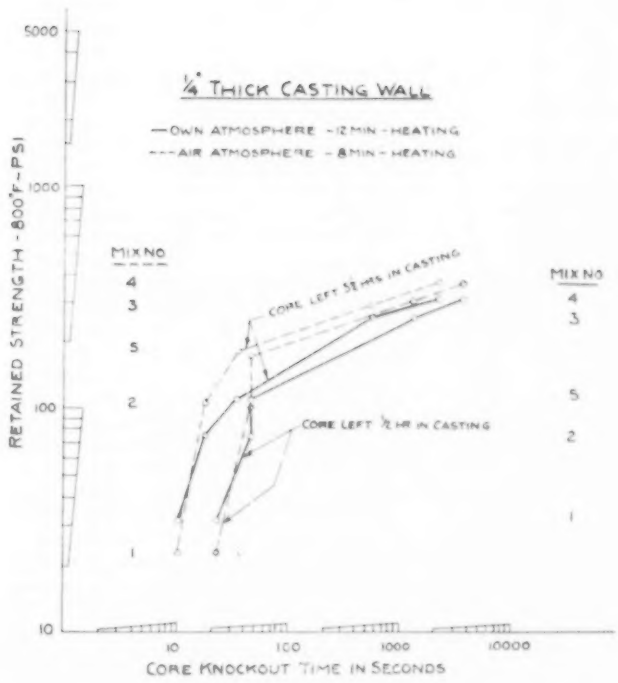


Fig. 13—Comparison between retained compressive strength at 800° F., and the core knock-out time for floor and system foundry operation with ¼-in. metal section.

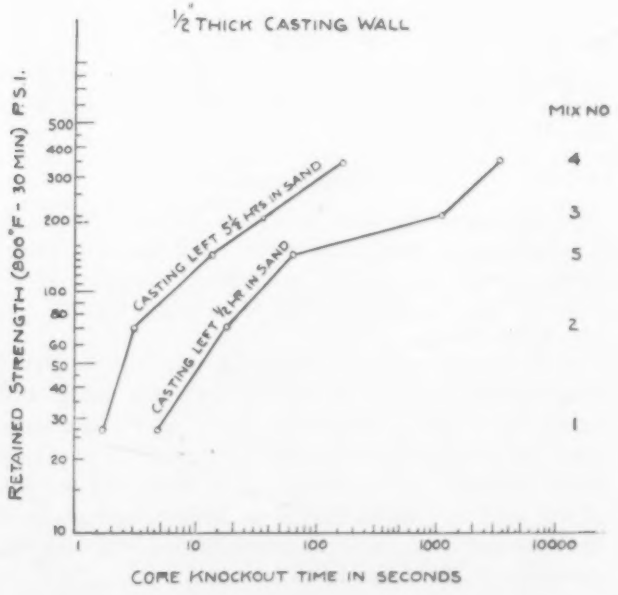


Fig. 12—Comparison between retained compressive strength at 800° F., 30 min. heating time, and the core knock-out time for floor and system foundry operation with ½-in. metal section.

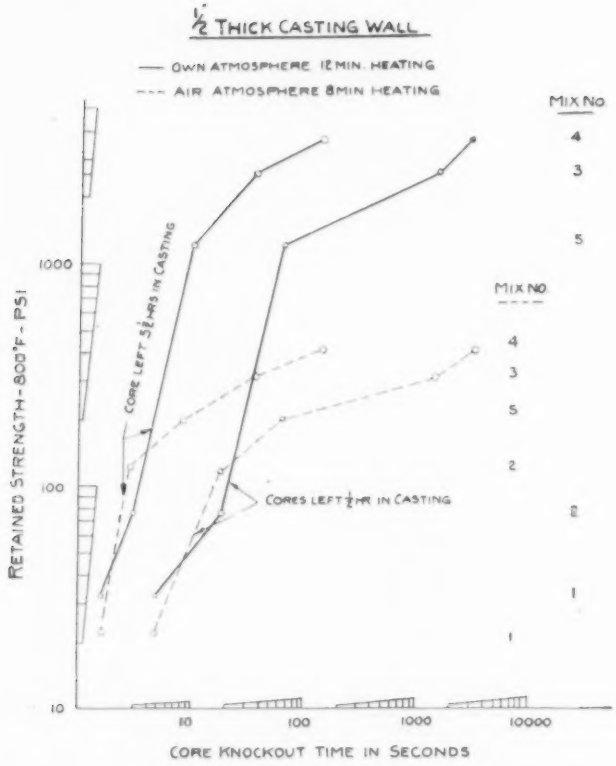


Fig. 14—Comparison between retained compressive strength at 800° F., and the core knock-out time for floor and system foundry operation with ½-in. metal section.

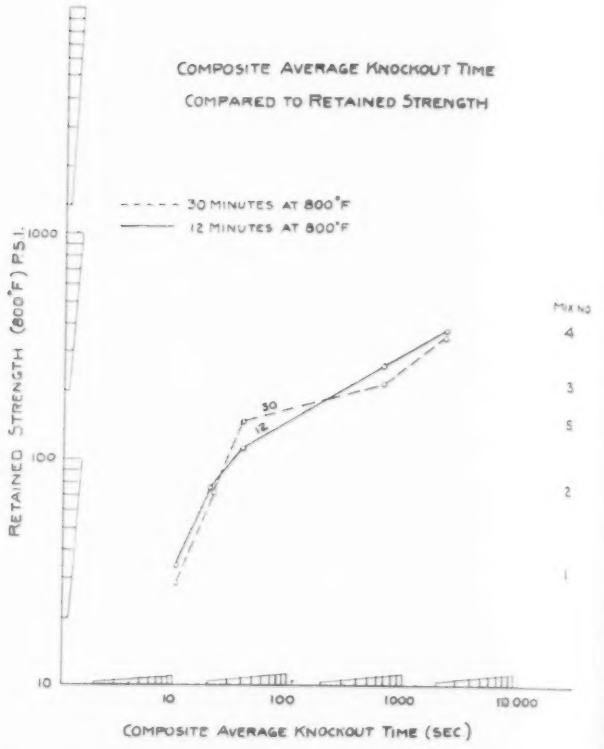


Fig. 15—Comparison between retained compressive strength at 800° F., 12 min. heating time, and the composite average core knock-out time of both ¼ and ½-in. metal section with floor and system foundry operation.

Combining the core knockout time for both floor and system foundry operation and ¼ and ½-in. metal section, a composite average knockout time for the various five core mixtures is obtained as shown in Table 16. The retained strength 800 F, 12 min. heating time in own atmosphere, room temperature compression test as obtained for the various core mixtures, may be compared to this composite average. Core knockout time shows a very good relationship, and is illustrated in Fig. 15.

Conclusions

1. The time required to knock out cores furnishes a good means of evaluating the work required to remove them from castings, providing a mechanical vibrating core knockout table is used to furnish the knockout energy.

2. Evidence to date indicates that the elevated temperature tests on cores should be made under own atmosphere conditions where the core test specimen is surrounded by a gastight container on all sides except at the bottom. A gas space ⅜ in. wide around the side of the core, and extending ½ in. below the bottom of the core specimen proved satisfactory for test work undertaken in this investigation.

3. In this investigation it was found that the hot compressive strength of baked core specimens 1⅛ x 2 in. may be used to determine the order of core knockout effort of a group of core mixtures similar to those studied in this investigation.

4. The hot compressive strength, when made at a temperature of 2000 F, 12 min. heating time, in own atmosphere, with a table rise of one in./min., may be used to determine the order of core knockout effort of a group of core mixtures.

5. This investigation showed that the retained compressive strength of baked core specimens 1⅛ x 2 in. in own atmosphere may be used to determine the order of core knockout effort of a group of core mixtures to be used for iron castings.

6. The most suitable retained compressive strength procedure for evaluating core knockout effort was to heat the 1⅛ x 2-in. core specimen at 800 F for 12 min. in own atmosphere, and then allow the specimen to cool to room temperature under a tall form 200 ml glass beaker having no lip, and finally testing for compressive strength.

7. The composition of the core mixture influenced core knockout to a greater degree than other factors such as metal section thickness.

8. The core mixtures used in this investigation were formulated not as suggestive core mixtures to be used in the foundry but were formulated merely to give a range of core knock-out for test purposes.

9. No hot tear casting defects were observed. The composition of the metal was such that a relatively soft gray iron was secured.

Acknowledgment

The University of Michigan foundry was made avail-

able to the Subcommittee and acknowledgment is made of the aid given by Assistant Professor W. A. Spindler, John Grennan, Bert Sheldon and Louis Ruffins of the Department of Metal Processing of the University, and by E. J. Hasty of the Harry W. Dietert Co.

The meetings of the Subcommittee were of two types. One type of meeting was purely technical and was held for the purpose of formulating plans and studying test data. The other type of meeting consisted of standard foundry operations required to produce and shake out the test castings and laboratory test work.

Subcommittee Personnel

The Subcommittee on Physical Properties of Iron Foundry Molding Materials at Elevated Temperatures is comprised of Harry W. Dietert, Harry W. Dietert Co., Detroit, committee chairman; G. F. Watson, American Brake Shoe Co., Mahwah, N.J., vice chairman; Prof. W. A. Spindler, University of Michigan, committee secretary; Roy Bennett, The Metro-Nite Co., Hammond, Ind., Day E. Cutler, J. S. McCormick Co., Pittsburgh, R. L. Doelman, Miller & Co., Chicago, H. H. Fairfield, Harry W. Dietert Co., Detroit, J. A. Gitzen, Delta Oil Products Co., Milwaukee, John Grennan, University of Michigan, Ann Arbor, H. J. Jameson, Detroit Testing Laboratory, Detroit, E. Pragoff, Hercules Powder Co., Wilmington, Del., E. W. Olsen, Campbell, Wyant & Cannon Fdry. Co., Grand Haven, Mich., C. S. Parsons, Bureau of Mines, Ottawa, Ont., Canada, Gene Passman, Frederick B. Stevens Co., Detroit, Victor Rowell, Velsicol Corp., Chicago, Arnold Satz, National Radiator Co., New Castle, Pa., J. Schumacher, Hill & Griffith Co., Cincinnati, Wm. Seese, J. S. McCormick Co., Pittsburgh, Leon B. Thomas, Wilson Foundry & Machine Co., Pontiac, Mich., Robert Walker, Werner G. Smith Co., Cleveland, D. C. Williams, Cornell University, Ithaca, N.Y., Elmer C. Zirzow, National Malleable & Steel Castings Co., Cleveland.

DISCUSSION

Chairman: E. C. Zirzow, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman: H. J. Williams, New Jersey Silica Sand Co., Millville, N. J.

Member: Is there any relation between core collapsibility and core knockout as determined in the new type caliber?

Mr. Dietert: There is no definite correlation between core collapsibility and core knockout for a wide group of core mixtures. Core collapsibility is used to determine the life of the core while the casting is solidifying around that particular core. It does not necessarily describe the nature of the core at room temperature.

Member: Would the data arrived at by pouring iron castings give comparative results in aluminum with the same core mixtures?

Mr. Dietert: The work of this committee can be applied to most any metal providing you find the proper temperature to which to preheat the core specimen. Probably 800° F. would not be suitable for some of the non-ferrous metals. You may find it necessary to determine a new preheat temperature, but I believe the general technique as described in this paper could be applied.

Member: Have you evaluated the tensile strength of each of the mixtures used in your experiments? I notice the compositions varied and I wondered how the tensile strength was affected. If so, did it have any bearing on your knockout observations?

Mr. Dietert: You will note in Table 3 the first one which shook-out the fastest had a baked tensile strength of 113 psi; the next one 181; the third easiest to shake-out had a strength of 186 psi. The one that was the fourth hardest to shake-out had a strength of 181 psi, whereas the one that was easiest to shake-out had a strength of 25.2 psi.

We selected these core mixtures deliberately to prove or disprove a test method. In other words, it was not obvious which one would be the lowest to shake-out. We intended to cover as many of the conditions as possible that were found in the iron foundry.

Member: If those tensile strengths were all on the same basis, in other words, if they were all 180 psi, would you find a much wider variation in shake-out time?

Mr. Dietert: Yes, in other words, suppose we take the core mix number four, which shook-out the easiest, raised its tensile strength from 18 up to 113 psi, we might have been testing it.

Member: Is there any correlation, or would it be a straight line function, between pouring temperature and shake-out? In other words, as it went up, would you shake-out faster?

Mr. Dietert: I believe that might hold true. The committee did not test the effect of iron temperature on core knockout. They chose one iron temperature of 2650° F.

Member: I notice the mixture that shook-out the hardest had 8.8 per cent moisture. I believe if the moisture content is run up it makes a harder iron strength in the core. If that would run at lower than 8.8 per cent moisture, would that not cut down the shake-out time?

Mr. Dietert: Yes, undoubtedly it would reduce the shake-out time of that particular core mix.

One reason why we all should think about core knockout time is because this is a department in all of our foundries where we should endeavor to remove more machine and more men. If we can do that, then we have accomplished a great deal, and we hope this is just the beginning of a very important progress in the foundry.

FOUNDRY COSTS
AND COST CONTROLS

C. E. Westover
Westover Engineers
Milwaukee

THE COST COMMITTEE of the American Foundrymen's Association was one of the first set up by the Association, and since its inception has been among the most active. Great effort has been expended by A.F.A. Cost Committee members, and also by the various societies representing the several branches of the industry, to develop a uniform accounting system and uniform method of figuring costs worthy of adoption by the foundry industry. The value of a plan of this type cannot be minimized today, although a number of years have passed since the need for it became apparent.

A.F.A. Cost Committee Papers

A perusal of all papers written on the subject of costs since inauguration of A.F.A. 51 years ago would provide sufficient material to make a most appropriate present-day paper. There is one paragraph, however, which might have appeared in any paper reflecting present-day conditions. It appeared in a paper entitled "Uniform Costs in the Foundry Industry" presented at the annual meeting of the Association in Philadelphia, September 29-October 3, 1919. It reads as follows:

"Perhaps the greatest problem before American industry today is that of determining accurate cost of production. Industry is becoming so complex—our tax laws are so intricate and the matter of accurate returns is so important that the concern operating without knowledge of costs is in the least possible position to conduct its business to best advantage. Another, and perhaps the most important reason why costs must be accurately ascertained, is that, to enable the manufacturer to determine just what he can voluntarily do in the way of increasing wages and arranging for profit-sharing plans if necessary to offset the unreasonable demands on the part of his workers, he must know where he stands with reference to his production costs."

If industry in 1919 was so complex as to provoke these comments, it must also have been complex when the first A.F.A. Cost Committee was set up over 50 years ago. Certainly, it remains complex to a certain degree today. Insofar as detailed reports and statistics for government and tax purposes are concerned, they no longer present the management problem created when the practice originated. The amount of information demanded in these reports over the years has increased considerably, but industry has taken this in stride, meeting the challenge by an increase in or the reorganization of personnel. In many cases, passage of the Social Security Act and Withholding Tax laws required the hiring of additional personnel, but this information was required by law and industry cooperated to the fullest extent.

It is regrettable that the castings industry has not adopted the same attitude and expended the same energy in the matter of determining its costs by establishing departments to obtain this information. It is doubtful if there is a plant in any branch of the castings industry, which was in existence during the war, that has not had its productive capacity increased materially, and probably to the point where it is in excess of normal requirements. During the war, and since, many new units have been added, especially in the gray iron and non-ferrous branches of the industry, because of continued abnormal demands for these types of castings. Improved production and management methods and techniques have resulted in a substantial increase in production per man-hour, but still no effort has been made to develop a plan whereby costs are predetermined.

Need for Sound Cost System

The marginal foundry, that foundry which is unable to figure its costs and sell at a profit will not be in existence once the first bulge of consumer demand is satisfied, and the semi-marginal foundry will find itself in serious difficulties. Thus, the familiar struggle for survival which has characterized the foundry industry as a part of a free enterprise system will be on in earnest. Let us not be too hasty in discarding this system, since it has proved to be the foundation of our rapid industrial growth and is responsible for the degree of efficiency which our industries maintain in comparison with that of other nations. Rather, let us be efficient and determine our costs. It is only in this way that we can hope to remain in business. The greatest contribution any foundryman can make toward stability of the industry is to operate his business profitably.

Cost Control and Cost Accounting

The cost accounting systems advocated by A.F.A. and other societies will give you your costs, but, as such, they are purely historical records—records of what has taken place. Generally speaking, the accounting department is the gatherer, keeper, and dispenser of the figures necessary to prepare financial statements which meet the requirements of the business and the government.

Too few accounting departments include in their function the figuring of job costs or participate in cost control work. Cost control and cost accounting are two distinct functions and are usually the responsibility of separate individuals in even small organizations. Cost control is a method of predetermining costs; cost accounting is a matter of recording what has taken place.

Usually accounting occupies a high position in the administration of business, due to the fact that statements must be prepared and presented at definite times; whereas, cost control work is like production: if you cannot deliver this week, then deliver next week.

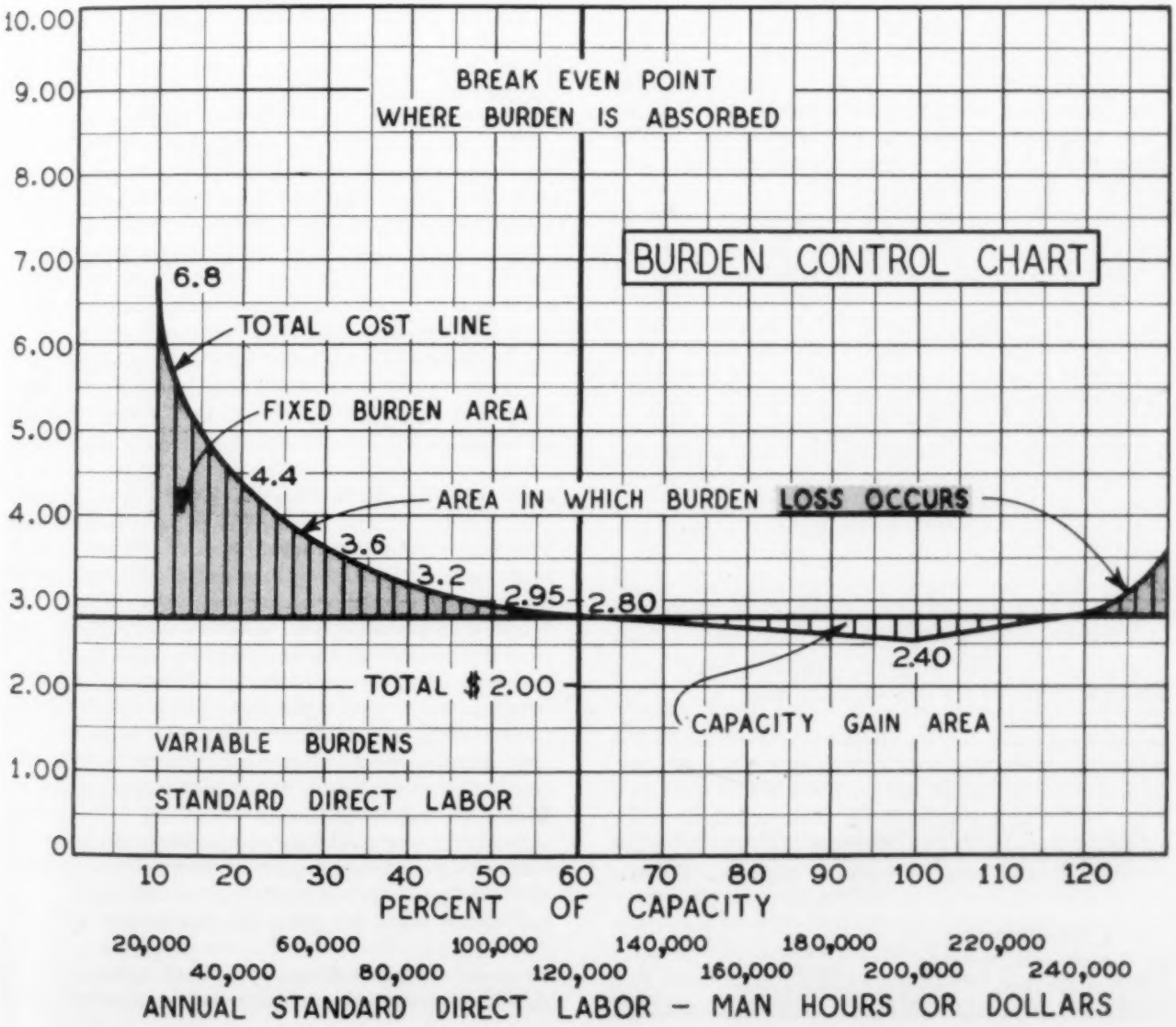
Management must be brought to recognize accounting and cost control work as two distinctly different functions or departments, but of equal importance in the operation of a business. Of course, close cooperation between the two departments is essential.

Financial and Control Budgets

The most effective system will center around financial and control budgets. Financial budgets, dollar value budgets, may be set up on sales forecasts. This will indicate profits for a given amount of sales produced at a predetermined cost. Of course, production processes, methods, wages and the like do not remain static. Therefore, budgets for control purposes must be both flexible and perpetual, adjusted to meet changing conditions, and be at all times reflective of actual operating conditions. Control budgets, to be most effective, should be expressed, wherever possible, in stable measures, such as, time (minutes or hours), weight (pounds or tons). At times, however, it is necessary to use money as a measure. A control budget may resolve itself into a chart or series of charts, for the plant as a whole or for a department covering labor or material.

One problem which concerns management is a loss in certain departments due to a decline in the volume of production in that department. The business as a whole may be operating at a satisfactory profit, but when each department is analyzed, one or more departments may reflect loss. The cause is usually that these departments are partly or wholly shut down, while fixed costs continue. A common example of this

Fig. 1—Burden control chart showing break-even points.



may be the balancing of core room operations with the molding department. Where management decides to establish, for cost purposes, a minimum volume or per-cent of plant capacity as the break-even point, it is then advisable to establish the point for each department.

In making an analysis it is advisable to follow a definite plan or pattern whereby:

1. Fixed costs of the department are calculated.
2. The semi-variable costs of the department are carefully analyzed and budgeted to determine the fixed and variable elements.
3. The standard direct labor, hours or dollars, required to absorb the fixed costs are determined.

If these calculations are expressed in simple chart form, the necessary production volume to break even will be readily ascertainable. A careful recording of figures may also develop the point in operations of diminishing returns or increasing burdens. Charts can be developed using various units of measure.

Break-Even Point

The accompanying chart, Fig. 1, illustrates graphically the break-even point for the plant or for a given department operating at 60 per cent of capacity, the selected point upon which to base costs.

The horizontal scales of Fig. 1 show the annual standard direct labor cost in man hours or dollars for varying percentages of capacity. For instance, at 60 per cent of capacity the total annual standard direct labor is 120,000 man hours, and may or may not be $120,000.00, depending on wage rates and whether they remain constant thru the year.

Vertically, Fig. 1 shows the changing ratio of total cost to standard direct labor cost. In this chart the break-even or burden absorption point is 60 per cent of capacity and the ratio of total cost to the standard direct labor cost at this capacity is 2.8. At this point the ratio or total manufacturing cost is made up of 1 unit of measure of standard direct labor, 1 unit of measure of variable burden cost and 0.8 unit fixed burden cost.

If operations were at 40 per cent of capacity, the ratio would then be 3.2. This ratio of total cost would then be 1 unit of standard direct labor cost, 1 unit of variable burden cost, and 1.7 units of fixed burden cost.

The difference between these two ratios, multiplied by the total annual standard direct labor cost at 40 per cent capacity level, will give the loss which will be experienced. This loss is due to failure to supply the plant or department with sufficient production volume to maintain capacity at the burden absorption point which is at the 60 per cent capacity level.

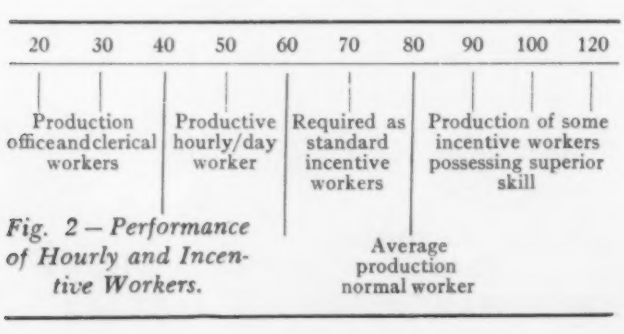


Fig. 2 — Performance of Hourly and Incentive Workers.

As an example, assume a production of 360,000 units at 60 per cent capacity. Then the manufacturing cost would be:

Standard direct labor.....	$120,000
Variable burden cost.....	120,000
Fixed burden cost.....	96,000
Total manufacturing cost.....	$336,000

Cost per unit at this capacity would be 336,000 ÷ 360,000 or $0.933.

At 40 per cent capacity level there would be production of 240,000 units and manufacturing cost would be:

Standard direct labor.....	$ 80,000
Variable burden cost.....	80,000
Fixed burden cost.....	96,000
Total manufacturing cost.....	$256,000

Cost per unit at this capacity would be 256,000 ÷ 240,000 or $1.066.

Actual standard cost at 40 per cent capacity....	$1.066
Standard cost at 60 per cent capacity....	$0.933
Additional cost.....	$0.133 per unit

Therefore, with production at the 40 per cent capacity level (240,000 units) the plant or department would suffer a loss due to production variance of 240,000 units multiplied by 0.133 or $32,000.

According to the chart, the total standard manufacturing cost at the 60 per cent capacity level, is 2.80 per dollar of standard direct labor. At 40 per cent capacity level, total standard manufacturing cost is 3.20 or an increase of 0.40 per dollar of standard direct labor.

Inasmuch as there are 80,000 of standard direct labor cost at the 40 per cent capacity level, this cost multiplied by 0.40 indicates a loss due to reduced production of 80,000 × 0.40 or $32,000. This figure compares with the loss shown in the foregoing example.

The principle of the above illustration is applicable to various problems and can be most useful in determining sales policies, pricing and effort.

Object of Cost Control

The object of cost control should be cost reduction, which may be accomplished through greater productivity per manhour, improved methods, techniques, mechanization and/or economical use of materials. It must be remembered, however, that labor is the predominant cost factor in the production of castings.

A main objective of American industry is to make a better product at a lower cost. Only ever increasing productivity per manhour will make it possible to maintain or reduce selling price, improve quality and pay higher wages. And, unless high wages are accompanied by high output, costs and selling prices of castings will be too exorbitant to encourage their use as a structural material or to provide the volume of business necessary to use a fair percentage of capacity.

Properly organized plans for cost control will automatically provide a cost reduction program, since these plans call for establishing standards of operation which determine materials used in production, methods of

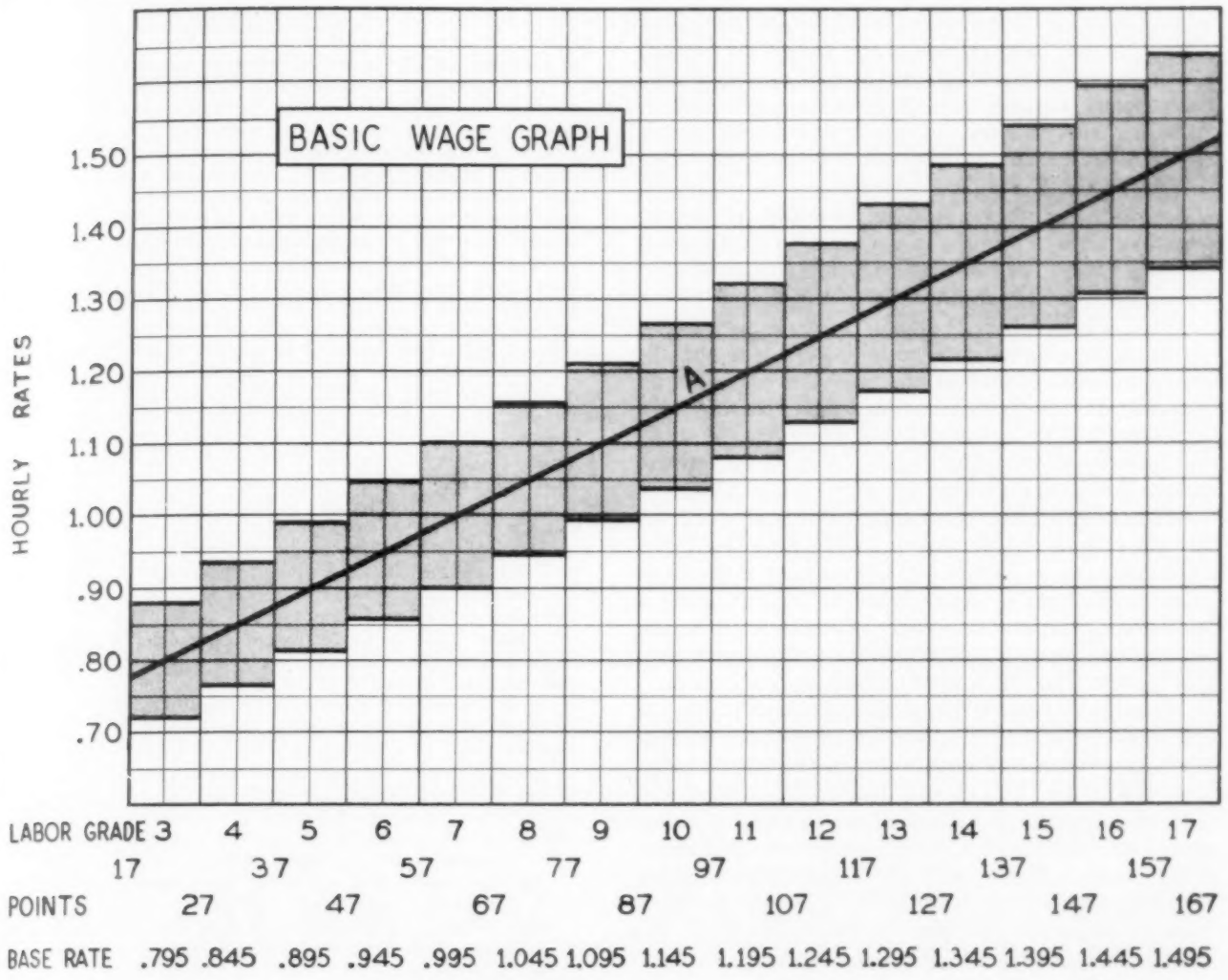


Fig. 3—Method of establishing conversion figure.

production and quality standards. Only after operation standards have been established can standards of performance for operators be set.

Without going into time standards, which is in the field of the A.F.A. Committee on Job Evaluation and Time Study, it will be sufficient to say that time is a stable measure. Time will measure effectiveness of materials, efficiency of methods and quality requirements, as well as the amount of work required to perform an operation. Such time standards should be in the form of standard data, which means element time and not over-all time, thus enabling the cost control department to analyze every job for economy in production.

If labor costs are to be controlled, regardless of whether operators are working for an hourly wage or under a wage incentive plan, it is necessary to know how long it should take to do a given piece of work. The same method of establishing time standards should be employed for either hourly or incentive workers. Of course, in the case of hourly workers, one has no assurance of attaining standard production. Figure 2 shows what has been quite generally accepted by the engineering profession as performance of hourly and incentive workers.

In Fig. 2 the numbers on the line represent the average number of measured minutes of work produced by operators per hour. A measured minute is a time study minute (may be stop-watch time, or other, camera time, etc.) which has been adjusted to compensate for rating performance of operator, fatigue and personal need allowances. Under some incentive plans, sixty measured minutes of work per hour are set as standard, paid for at the base hourly rate of pay and each measured minute of work produced over and above standard is paid for at the same rate as standard production. The production of the normal operator on incentive will average around 80 measured minutes of work per hour. This compares with about 40 measured minutes of work usually produced by the normal operator working on an hourly rate basis.

From this chart it is readily seen that, in the case of hourly workers, there is no assurance of attaining standard production. To achieve a schedule of planned output from the worker, the help of a wage incentive plan will be necessary. Wage incentive plans based on sound principles and strictly adhered to by management will become increasingly important with the return of competitive business.

A step further toward controlled costs is a sound basic wage structure. Wages are the product of a money

and time factor. In the case of hourly work, the base or hourly rate of pay determines the money factor, and the produced measured minutes of work determines the time factor. For example, if the base hourly rate is $0.60/hr., that is a cent/minute. The hourly worker would receive $0.60 regardless of his productivity which might be only 40 measured minutes of work. However, the incentive worker would be expected to produce over 60 measured minutes of work per hour and thereby earn over $0.60 or a cent/minute. If his production is 80 measured minutes he will receive $0.80/hr.

Next, it is necessary to establish a basic wage structure, and the simplest method of doing this is with the aid of an occupational or job evaluation plan. An occupational or job evaluation plan establishes the relative value for every occupation in a foundry and serves as a foundation for all bargaining of wages for individuals or groups of individuals.

Without going into the detailed processes of evaluating the various occupations, it will suffice to state that all occupations in a plant will fall into one of the labor grades established by the evaluation. If merit rating of individuals is a part of the evaluation plan, then hourly workers may have a rate within the rate range for the labor grade into which their occupation places them.

Figure 3 illustrates the method of establishing the money factor or the conversion figure used in establishing wages for hourly workers or earned wages for incentive workers. This chart is divided into labor grades 3 to 17 inclusive. The plant in which this evaluation work was done did not have workers who fell below labor grade 3, therefore, 1 and 2 have been omitted. The base rate line, or A on the chart, is used for determining the conversion figure. It is the midpoint on this line in each labor grade that indicates the conversion figure for all work performed by individuals within this labor grade. This conversion figure for incentive work is not only a very important factor in cost control due to the fact that it establishes related conversion figures for various occupations, but it also becomes a very important factor in industrial relations.

Cost Control Defined

In offering the foregoing illustrations and examples, it was not intended to deviate from the original theme of foundry costs and cost controls. The author is convinced that the apparent lack of interest on the part of the foundry industry in following a plan to predetermine costs has been due in some measure to lack of information on how this might be accomplished. It was with this thought in mind that the author has sought to explain the function a cost control department might serve, as well as some of the basic steps of a good cost control system.

Perhaps the best definition of cost control is that given by Webster. He states that control is a matter of "directing influence over," "regulating" and "anything affording a standard of comparison or means of verifying." This definition may be applied to cost control.

It might well be said that a cost control department serves management through an assumption of the responsibility of directing, regulating and verifying costs through standards of comparison.

DISCUSSION

Chairman: R. L. LEE, Grede Foundries, Inc., Milwaukee

Co-Chairman: G. TISDALE, Zenith Foundry Co., Milwaukee

A. H. EDWARDS[1]: You mentioned production of hourly against incentive workers. Do you have figures bearing out what the percentage is as to the production that you will get?

MR. WESTOVER: It is generally conceded that on a measured work basis, you can expect the production of hourly workers to average about 60 to 70 per cent of the production of incentive workers when they are producing at standard (Fig. 2). Operating under measured work standards, incentive workers, in order to make extra pay, must go above your standard in production. In other words, if we think of minutes of work produced, the day worker or hourly worker will give you about 40 minutes of work against a standard of 60 minutes being required of an incentive worker before he makes any additional pay.

My paper states that you would use the same standard for measure of work for the hourly worker as you would for the incentive worker. However, do not expect because you set a standard that you are going to obtain from the hourly worker what is termed standard production. Standards will give you a measure of the performance of your operators. Standards will give operators in the so-called hourly or day work shops some idea of what is a fair day's work. I certainly advocate the use of standards, but believe that when you have gone that far, you would want to go on into offering some incentive to induce the operator to increase his production.

MEMBER: At what percentage of capacity operation should you absorb burden? In other words, at what point would you establish your break-even point?

A. E. HAGEBOECK[2]: At about 60 per cent of capacity.

W. E. GEORGE[3]: Would the industry find 60 per cent of its capacity used, do you suppose, over the course of ten years?

MR. HAGEBOECK: We have used 60 per cent for 25 years.

MR. WESTOVER: That might vary with the different branches of the castings industry. I think 60 per cent would be a reasonable figure. I have seen 50, 60, and 65 per cent used. Individual companies may determine a figure of their own.

MR. HAGEBOECK: You are better off to operate at about 80 or 90 per cent of capacity than at 100 per cent capacity.

MR. WESTOVER: I am sure that many plants found during the war that operations over capacity were costly when they passed a certain point.

MR. HAGEBOECK: A good cost system would show that.

MEMBER: What would those costs be?

MR. WESTOVER: One thing in particular is lack of good supervision. In going to third shift operations, for instance, your supervision would be either spread too thin or inexperienced, resulting in high scrap and other waste.

MEMBER: I was thinking of a one shift operation.

MR. WESTOVER: The chances are you would not go over your capacity in one shift operation.

MR. GEORGE: There is overtime for the sixth and seventh days, and that overtime is going to raise your costs per unit of labor.

MEMBER: Then do you base capacity operation on six days?

MR. HAGEBOECK: You base it on five days, 40 hours.

MEMBER: What additional cost do you have between 80 per cent of that and 100 per cent. That is the point I made.

CHAIRMAN LEE: In one of our plants we operated at theoretical capacity. Every molder's floor was filled for four days per week for 27 years. When we operated at actual capacity, our supervision was inadequate to take care of all the molders we had. You always figure a certain group of molders being absent. The situation is the same in the core room or any department. So, personally, I do not think that a practical capacity can be any more than, say, 90 per cent of your theoretical top capacity.

MR. HAGEBOECK: We figure if we operate 80 per cent of capacity we are running full. You have a little leeway there. You are not all tied down. When you have all floors filled, it just does not work.

M. R. VIPOND[4]: If you are operating a foundry below the break-even point, what factor do you consider in arriving at a

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[1] Woodruff-Edwards, Inc., Elgin, Ill.
[2] Frank Foundries Corp., Moline, Ill.
[3] Booz, Allen & Hamilton, Chicago
[4] Aluminum Company of America, Cleveland

selling price below which you would not want to take a job? What factor do you consider in arriving at a price below which you would want to refuse a job? Would you add profit in the job or take it without profit, or without all the overhead?

MR. WESTOVER: I cannot justify taking any job without a profit. That is one of the curses of the foundry industry. People are continually justifying taking a job below cost in order to absorb burden. When they start doing that, they can justify taking every job that they take, below cost, and that is what happened in the 30's. I cannot justify taking any job without at least absorbing your burden cost, and I say it is far better if you figure that you must have a profit.

MR. HAGEBOECK: What is the percentage of fixed overhead?

MR. GEORGE: As I drew the figures for my paper, they were drawn with a fixed overhead of $18,000 a month for that particular chart. That represented 1,000 tons. So that when you got to 1,000 tons, you had $18 overhead. If you had 500 tons, the item was double that or a $36 overhead. I think possibly with the question coming from a captive foundry, they may be wondering how they can compare the cost in a captive foundry with the cost of a supplier who is quoting on castings. When you set up a foundry cost in a captive foundry, you have to decide what you should carry for administrative expenses and what you should add on for your general company burden. You could distribute the top load of the company down to the foundry, where the foundry has to carry so many thousand dollars a month, and you could put that against everything run and arrive at a total cost. Then you would sell your machining division or assembly division castings at your total cost or at your total cost plus a flat percentage, if the company wanted you to show your foundry unit operating at a profit. When you compare with a competitor, I certainly think you have to put a due share of your top company burden allocated back to the foundry division and spread against everything you make, to make a fair comparison. And remember your competitor's price contains the equivalent of that plus a selling cost plus an expected profit. You have not added the selling cost, either, possibly, so that if you followed this estimate form as I set up in my paper, you would get a total plant cost. On top of that, you could estimate a selling and administrative expense. You would have the administrative expense but not the selling expense, so you could take five or ten per cent for selling expense and add that on and say that would be your supplier's total cost.

Now, he undoubtedly has 10, 15 or 20 per cent more added on for profit, so as you would try to compare your total cost with his selling price or his total cost, you have to get as near the same basis as he has in order to make a fair comparison.

MEMBER: In connection with Mr. Westover and Mr. George's statements, I believe that the inference given in connection with setting the selling price is not what your actual costs are but what you figure is a practical or normal cost position for establishing a selling price. In other words, if you are operating a foundry at below capacity you cannot price your goods at below cost. You would price yourself right out of the market. So in endeavoring to arrive at a cost, whether for comparison with the outside, or for selling purposes, you should always be comparing your costs on the basis of what would be considered—say 80 or 90 per cent of capacity, and use that as a basis for establishing selling price. I think that is really what you have in mind. If you got below that, how would you establish a selling price, if you tried to get your actual cost in below capacity? The inference is that you could not. But if you wanted to bring up the capacity of your plant, you should cost the thing on what might be construed as capacity or normal basis, and on that basis you could set a price which ultimately would fill up your plant and give you capacity operation.

MR. WESTOVER: When we talk of basing a price on 60 per cent capacity operations, it is to enable a foundry to at least break even at that point and find their profit in operations above that point. By the use of such a figure they will know that whenever the operation falls below 60 per cent—if they are using that as a basis for figuring costs—that they have a potential loss in everything that they sell. It is true that profits will vary as your percentage of capacity operation varies.

CHAIRMAN LEE: It is usual practice—at least, in gray iron—to use under or over-absorbed overhead. You are right if your theoretical capacity is 60 per cent and you are basing your figures on 60 per cent of that, you base a certain percentage or dollars per ton for your burden. If you fall below that, you do not try to make it up. You still charge at that same rate in establishing your price.

On the other hand, if your operations go above that, then you have an over-absorbed overhead because over a period of years you will have some years above. If you have over and under-absorbed overhead, then you have a chance to maintain a constant level.

I thoroughly agree with you that it is not fair if you are operating at 40 per cent capacity, to try to establish a price on the jobs you are running in your plant on the basis of 40 rather than 60 per cent estimated capacity.

MR. GEORGE: I think we have talked break-evens and normals so much that undoubtedly a lot of you are confused. This may confuse you further. To me, the normal is not the break-even. The normal is the point at which you will make the profit you put into your estimate. When you estimate that you want to make 15 per cent, you find a total normal cost, add 15 per cent to it on inverse figures, or just a straight 15 per cent, and you come up with a selling price. And then if you hit this 70 per cent of your capacity in terms of my chart, if you run 700 tons year in and year out, you will be making the 15 per cent, providing your costs are under control and you can get your actual direct costs in your estimate. Now, this will illustrate what it amounts to in this overhead. If we apply the $18,000 overhead at the break-even point of 500 tons as I showed it, it amounts to $36 per ton. When you are estimating you ought to put in $26 for that burden, or $18,000 divided by the normal of 700 tons. You ought to put in $36. If you put it in, you will be putting $26 into every estimate, knowing that when you hit 700 tons a month, you are breaking even with overhead and making the profit you expected. You are not just breaking even in overall operations but are merely breaking even with your estimate. That is the distinction I want to make between a break-even point and a normal absorption point.

MR. HAGEBOECK: I think we all over-estimate the percentage of costs for fixed charges. You are just kidding yourself when you take a lot of work below cost in order to absorb overhead.

It is considered good practice to take your iron figure at current costs even if you operate under the 60 per cent for the reason that in operating a cupola in a gray iron foundry the percentage of operation does not materially affect this item. However, overhead or burden in all other departments will be materially effected when running under the 60 per cent. Mr. Lee has referred to over and under-absorbed burden, an item that most foundries watch very closely, and over the years, in normal times, we had developed percentage figures that will average out over a five or ten-year period. However, it must be remembered that if costs will continue to increase as they did during the war period and as they are now increasing in the postwar period, we may all find it necessary to establish higher standard figures as a safe yardstick.

I agree with Mr. Westover that there is very little justification for ever taking a job for less than cost.

MR. WESTOVER: The greatest variable we have in costs in the foundry business is our labor item.

MR. HAGENBOECK: That is right.

MR. WESTOVER: Burdens can vary depending on company policy. So we must list as an important variable, company policies. The matter of the variable labor in the production of castings varies greatly with foundries. I think that is the reason so much effort has been put forth by the Time Study and Job Evaluation Committee in trying to bring to you the method and means of establishing standards of time for production. If you have a true measure of work in the making of your castings, you are going to come close to controlling your cost and you have the essential thing that will give you nearer a true cost than you have without standards. I believe we are going to talk more and more about standard costs, and standard costs means coupling standards of measure, such as the measuring of time by standard data as well as measuring your materials and the other things that go into castings.

PHYSICAL PROPERTIES OF MOLDING SANDS

By
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ABSTRACT

One goal of laboratory sand testing is to obtain maximum control over the composition and properties of sand mixtures by means of standard methods. Equally important, however, is the attainment through laboratory testing of control over actual mold quality.

By means of testing techniques based on standard procedures, but modified to take into account variables frequently encountered in production practice, a method is described for accumulating data useful in defining the range of values over which production mold properties may extend. A technique is also described whereby estimates of any mold property may be obtained by non-destructive testing.

IT IS NOW COMMON FOUNDRY PRACTICE to determine the properties of green molding sands by testing under the controlled conditions defined in the standards and tentative standards adopted by the American Foundrymen's Association.[1] These tests are of great benefit in achieving control over sand mixtures, and by virtue of their nearly universal recognition permit ready exchange and understanding of test data among foundry personnel.

It is apparent, however, that production foundry practices introduce certain variables not accounted for in laboratory testing techniques. Although a high degree of control may be maintained over sand compositions by standard testing at the time of mixing, it does not necessarily follow that this control is automatically extended to include the quality of molds and castings. The author believes that in addition to making standard tests for the purpose of controlling sand mixtures, it is also desirable and beneficial to understand thoroughly the extent to which operating variables are capable of affecting mold properties.

For example, laboratory testing might indicate the moisture content of a green molding sand to be optimum, and physical properties to be satisfactory in specimens rammed with three blows of the rammer according to the standard method. Assuming the test sample was taken from a freshly prepared mix, many factors may cause the moisture content of the sand to change by the time it is finally rammed into a mold. Furthermore, variations of the degree of ramming in production practice may be of considerable magnitude, with the result that finished molds are likely to have properties different from laboratory test specimens.

It is the purpose of this paper to demonstrate the extent to which several mold properties can vary when sand moisture content is not a constant, and when the ramming energy is other than the rigidly controlled value used in making standard tests. The fact that such variations occur is, of course, well known. An attempt is made herein to define the variations in a quantitative manner and to demonstrate useful correlations based on the data.

Materials and Specimens

Two typical synthetic molding sands of the following composition were selected for the tests:

MIX A

Bank sand, A.F.A. Fineness No. 150, clay content, 0.9 per cent
Western bentonite, 4.50 per cent
Cereal flour, 0.50 per cent

MIX B

Washed silica sand, A.F.A. Fineness No. 70
Western bentonite, 4.00 per cent

Using these basic formulas, six mixes of each composition were prepared which differed only in their water content. The moisture was varied in steps over the entire workable range for each basic mix. Control over uniformity of the mixes was obtained by carefully weighing all ingredients from standardized stocks, by establishing the mixing time at one minute dry and 15 minutes wet in a covered muller, by aerating each mix through a six mesh screen, and by storing the mixed sand in sealed containers which were opened only to remove sand as each test specimen was produced. Frequent checks were made to insure that no change in moisture content occurred while any series of tests was in progress.

Method of Study

In addition to standard specimens made by ramming with three blows of the rammer, specimens were also made with one, two, four and five blows. The test specimens of each basic mix, therefore, varied in moisture content within the workable limits and also varied

[1] *Foundry Sand Testing Handbook*, published by American Foundrymen's Association, Chicago, fifth edition (1944).

in hardness corresponding to a range which would be classified from "soft-rammed" to "hard-rammed."

The following properties were determined for each possible combination of moisture content and degree of ramming:

Permeability, A.F.A. standard method
Compressive strength, A.F.A. standard method
Deformation, experimental method
Tensile strength, A.F.A. standard method
Surface hardness, A.F.A. standard method
Apparent density, experimental method

All specimens were tested immediately after forming and the tests were made in triplicate. Average values only are used in presenting the data. In considering each physical property determined in these tests it was necessary to deal with three variables; the data, therefore, has been assembled in the form of plan views of three-dimensional graphs. The magnitude of each property with variations in moisture and ramming is shown by means of contour lines which pass through points of equal elevation.

Property Test Results

The data presented in Fig. 1 to 12 inclusive define the physical properties of Mixes A and B throughout the ranges tested. Optimum moisture for best working characteristics was considered to be about 4.3 per cent for Mix A and 2.5 per cent for Mix B. In each case it will be seen that the properties vary considerably when both the moisture content and ramming energy are changed from standard values, and changes follow smooth, characteristic patterns for each property.

Permeability. Figures 1 and 2 illustrate the permeability ranges of Mixes A and B and show same general type of pattern. Both mixes provided the highest permeability at a definite moisture content for any specified degree of ramming. The permeability of Mix A decreased when moisture was raised or lowered from 4.0 per cent. The critical moisture content for Mix B was 2.0 per cent. Employing any moisture content in the range tested, the permeability of both mixes decreased as ramming was increased and the rate of decrease became less as more ramming energy was applied.

Of particular interest is the amount of change in permeability which occurred over the entire range of these tests. The results demonstrate that if moisture content and ramming were varied in production use of these sand mixes to the extent indicated, molds made with Mix A might have a permeability anywhere between 22 and 43 while the permeability of molds made with Mix B might fall between 95 and 170. Thus the effect of possible production variables should be taken into account in interpreting standard laboratory tests, which would have indicated the permeability of Mix A to be 33 and that of Mix B to be 126.

Compressive Strength. Figures 3 and 4 show the compressive strength of the test mixes with the same variations in moisture content and ramming previously described. Again it was found that the pattern of this property was similar for the two mixes. Highest compression strength with Mix A was achieved with about 3.0 per cent moisture for any specified ramming energy. This peak is not defined clearly for Mix B since it appears to fall at a moisture content below that required for safe molding qualities. Extrapolation would indicate the peak to fall between 1.0 and 1.5 per cent moisture which is outside the usable range.

Moisture and Ramming Variations

It is again of interest to note the wide range of compressive strength values which resulted from variations in moisture and ramming. Values ranging from less than 3.5 psi to over 8.0 psi were obtained, while the standard test results indicated the compressive strength of Mix A to be 5.9 psi and of Mix B to be 5.3 psi.

Deformation. The effects of variations in ramming and moisture on deformation of Mixes A and B are

Fig. 1—Permeability range chart of mixture A.

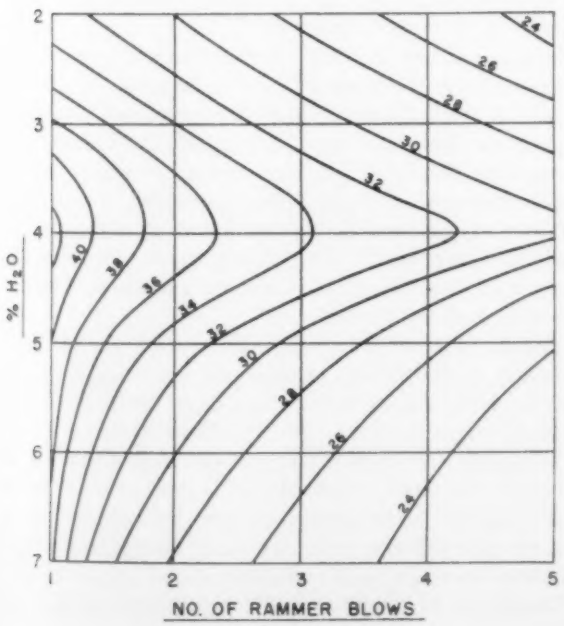
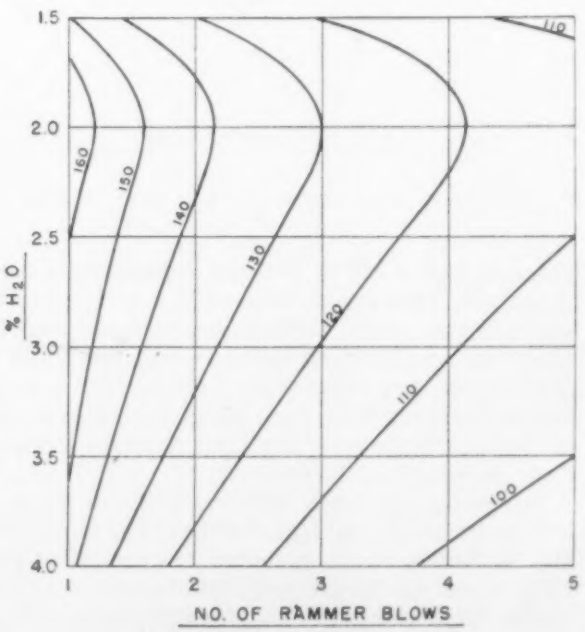


Fig. 2—Permeability range chart of mixture B.



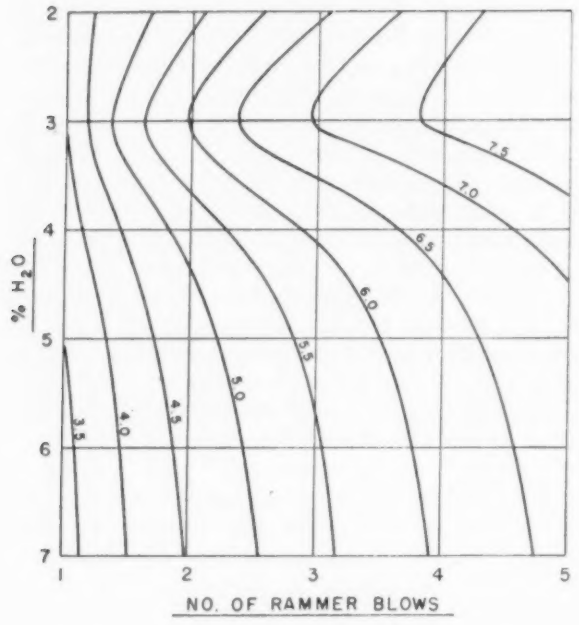


Fig. 3—Green compressive strength (psi), mixture A.

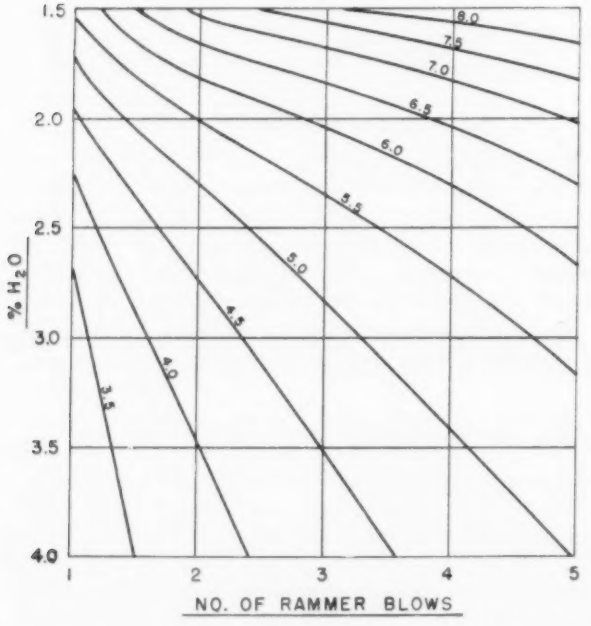


Fig. 4—Green compressive strength (psi), mixture B.

shown in Figs. 5 and 6. Changes in ramming did not appreciably affect deformation of Mix A except in the high moisture range. Deformation increased over the entire range as moisture was increased. With Mix B, ramming affected deformation to about the same extent as moisture. With both mixes, it was found that in the high moisture range hard ramming caused a slight decrease in deformation.

Values ranging from 0.006 to 0.030 in./in. were found with Mix A and from 0.006 to 0.015 in./in. with Mix B. Values for the standard test conditions were 0.016 in./in. for Mix A and 0.008 in./in. for Mix B. Unlike the permeability and strength results, the deformation property was not found to peak at a particular moisture content but, in general, increased directly as the moisture was increased.

Tensile Strength. The tensile strength test results, shown in Fig. 7 and Fig. 8, revealed an unusual pattern for both mixes in that optimum amounts of both ramming and moisture were found required to obtain the peak green tensile strength. To define this relationship in a different manner, it was found that in the low moisture ranges for both test mixes, ramming energy beyond certain amounts caused the tensile strength to decrease.

No explanation of this phenomenon is offered, but if such a condition actually exists it would have practical significance when ramming molds with sand tempered in the dry range. Again it was found that variations in moisture content and ramming resulted in tensile properties widely different from those obtained under standard test conditions. The standard tensile strength values were 7.0 oz. per sq. in. for Mix A and 13.7 oz. per sq. in. for Mix B.

Property Patterns

Surface Hardness. Determination of surface hardness of the test mixes, shown graphically in Figs. 9 and 10, reveal that the pattern for this property approximates the compressive strength pattern, the two properties being roughly proportional. Both hardness and compressive strength appear to reach peaks at the same moisture content for each test mix. Hardness values ranging from 45 to 85 were found with Mix A and from 45 to 75 with Mix B. The hardness values determined under standard test conditions were 71 and 68 respectively.

Density. As a matter of further interest, specimens of the two mixes were formed under the various conditions of moisture content and ramming and the apparent density determined. The results, shown in Figs. 11 and 12, reveal similar patterns to those obtained in the permeability tests, with the permeability inversely related to density. The density of Mix A varied between 86 and 95 lb per cu ft and that of Mix B between 91 and 98 lb per cu ft.

Non-destructive Testing. As the data were assembled and plotted, it was found that the various sand properties followed characteristic patterns when moisture content and ramming were varied. It then became apparent that the determination of one property under known moisture and ramming conditions would permit other properties to be estimated by reference to the same intercept on the other graphs. Practical advantage of this principle might be gained in non-destructive testing of molds.

The surface hardness test, which can be made conveniently at almost any location on a mold surface, results in a small indentation but does not destroy the mold or affect its serviceability. Moisture tests made of a representative sand sample taken at the time of ramming the mold, combined with the hardness value as determined at the mold surface, provide sufficient data to establish the exact ramming energy applied at the mold location under consideration.

Reference to graphs of other physical properties.

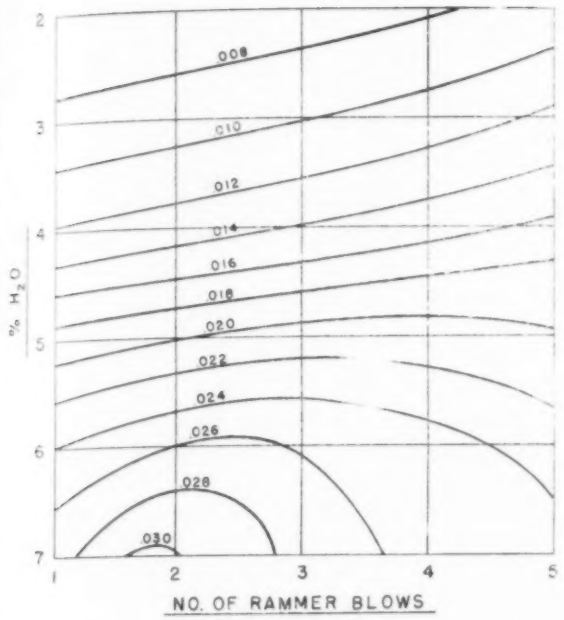


Fig. 5—Deformation range chart of mixture A (in./in.)

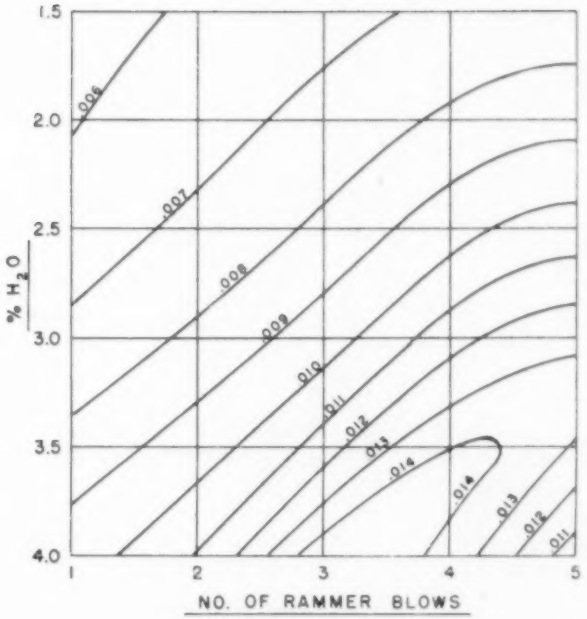


Fig. 6—Deformation range chart of mixture B (in./in.)

employing these established values of moisture and ramming, would permit estimates to be made of these other properties existing in the mold. It is, of course, necessary to maintain control over the composition and properties of the production molding sand by the usual standard tests.

Conclusions

1. It has been found that several physical properties of two basically different synthetic molding sands follow similar trends when test specimens were molded under various combinations of moisture content and ramming energy. Although the magnitude of the values was found to be quite different for those properties which were influenced by the grain size of the two test mixes, the effects of changes in moisture and ramming on these properties conformed to the same general pattern.

2. The magnitude of the changes in physical properties of the test mixes which resulted from variations in moisture content and ramming within the workable limits was found to be very great. These data emphasize the importance of knowing the extent to which moisture and ramming vary in production use of molding sands, and of understanding the effect of these variations on the properties of molds as com-

Fig. 7—Green tensile strength (oz./sq.in.), mixture A.

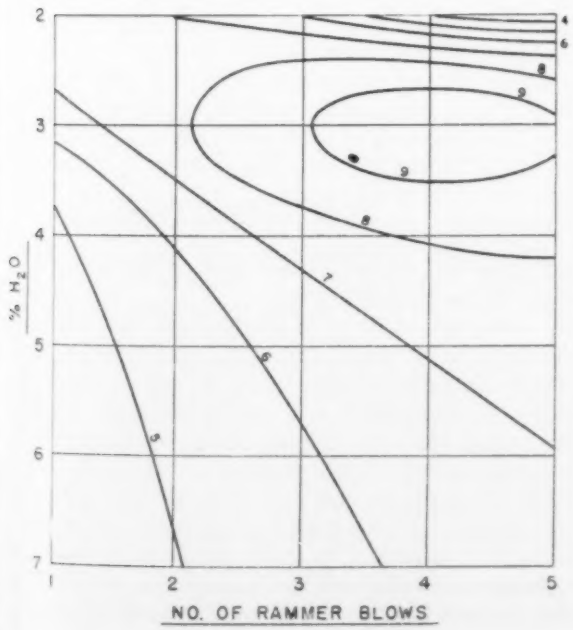
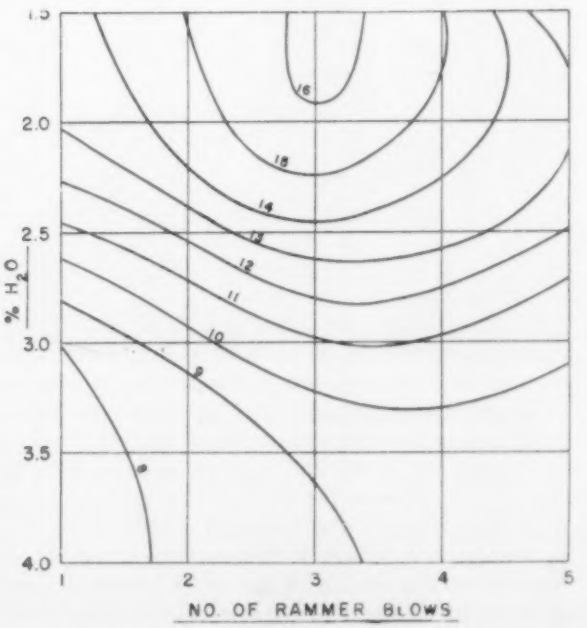


Fig. 8—Green tensile strength (oz./sq.in.) mixture B.



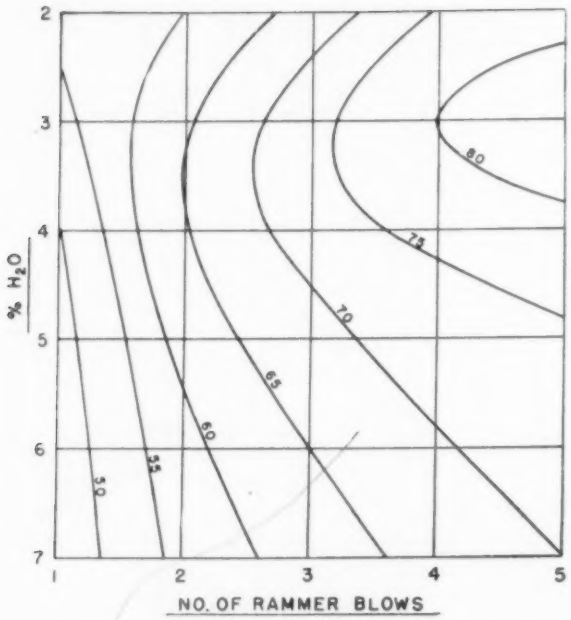


Fig. 9 — Surface hardness range chart of mixture A.

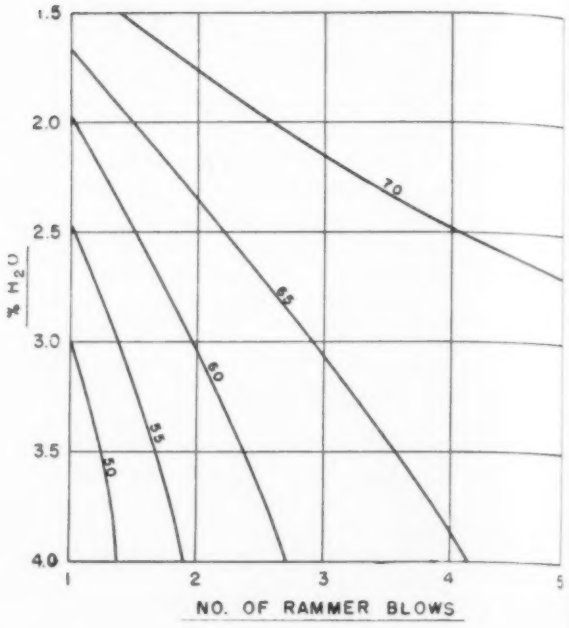


Fig. 10—Surface hardness range chart of mixture B.

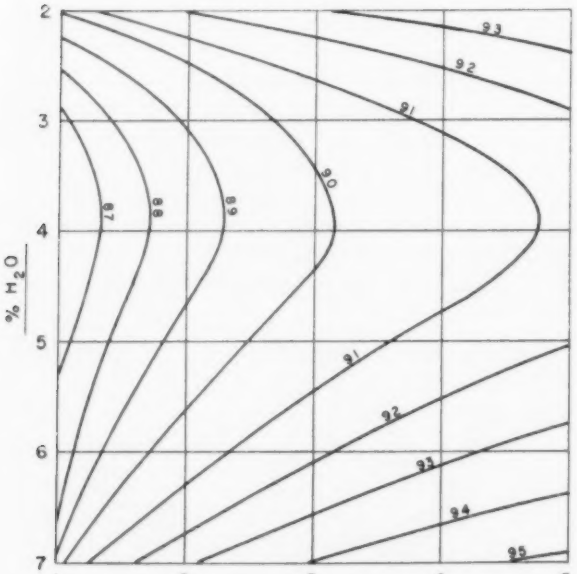


Fig. 11 — Apparent density of mixture A (lb./cu.ft.).

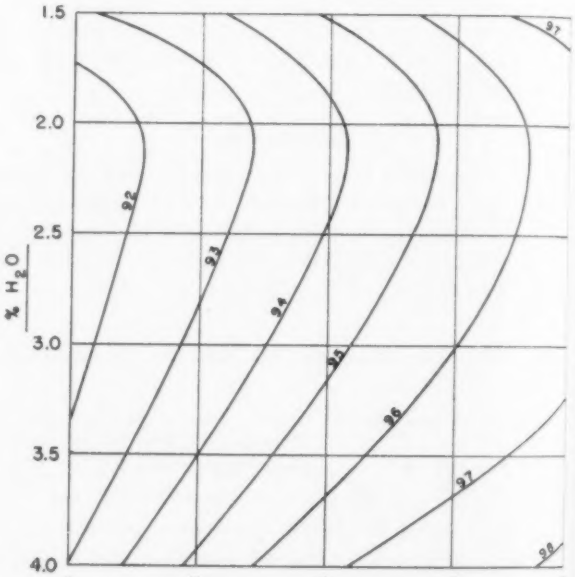


Fig. 12—Apparent density of mixture B (lb./cu.ft.).

pared with the values obtained when testing the sand under standard conditions.

3. It is believed that when a molding sand has been surveyed by the method presented, the data would then permit the non-destructive testing of any molds produced with that sand. It would be necessary to make a moisture determination of the sand just prior to making the mold, and surface hardness determinations at any desired location on the mold. The point of intersection on the hardness graph of the values so determined defines a reference point which, when applied to the graphs of the other properties, would provide an estimate of permeability, strength, or any other property previously surveyed, at the location of the mold under examination.

4. Although fairly close correlations were found to exist between certain properties, for example, compressive strength and hardness, or permeability and density, the data taken as a whole indicates that most sand properties vary independently of other properties when moisture and ramming are changed. In selecting an optimum moisture content and degree of ramming for a molding sand as applied to a particular casting, it is necessary to make compromises based on requirements of the part and knowledge of sand properties beyond that provided by standard test results.

SURVEY OF
FOUNDRY COKE CHARACTERISTICS

By
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Columbus, Ohio

EFFICIENT UTILIZATION OF COKE in the cupola requires a knowledge of the characteristics of coke which have the greatest effect upon the economy and efficiency of cupola operation and upon the quality of the iron produced. At the present time, the foundryman has no practical laboratory tests by which he can reliably predict the behavior of various foundry cokes in his particular cupola practice. If such tests were available, they could serve as a basis for coke specifications and would enable cupola operators to make adjustments in practice to obtain maximum performance from cokes of different characteristics.

It is the purpose of this paper to review and summarize the published information on foundry coke, and to present test data which help to define the problem. These data were selected from tests conducted on foundry cokes in connection with cupola studies made by the Gray Iron Research Institute, Inc. It is believed that a critical summary will be of value to the cast iron industry in general. This summary is intended to stimulate thought on the problem of foundry coke, to prevent duplication of past efforts, to summarize lines of attack which have been fruitless in the past, and to indicate a new research method which may prove successful.

I. Conclusions

A review of the literature and supporting experimental studies indicate that, up to the present time, no suitable tests or combinations of them have been made available to foundrymen for predicting the behavior of foundry coke in any particular type of cupola operation. The problem is further complicated by the wide diversity of types of cupola operation. Laboratory tests have shown only limited and doubtful correlation with full-scale commercial operations. Of the various properties of coke tested to the present time, uniformity of size is reputed to be the most important factor controlling commercial operation. Coke strength seems to be the factor of next importance.

Conventional commercial cupola operation at its best is beset with many variables—coke being only one of them. These variables are so difficult to eliminate that it becomes an extremely difficult problem to adapt the performance of large commercial cupolas to the obtaining of reliable quantitative data which will permit prediction of the performance of various cokes when used in other commercial cupolas.

It seems reasonable to expect that a small cupola operated under rigorously controlled and standardized conditions can provide the necessary criteria for the qualitative comparison of the behavior of foundry cokes. However, the comparison of the behavior of such cokes in larger commercial cupolas under various conditions would still have to be ascertained. Although this may present a considerable task, hope is provided for the development of a test that can be reliably interpreted and which may provide the desired ability to predict coke performance in a cupola. A fresh start has been made in that direction. Figure 1 shows a 10-in. diameter cupola which has yielded highly reproducible performance over a considerable number of heats. The results obtained using this cupola as a tool for assessing coke performance will appear in future publications.

II. Review of Foundry Coke Literature

Chemical Abstracts was used as an index to publications on coke from 1936 to 1946. From approximately

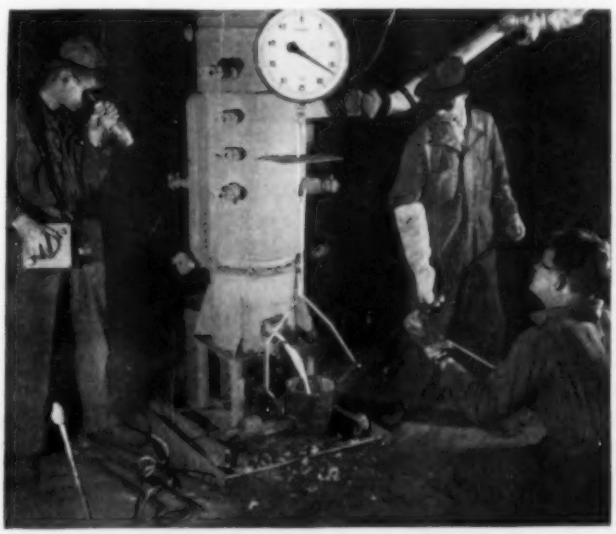


Fig. 1—Experimental 10-in. cupola in operation.

Tapping Temperature ..... *2760 to 2820 F*
Coke Size ..... *1¼ x 1¾ in.*
Coke Ratio ..... *7½ to 1*
Air Rate ..... *11 to 12½ lb/min*
Melting Rate ..... *600 to 700 lb/hr*

* Executive Director, Gray Iron Research Institute, Inc.

** Research Engineer, Battelle Memorial Institute.

in 1941 and the subcommittee later disbanded. The land, Bulgaria, Poland, Italy, Rumania, and Brazil. 158 were selected as bearing upon the performance of coke in the cupola. The selected articles, listed in the bibliography at the end of this paper, originated in fifteen countries as follows: England,41; United States, 38; Russia, 32; Germany, 25; France, 7; Japan, 4; Scotland, 2; India, 2; and one each from Belgium, Switzer-

Scope of Literature Review

Some thought was given to the advisability of extending this review of the literature back beyond 1936. However, after a review of several of the major papers published between 1925 and 1935, it was concluded that most of the significant work done before 1936 has been included, summarized, or modified in later papers. The review from 1936 to 1946, inclusive, therefore, is believed to satisfy the requirement of a general picture of the present situation regarding foundry coke.

Each of the selected publications was abstracted and the general summary has been prepared according to the following outline:

A. SPECIFICATIONS
B. USUAL ANALYTICAL AND TEST METHODS
   1. Sampling
   2. Chemical Analysis
   3. Calorific Value
   4. Mechanical Strength
   5. Other Tests
C. COMBUSTION AND COMBUSTION TESTS
D. REACTIVITY AND REACTIVITY TESTS
E. PROPERTIES
   1. General Reviews
   2. Special Tests
F. STRUCTURE
G. CUPOLA OPERATION
   1. General Reviews
   2. Coke Size
   3. General Operation
H. BLAST-FURNACE OPERATION
I. CHEMICAL TREATMENT
J. PRODUCTION

A. SPECIFICATIONS

Standard specifications for foundry coke are limited to test methods, procedures, and nomenclature. The desirable range of properties is not indicated nor specified in any known standard specifications. There may be, however, some instances in which progressive individual plants or organizations have adopted unpublished specifications which specify particular properties.

Standard *test methods* are published by the American Society for Testing Materials to cover sampling procedures and several common tests.[1]

Standard Specification for Foundry Coke D 17-16 of the ASTM was written in 1916. This standard specified the following proximate analysis for foundry coke on a dry basis:

Volatile Matter	–	Not over 2.0 per cent
Fixed Carbon	–	Not under 86.0 per cent
Ash	–	Not over 12.0 per cent
Sulphur	–	Not over 1.0 per cent

The subcommittee charged with revision of this outdated specification reported in 1941[2] that the specification had outlived its usefulness, and that it was not possible at that time to prepare general foundry-coke specifications which would cover current operating practices. Accordingly, the specification was cancelled ASTM is not carrying on any activities bearing on foundry-coke specifications at the present time.*

There are numerous statements throughout the literature deploring the lack of suitable consumers' specifications, but there are as yet no universally accepted standards for controlling and specifying foundry-coke quality. Each producer has his own methods for plant control of coke quality. Such individual control tests are often based upon the personal observations of a foundry engineer or sales engineeer employed by the coke producer.

The deficiency of acceptable consumers' standards is largely due to the fact that the interrelations of factors that make up coke quality are still undetermined. The determination of the properties of coke that affect commercial operation is prerequisite to the adoption of suitable standards.

B. USUAL ANALYTICAL AND TEST METHODS

1. *Sampling*—Sampling procedures and discussions found in the literature emphasize the necessity for obtaining accurate and representative samples of coke to be tested. The significance of subsequent tests depends upon the accuracy of the sample.

The procedures and precautions contained in ASTM Standard Method of Sampling Coke for Analysis D 346-35 for collecting the gross sample, crushing, mixing, and reduction are typical of various recommended practices and are regarded acceptable for general use.

All of the recommended sampling procedures are based upon obtaining large gross samples by the collection of a number of randomly selected increments. ASTM Method D 346-35 recommends that, for foundry coke, a minimum gross sample weight of 250 lb. be collected in not less than 25 nor more than 50 approximately equal random increments. This practice attempts to average out heterogeneity and variation in properties usually found within a single oven of coke.

Coke Sample Location

There is an excellent possibility that information of greater usefulness might be obtained by testing coke samples selected from certain specific locations within the oven. Such specific sampling from locations next to the oven wall and at the oven center is illustrated later in this review and has also been attempted on a limited scale by some producers. The difficulties attendant on such sampling, however, render it impractical for routine testing.

2. *Chemical Analysis*—The *proximate analysis* of coke consists of the determination of moisture, volatile matter, ash, and fixed carbon (by difference). *Ultimate analysis* consists of the determination of carbon and hydrogen as found in the gaseous products of complete combustion, the determination of sulphur, nitrogen, and ash in the material as a whole, and the estimation of oxygen by difference. The standard American procedures for determining both the proximate and ultimate analysis of coke are contained in ASTM Specification D 271-44. A number of foreign references suggest variations of the American methods.

*Private communication from Mr. A. C. Fieldner, Chairman ASTM Committee D-5 on Coal and Coke.

When analyzing cokes for proximate analysis, it is necessary to adhere rigidly to the prescribed methods as the results obtained may be different if the procedures are altered. Minor variations in temperatures and times can appreciably alter the results. Until more refined methods are developed, it is advisable to adhere to the present ASTM methods so that the results obtained by different laboratories may be compared on the same basis.

Heating Value Determination

3. *Calorific Value*—The calorific or heating value of coke is determined quantitatively by the use of calorimeters. The actual determinations are rather laborious to make and require considerable precision. However, fairly good correlations have been worked out between the proximate analysis of cokes and their calorific value[24]. These correlations are sufficiently accurate for most purposes and, with few exceptions, may be used to eliminate the necessity for making the actual calorimetric tests.

4. *Mechanical Strength*—Many workers on the problem of coke quality feel that mechanical strength is the determining factor in coke quality. The usual tests for mechanical strength are the shatter test and the tumbler test. The shatter test is made by dropping sized coke through an appreciable distance under standardized conditions and measuring the degree of shatter due to the dropping. The tumbler test is made by rotating a standard sample of coke in a standard drum for a specific number of revolutions and measuring the degree of breakup. Standard methods for making both the shatter test and the tumbler test are specified by ASTM.[1]

The shatter and tumbler tests as specified by ASTM are not regarded as satisfactory for general use in testing foundry cokes. The shatter test was originally developed for testing blast-furnace coke. In the application of the test to foundry coke, some producers have developed variations of the test which use larger sample weights, larger coke before testing, and a more detailed screen analysis of the shattered coke.

The modifications of the shatter test vary from producer to producer, depending upon their individual experiences and the amount of thought which they have given to the problem. Variations of the tumbler test are also found, but this method is used in routine testing to a much less degree than the shatter-type test.

Specify Test Conditions

The ASTM shatter and tumbler tests both have the disadvantage of being arbitrarily applied to cokes of certain sizes only. However, the number of modifications of these tests is so great that the exact conditions of the test should be specified when results are reported. Other types of mechanical tests such as impact strength, grinding strength, and bending strength have also been applied to foundry coke, but none of them have yet been proven to be acceptable universally for the prediction of cupola performance under various conditions.

5. *Other Tests*—Tests for density, porosity, volume of cell space, true and apparent specific gravity, ash fusion temperature, and others are frequently applied in the routine testing of cokes. Most of these tests are included in ASTM specifications. Foreign workers use similar tests varying from ASTM tests in some details.

C. Combustion and Combustion Tests

Combustion is considered as the reaction of coke with air or oxygen. Combustibility is considered at the *rate* of reaction of coke with air or oxygen. There is no standard test procedure for determining the combustion characteristics or combustibility of coke, although numerous tests are proposed in the literature. Most of these are made on small laboratory samples which are heated in a stream of oxygen or air.

Combustion tests are usually designed to determine the ignition temperature of the coke under specific conditions. The ignition temperature changes as the conditions of the test are changed.

Combustibility is sometimes measured by continuously weighing a sample while burning under standardized conditions. The rate of loss of weight is used as a measure of the combustibility. Another method is to determine the rate of temperature rise while the sample is burning under standard conditions, and to use some function of the temperature rise as an index of the combustibility. This latter method is the one used in the Boegehold Combustibility Test[45].

Sample Size Variations

The major disadvantage of most combustion and combustibility tests is the small size of the sample used. The combustion characteristics of coke are greatly dependent upon particle size. The effect of particle size is so great that changes in the size of the coke pieces in the sample may affect the test results more than does the inherent combustibility of the coke. For this reason, combustion test results are difficult to correlate with commercial practice.

The most precise combustion and combustibility tests are rather complicated to perform. Variations in the size of the sample, particle size of the pieces in the sample, percentage of oxygen in the gas stream, gas-stream pressure, gas-stream velocity, rate of heating, and physical arrangement of the test apparatus all affect the test results.

For coke of a given size in a given test, the following factors have been credited with affecting combustion characteristics: particle size of the coal charge, coking time, coking temperature, coking rate, ash content, volatile-matter content, porosity, cell size, and degree of graphitization.

The literature shows no indication of a correlation between combustion characteristics as measured in a laboratory test and regular commercial operation. Combustion characteristics of the coke must undoubtedly affect commercial operation, but it is apparent that other factors in operation have considerably more effect than the measured combustibility. Therefore, combustibility tests by themselves are of doubtful value.

D. Reactivity and Reactivity Tests

Reactivity is defined as the rate of reaction of coke with carbon dioxide to form carbon monoxide. The reactivity of coke is important in cupola operation because the use of a highly reactive coke usually results in excessive formation of CO in the charge preheating

zone. Excessive CO not only usually results in lower melting-zone temperatures because of incomplete combustion, but it also results in high latent heat losses in the stack gas because of the presence of so much unburned CO.

With highly reactive coke, considerable coke is also lost by solution, i.e., by the reaction of coke with CO_2 to form CO. Therefore, coke ratios and thermal efficiencies are apt to be low when high-reactivity coke is used, and low-reactivity cokes in general would seem to be desirable for cupola melting.

Factors Affecting Test

Reactivity has been determined by a number of test methods. Essentially, all of them consist of passing CO_2 over coke heated to a definite temperature and determining the percentage of CO and CO_2 in the effluent gas. The actual test results vary with the temperature of the coke, temperature of the CO_2, pressure, CO_2 velocity, particle size of the sample, and other factors. Under given conditions, reactivity is affected by the same factors affecting combustion characteristics.

Reactivity tests are more difficult to perform than combustibility tests and are subject to the same disadvantages. As in the case of combustion tests, reactivity has not been satisfactorily correlated with actual operation. Other factors mask the effect of reactivity and make such correlation exceedingly difficult. Reactivity has received considerably more attention in gas-producing operations and blast-furnace practice than in cupola operations.

E. Properties

1. *General Reviews*—Thirteen general review articles on coke properties are listed in the bibliography. The first of these[73] is the most comprehensive single reference encountered in this review, although many of the coke properties shown are not representative of good-quality American coke.

A summary of these articles shows the following coke factors to be considered as the most important in affecting commercial operation. They are listed in order of apparent importance:

a. Uniformity of size
b. High strength and hardness
c. Low reactivity
d. Low ash and low sulphur

Size and strength taken together apparently constitute the best measure of coke quality known at present.

These review articles are generally unanimous in pointing out the failure of present methods of testing to evaluate the cupola operating characteristics of coke. Support is also given for the belief that coke quality can be best assessed by use of full or pilot-scale tests.

2. *Special Tests*—In addition to the usual tests listed under USUAL ANALYTICAL AND TEST METHODS, a number of special tests have been applied to coke in an effort to determine some property that would correlate with commercial operation. At the risk of repetition, but for the sake of completeness, the following list includes the *usual* and *special* tests most frequently encountered in the literature (see adjoining column).

As each of these tests has been made under numerous different conditions, each condition constituting a test in itself, it is apparent that considerable effort has been directed toward the solution of the problem. As none of the tests have yielded the desired results, it is interesting to note that all of them are laboratory tests. With the present background, therefore, it appears logical that laboratory testing should be augmented by other methods of testing, and that the results of such tests should be more carefully correlated with commercial operation.

It should also be pointed out that the suitability of coke for a certain type of cupola operation will probably involve the combination of several factors. Individual laboratory tests that have been used up to the present time apparently do not satisfactorily indicate the combined effect of these factors. Also, the relative importance of each property is not known. The solution of the problem will probably not be obtained by measuring properties independently. For a hypothetical example, let

S = coke strength
C = combustibility factor
k = empirical factor

After testing a number of cokes it might be found that S x C x k equals a useful index of coke performance. S and C might vary independently of each other so that individual determinations of S and C would give little useful information, but a large number of combinations of S and C will give the same product.

F. Structure

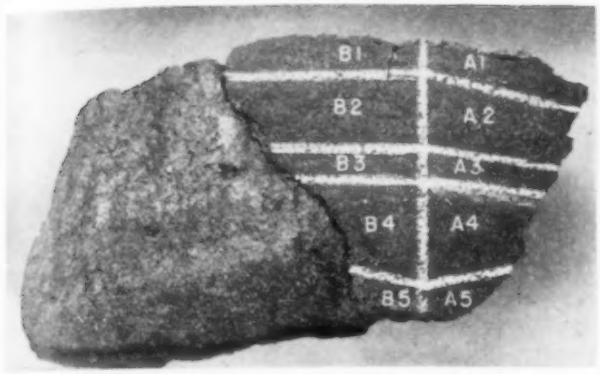
A number of investigations have been made of the structure of coke, chiefly through the use of X-rays. The information obtained provides some fundamental back-

Usual Tests

Proximate analysis (moisture, volatile matter, ash, fixed carbon)
Ultimate analysis (C, H_2, O_2, S, N_2, ash)
Total moisture
Shatter test
Tumbler test
Apparent specific gravity
True specific gravity
Volume of cell space (porosity)
Bulk density
Ash fusion temperature
Composition of ash
Sieve analysis
Calorific value (heating value).

Special Tests

Combustibility and combustion
Reactivity
Electrical conductivity
Thermal conductivity
Specific heat
Permeability
Volatile therms
Luster, color, and reflectivity
Critical air blast
Microstructure
Macrostructure
Crystal structure (X-ray)
Selective flotation
Impact strength
Compressive strength
Elevated temperature strength
Wet oxidation.



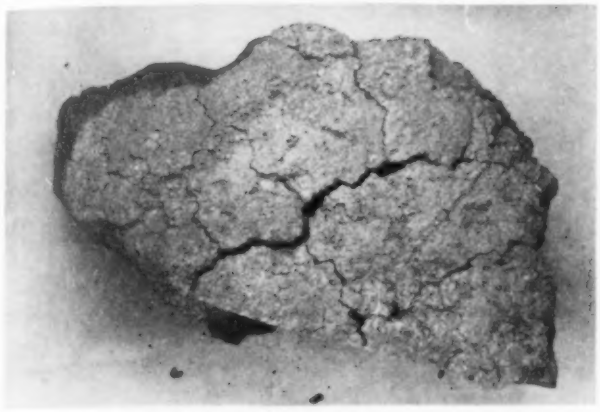




Fig. 2—Location of samples from lump of incompletely carbonized by-product coke showing differences in appearance from side adjacent to coke-oven wall to side at center of oven. Top, transverse view through oven. Oven wall is at top of photograph. Oven center is at bottom (against scale). Note darkening of coke towards oven center. Center, oven wall side. Saw cut at bottom shows location of slice taken for testing. Note fissuring, relatively smooth surface, and "cauliflower" appearance. Bottom, oven center side. Note dark, rough appearance of this coke specimen.

ground, but little has been developed that can as yet be applied to the solution of practical problems.

G. Cupola Operation

1. *General Reviews*—An English paper by O'Neil and Pearce in 1937[108] reported on a complete series of tests intended to correlate laboratory tests with performance in the cupola. It was concluded that none of the laboratory tests used were capable of predicting behavior in operation. Other papers report on the advisability of having coke of uniform size and suggest the use of baby cupolas for coke tests.[110]

2. *Coke Size*—The results of changing coke size are somewhat controversial. By using smaller coke, temperature is sometimes increased, sometimes lowered; melting rate is sometimes raised, sometimes lowered. Blast pressure generally increases with the use of smaller coke. Variations in the reported results of a number of independent tests from different sources are probably due to uncontrolled variables in cupola operation, differences in cupola construction, variations in charged materials, and nonuniformity of coke size. It is generally considered that the efficient operation of a given size of cupola requires the use of coke within certain limits of maximum and minimum size. Within these limits, it appears that the *uniformity* of coke size has more bearing upon operating results than the actual size itself.

3. *General Operation*—Several papers on the effects of coke ratio and blast volume upon the composition of the stack gases are not too enlightening, although an early paper (1913) by Belden[119] shows the gas composition and indicates the temperatures found in various parts of a coke bed under blast. Belden shows the presence of an inverted conical zone of max. temperature.

The ability of a cupola to melt iron under adverse conditions is shown by the fact that cupolas have been operated on anthracite, peat, charcoal, and wood.

As a general observation, various tests on cupola operation reported in the literature have paid very little attention to the quality of the coke being used.

H. Blast-Furnace Operation

The use of coke in blast furnaces has received considerably more attention than the use of coke in cupolas. Thirteen articles dealing with blast-furnace operation that have been included in this review show that the type of work being done in this field closely parallels that being done with respect to cupola operation. The problems in regard to blast-furnace operation are naturally somewhat different from those in cupola operations. Even so, the present conclusions regarding blast-furnace coke are very similar to those for cupola coke. Uniformity of size and high strength appear to be the two most desirable features. In each case reported, a change from unsized to sized coke resulted in improved furnace operation. Several papers conclude that actual use in a blast furnace is the only good measure of coke quality.

I. Chemical Treatment

The possibility of altering the properties of coke by chemical treatment has received considerable attention. Most of this activity has been directed toward increasing the reactivity of coke to be used in gas-producing reactions, gas absorption, and chemical processes other than combustion. For cupola coke, it is usually desired to go the other way and to reduce reactivity.

Salts of calcium, strontium, vanadium, zinc, manganese, lithium, barium, cadmium, chromium, copper, mercury, potassium, sodium, aluminum, silicon, iron,

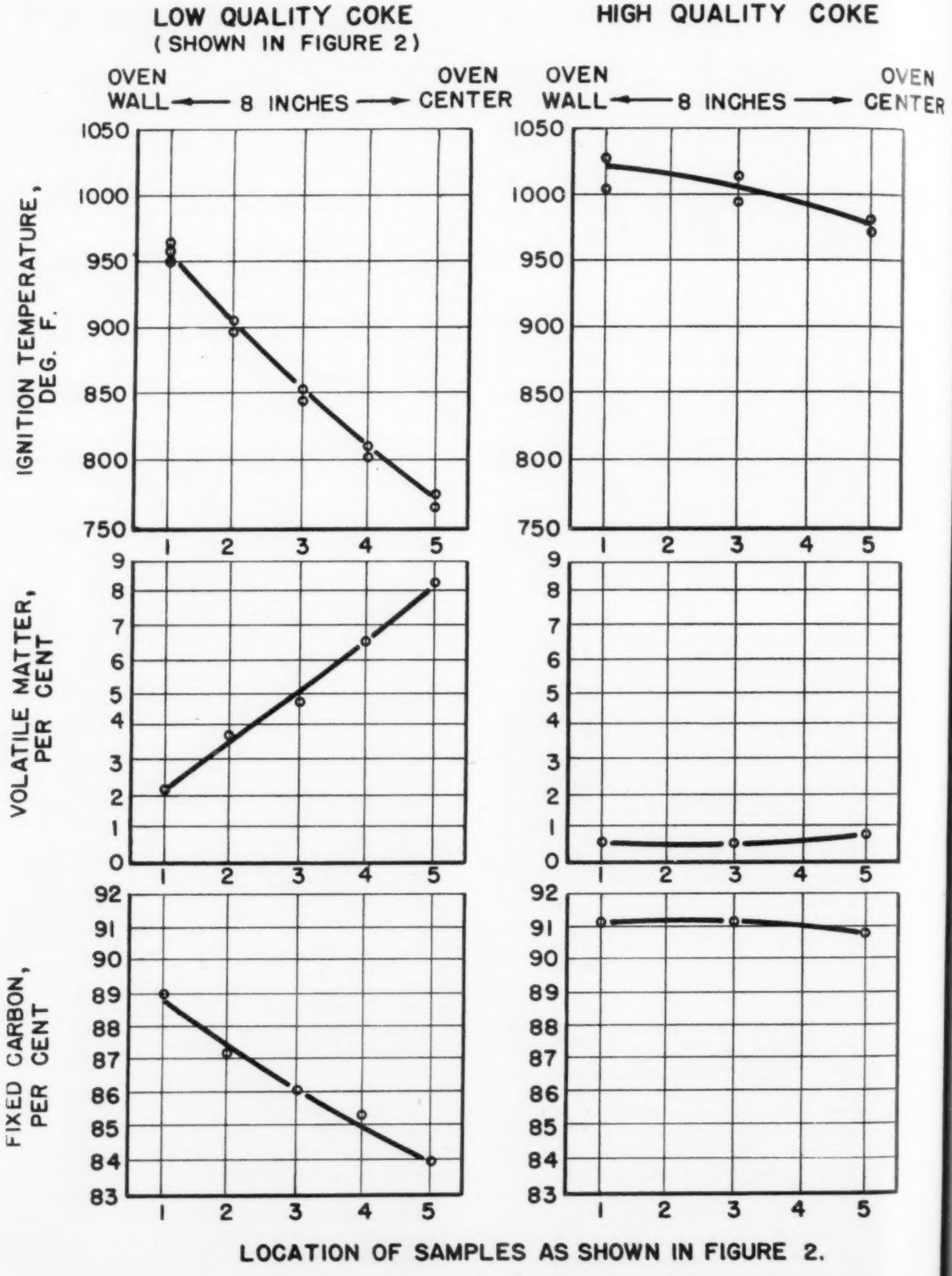


Fig. 3—Characteristics of selected pieces of by-product coke shown graphically.

and probably others have been used for the chemical treatment of coke. The usual compounds are the oxides or hydroxides, carbonates, sulphates, chromates, dichromates, and chlorides. None of the treatments appear particularly promising for improving the operation of cupola coke.

Chemical additions have been made both to the blended coal before coking and to the finished coke by dipping or spraying. There are objections to both of these methods of treatment.

Chemical treatment has also been used to decrease the ash content of coke. Reductions in ash content from 17 to 7 per cent and from 6.45 to 0.70 per cent have been reported in some investigations.

Additions of lime and other fluxing agents to coal have been used to produce self-fluxing cokes which are said to cause less refractory loss in the cupola.

J. Production

As this review is being made from the consumer's point of view, only a few papers were selected on the production of coke. The selected papers are included to indicate some of the methods that are being used in the production of coke to improve coke quality. Coal blending and coking practice are sometimes used as a basis of coke specification. Blending and coking practice undoubtedly have a major influence upon the performance of coke in a cupola, and continued efforts along these lines are definitely desirable. However, producers are handicapped in such work because of the necessity of using the coals which are economically available in a particular area. At the present time, producers rely heavily upon contacts with the consumers of their coke and attempt to work out correlations between some type of coke-plant control test and the results obtained in cupolas using their particular coke.

The dry quenching of coke with gases instead of wet quenching with water has received some attention, but has not been generally adopted. Coke quality may be appreciably improved in some cases by washing the coal before coking.

Most producers are regularly making tests to attempt to keep their products under control. However, such individual efforts aimed at uniformity of product from a single plant do not assure the uniformity of coke quality in the foundry industry as a whole.

III. Performance vs. Laboratory Tests

Several examples of tests on foundry cokes are given below to illustrate some of the difficulties encountered when present methods of testing are used to correlate coke properties with performance in the cupola.

A. Plant Survey

Two cupola heats were run at each of the 18 member foundries of the Gray Iron Research Institute. These heats were run on 12 different cokes or combinations of cokes. Cupola operating data and heat results were recorded as completely and as accurately as was possible in good commercial operation. The cokes were tested by determining the proximate analysis by ASTM methods, determining combustibility curves by a modification of the Boegehold combustibility test and, in some cases, by measuring other special properties.

Although certain trends were evident, the correlation between cupola performance and coke properties was generally unsatisfactory. That is, the correlations were unsatisfactory because they provided no dependable basis on which coke performance could be *predicted*. The lack of correlation might also be due to inadequate control over some of the variables or to the effect of variables which were not evaluated.

B. Selected Cokes

Over an extended period of operation, each of three different foundries was able to set aside two lots of coke —one lot of which gave good cupola performance and one lot which yielded poor performance. The performance ratings were based upon general cupola operation and were made by the foundry personnel. The product of these foundries varied from piston rings in one to heavy machinery parts in another.

Laboratory tests for combustibility, proximate analysis, and true and apparent specific gravity, upon these three pairs of cokes, revealed no consistent differences between the *good* and *poor* cokes. None of these tests consistently *predicted* whether a coke should be rated as good or poor.

In evaluating tests made at different foundries, with different types of cupola operation or producing different products, considerable attention must be given to the fact that a coke that is considered good by one foundry may be considered poor in another type of operation. Such a divergence of opinion might mean either that the operation of one group of cupolas has not been adjusted to burn this particular coke properly or that the coke actually is not suitable to one of these types of operation. The determination of the effect of coke properties upon cupola operation must be made in such a manner that performance under *various accurately controlled* conditions of cupola operation can be predicted.

C. Dangers of Inadequate Sampling

During the plant survey mentioned above, cokes were sampled and tested according to ASTM recommended methods. With several cokes, however, a determination was made of the uniformity of coke properties from the coke-oven wall to the center of the oven. For one incompletely carbonized piece of coke (shown in Fig. 2), volatile-matter content increased from about 2.1 per cent at the oven wall to about 8.3 per cent at the oven center, as shown in Fig. 3. Ignition temperature in a combustion test varied from 960 F at the oven wall to 780 F at the oven center, and fixed carbon content varied from about 89 per cent at the oven wall to about 84 per cent at the center of the oven.

These variations within this one lump of coke were greater than the variations encountered between all the different cokes previously sampled by ASTM methods. This piece of coke is admittedly of very poor quality, but the fact remains that it was selected from a routine shipment of coke made to a commercial foundry.

In contrast to this piece of incompletely carbonized coke, Fig. 3 also shows the properties obtained across a section of high-quality coke that was sampled in the same manner. The properties shown in Fig. 3, therefore, illustrate the extreme conditions obtainable in a

distance of about eight inches from oven wall to oven center, and emphasizes the sampling difficulties attendant upon coke testing.

Because of the magnitude of the variation in coke properties that may be encountered, accurate sampling is extremely important. Inadequate sampling practice can well account for some of the poor correlation between coke test data and cupola operations. When attempting to make such correlations, it is extremely important that (1) the true nature of the coke is quantitatively known, and (2) that the cupola operation is carefully controlled and quantitatively recorded.

Even assuming that accurate samples can be obtained, the values of data obtained on an average sample of widely differing components is likely to be misleading. The ignition temperature of a piece of coke depends more upon the maximum volatile-matter content in any part of that piece rather than upon the average volatile-matter content Mere determination of an average value yields little useful information unless limits are also established. It is easy to imagine two cupola operations, one with tapping temperatures of 2680 to 2720 F and the other with temperatures from 2500 to 2850 F. Both might show an *average* of 2700 F, but there is little doubt that the first would represent better operation.

These difficulties involved in obtaining representative samples emphasize the unreliability of laboratory testing using small samples when the data are to be applied to commercial operations.

IV. Acknowledgment

The summary of the literature, bibliography, and all test data upon which this paper is based were selected from data developed by the Gray Iron Research Institute, Inc., as a part of its research program on foundry coke.

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D 141-23 "Drop Shatter Test for Coke"
D 294-29 "Tumbler Test for Coke"
D 293-29 "Test for Sieve Analysis for Coke"
D 292-29 "Test for Cubic Foot Weight of Coke"
D 547-41 "Test for Index of Dustiness of Coal and Coke"
E 11 -39 "Sieves for Testing Purposes"
D 121-30 "Terms Relating to Coal and Coke"
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DISCUSSION

Chairman: B. P. MULCAHY, Consultant, Indianapolis

Co-Chairman: H. W. STUART, U. S. Pipe & Foundry Co., Burlington, N. J.

L. H. RUDESILL[1] *(written discussion):* Mr. Krause and Mr. Lownie's analysis of the available data on foundry cokes can be a valuable contribution to a new and different program toward uncovering some of the mystery in connection with the production of a suitable cupola coke.

It is not my intention to repeat what has already been said in their review, as they have thoroughly analyzed the laborious effort of many workers and writers to develop some test that could be correlated to actual operating conditions.

[1] Griffin Wheel Co., Chicago

The writers point out that it has been impossible to develop any specification because none of the physical or chemical tests available could be releated to cupola performance or operating control. The reason for this is the normal existence of innumerable variables within the cupola and in the coke itself.

The actual function of the cupola melting process under normal conditions involves a continuous series of intermittent variables. The coke size, the melting rate, the fluxing rate, the section size, the density of the charge and the flowability of the charge produce melting fluctuations and constant variables during the melting process. Because of these conditions, it seems impossible to measure to any relative degree the coke qualities by making pilot or test heats in a test or experimental cupola.

On page 343 the writers point out that each producer has his own method for plant control of coke quality. Such individual control tests are often based upon the personal observation of the foundry engineer or sales engineer employed by the coke producer. I do not feel that much progress can be made toward developing any specifications for coke quality until the initiative is taken by the coke producers. There seems to be a lack of coordination or a lack of willingness on the part of the producers to cooperate with consumers to solve the mysteries that are connected with most complaints. Most complaints are passed over with the assertion that the coke was poorly picked, the bad labor situation or the inability to obtain suitable coal. These are all legitimate explanations but they are only assumptions and do not develop the real analysis of the inferior material.

The inability to develop relative tests of foundry cokes might be attributed to the fact that the consumer seldom has any knowledge of the original mixture or the coking procedure or coking time. It is not unlikely that in most cases the oven superintendent can predict the quality of the coke before it is shipped. This is not unusual as the same applies to the cast iron and steel industry. Without the original mixture and melting data it is sometimes difficult to determine the cause of defective castings.

I do not wish to cast any reflections on the coke manufacturers as I know a great many complaints are not justified. I do feel, however, that a great deal could be accomplished by the general dissemination of information in connection with quality of coal, coking times and manufacturing methods.

It is probable that a joint committee composed of representatives of both the producers and consumers would be an important step in the right direction. Such a committee would be capable of sifting the data already available and would be better able to outline more practical tests.

The writers have made an excellent contribution toward just such a program and their paper can be used as the nucleus for unlimited expansion of practical and technical knowledge on this subject.

W. W. LEVI[2] *(written discussion):* Foundrymen and cupola operators have for a long time recognized the need for the development of tests which will enable them to properly evaluate cupola cokes and at the same time enable them to predict with a reasonable degree of certainty just what to expect when various grades are used for cupola melting. It is gratifying to know that such able men as the authors of this paper have undertaken a research on the subject in which there is such general interest and where the need was never greater than at the present time.

In the foundries with which the writer is connected, we are interested in carbon control and particularly in the amount of carbon pick-up by an iron charge during the process of melting in the cupola. (There are, of course, some instances where it appears that carbon is lost from the charge during melting.) Inasmuch as the carbon absorbed comes from the coke within the cupola, our interest centers upon the characteristics of the coke which influence the amount of carbon picked up.

We produce various grades of iron as follows:

Application	Total Carbon, Per Cent
High Strength	2.85–3.05
Automotive	3.25–3.45
Permanent Mold	3.50–3.70 (3.60 desired min.)
Special Castings	3.60–3.90

[2] Lynchburg Foundry Co., Radford, Va.

To produce the various grades of irons just mentioned, we have over a period of years used by-product cokes, pitch coke "A," and beehive coke "A." These three cokes used either singly or in combinations have been very useful in maintaining carbon control and definitely influence carbon pick-up. Due to existing shortages, we have in the past few weeks resorted to the use of two additional cokes which will be designated as pitch coke "B" and beehive coke "B" respectively. As pointed out by the authors, there seem to be no tests available at the present time which will enable foundrymen to predict the behavior of coke in commercial operation. In this connection, we have made very few tests on any of the cokes we use, but have arbitrarily selected ash content as a basis of comparison. Results obtained on the cokes referred to above are as follows:

Coke Type	Ash Content, Per Cent
Pitch Coke "A"	0.5
Pitch Coke "B"	0.7
Beehive Coke "A"	4
Beehive Coke "B"	10
By-Product Coke	10–12

In our shops, we have made many tests using cupolas lined to 72 in., equipped with front slag spouts. These cupolas are of the balanced-blast type. The air blast was dried to a uniform moisture content of four grain per cu. ft. and was measured by means of an air-weight control blower. These conditions as well as the weighing of the various components of the cupola charge were controlled as rigidly as is possible in an operation of this size. Under the conditions just described, our experience indicates that pitch coke "A" produces the hottest iron at the cupola spout and also results in the greatest carbon pick-up. The by-product cokes melt irons almost as hot as pitch coke "A," but result in a considerably lower carbon pick-up. Beehive coke "A" does not melt as hot as either pitch coke "A" or the by-product cokes, but the carbon pick-up is intermediate. The behavior of pitch coke "B" is very similar to that of beehive coke "A." Beehive coke "B" produces irons of the lowest temperature with the lowest carbon pick-up. The foregoing data are tabulated as follows:

Type Coke	Relative Iron Temperatures	Carbon Pick-Up	Ash, Per Cent
Pitch "A"	Highest	Highest	0.5
Pitch "B"	3rd Highest	2nd Highest	0.7
Beehive "A"	3rd Highest	2nd Highest	4
Beehive "B"	Lowest	Lowest	10
By-Product	2nd Highest	3rd Highest	10–12

For the production of high strength irons, we use by-product coke exclusively in order to obtain a minimum carbon pick-up. For automotive irons, we can and do use by-product coke alone, but prefer a combination of about 75 per cent by-product and 25 per cent pitch coke "A." For permanent mold irons, we find beehive coke "A" alone to be satisfactory when the cupola charge contains 35 to 60 per cent of pig iron and by-product coke alone is satisfactory when the charge contains over 60 per cent pig iron. When the charge contains from 15 to 35 per cent pig iron a combination of 25 to 35 per cent of pitch coke "A" and the balance behive coke "A" is desired. We have further found that a combination of 50 per cent beehive coke "A" and 50 per cent beehive coke "B" behaves just about the same as 100 per cent beehive coke "A," when melting a mixture for permanent mold iron containing 40 per cent pig iron, while a charge consisting of 100 per cent beehive coke "B" was entirely unsatisfactory. On a few occasions, we have used a combination of three different types of coke, that is, 50 per cent beehive coke "B" and 25 per cent each of pitch coke "B" and beehive coke "A." This gave very satisfactory results. There are, of course, many other possible coke combinations that might have been used. However, the significance of these tests lies in the fact that a combination of cokes is in some instances very effective in bringing about the desired results for a particular application in spite of the fact that at least a portion of the coke charge consists of a type which if used by itself would be very unsatisfactory.

A yardstick for measuring the quality and characteristics of by-product coke which we have used for some time in our foundries is a formula by which we can accurately calculate the percentage of total carbon in the iron at the cupola spout when the percentage of carbon in the charge is known. If the

carbon pick-up is very far off from the calculation, we generally find ourselves in trouble from the standpoint of metal temperature, and physical properties may be adversely affected as well.

Practically all of the beehive cokes that we have used consisted of pieces weighing from three to four ounces up to three pounds or more; and a good deal of the by-product coke contains pieces weighing from a few ounces up to four pounds or more each. In spite of this, results obtained have been very satisfactory, though as pointed out by the authors, a condition which we call "good" might be considered pretty bad in another foundry. Likewise, the strength of the beehive coke is far lower than that of either the pitch or by-product cokes. Nevertheless, we have obtained satisfactory results at least with beehive coke "A."

The writer would very much like to know something of the practices and auxiliary equipment used in operating the 10-in. diameter cupola to which the authors have made reference. Do they feel that such a cupola could be used as a practical laboratory tool for predicting the performance of foundry cokes, and could the cost of such installation be justified?

CHAIRMAN MULCAHY *(written discussion):* The objective of the authors to establish or define a procedure for predicting the behavior of foundry coke for cupola operation is both admirable and important. Admirable because of the diversity of existing opinions on the subject and a clarification certainly is in order and important because coke is more than just a fuel for the process, influencing as it does, the chemistry of the iron.

The review of the literature by the authors is very comprehensive and I believe of sufficient extent to outline the present status of the subject.

As indicated by the authors there are many measurable characteristics of foundry coke—they point out also that while certain of the tests are satisfactory in their present mode of application others are quite unsatisfactory. The authors have also pointed out that one of the major difficulties has been the lack of correlation between certain properties of the coke and its resultant behavior. The nub of the problem, of course, centers in this situation.

The authors recognize the necessity for obtaining the final answer from commercial cupolas. This is essential to the solution of the problem. The initial studies in the "baby" cupola should yield some worth while information pointing out the direction for their studies on actual operations.

The subject is broad and complex and of extreme interest to all cupola operators. This initial report should certainly elicit considerable discussion, and the accumulated experience of the industry if directed along the lines suggested should be very helpful to this research program.

In their conclusions the authors state that "no suitable tests or combination of them have been made available to foundrymen for predicting the behavior of foundry coke in any particular type of cupola operation." I feel that this statement is too broad in its implications and must disagree with this conclusion. The listing of the tests available certainly indicates that the subject has been given considerable thought and the lack of correlation lies with the cupola operator himself who has not used these tests to identify the features which affect his operation.

It must be kept in mind, which I am sure further work will reveal, that good coke can be obscured by poor cupola operation, and that under such conditions predictions of coke behavior are impossible, and further that good coke cannot be a guarantee of good cupola results. This fact must not be lost sight of in the assignment of responsibility.

To one who has devoted considerable study to this problem from both the production and consumption standpoints, it is quite evident that there is no one single property of coke which can be used to measure or indicate its behavior even when used under ideal conditions. This means that the problem resolves itself into determining what group of measurable characteristics of coke can be employed for this purpose.

The authors have done a real service in reviewing this subject in the manner they have, and certainly success in their endeavor will be of lasting benefit to all cupola operators.

G. C. CREUSERE[3] *(written discussion):* Messrs. Krause and Lownie have done some straight thinking and clear writing on the subject under discussion.

We of the by-product coke industry have for a great many years experimented through laboratory tests as well as field tests in an effort to find a method or formula by which the quality of foundry coke could be controlled and its performance predicted in commercial cupola operation. Thus far we must confess that our efforts have not been rewarded with worth-while success. This is chiefly due to the following conditions:

There is a wide diversity of type of cupola operation and, as a consequence, a foundry coke which is ideal in performance in one plant will not necessarily give the same results in another. This is undoubtedly due to the fact that there are many variables in cupola operation, of which coke is only one. Thus far it has been impossible to eliminate all other variables.

Furthermore, quality factors are not only important in themselves but in their inter-relation. The significance of this inter-relation has been difficult to measure because of different operating methods and other problems in the foundry.

Finally, our industry has been severely handicapped in recent years by not being able to get the kinds of coal we want in the quantities needed for adequate foundry coke production.

It is our confirmed opinion that the identification of quality characteristics and their inter-relation is a prerequisite to the adoption of useful standards for commercial cupola operation. We of the Semet-Solvay Company will resume with added vigor our efforts toward the identification of these characteristics and the adoption of standards by which performance can be predicted just as soon as the proper coals are available. Until more progress is made in this direction, however, we feel that our present policy of working closely with our customers as individual operating units and maintaining the most rigid possible controls in the manufacture of our products is the greatest contribution that can be made by the coke manufacturer to the gray iron foundry industry.

DR. J. T. MACKENZIE[4]: The authors have rendered a real service in having made this survey but there are some of their conclusions that I differ with. One is the matter of coke size. I will agree that uniformity of coke size from car to car is desirable but I would not agree that all one size is the best cupola fuel. We went to some trouble to get some carefully sized 4 x 4 coke one time and found that the "2-in. and larger" coke worked better.

One of the troubles with the test evaluation that the authors have made is that they are working in such a small part of the test field. If you consider the whole range from zero shatter test to 100 shatter test, I am sure you would find that 40 shatter test would be very inferior cupola fuel, whereas other variables might outweigh the difference between 70 and 80 shatter test. The same thing is true of volatility. Nobody would specify 5 per cent volatile coke if he was not interested in getting all the carbon absorption he could. The same is true of ash. You know that 20 per cent ash is undesirable in coke but as between a 6 and 8 per cent, the ash may not be the preponderant factor at all.

We have made quite a few tests on the baby cupola and find that the results are not even applicable to a 48-in. cupola. Two cokes may actually show in reverse order. We had two cars of special beehive coke, the nicest coke we ever saw in the 18-in. cupola but it would not carry the burden in a 72-in. cupola at all. In fact, we could not blow an 11-oz. pressure and keep the slag from foaming.

As to the number of tests in Great Britain and Germany, I do not agree with the authors' conclusion on that. The reason they made so many tests is because they were troubled with coke problems all these years, whereas we have only lately begun to have trouble.

MR. LOWNIE: I think that we are essentially in agreement with Dr. MacKenzie on most of his points. Some of the minor differences are merely matter of differences in definition. "Uniformity of coke size," for example, does not necessarily mean coke sized to 1¼ x 1¾ in. as we used in the small cupola, or even 4 x 5 in. for use in a larger cupola. For commercial use, a coke may vary considerably more than this and still be considered relatively uniform in size. We certainly agree with Dr. MacKenzie that we have covered only a very narrow range of operation. That is simply because we have decided to start from scratch on this problem and feel our way along rather cautiously. This paper is mainly a summary of our interpretation

[3] Semet-Solvay Co., Detroit

[4] American Cast Iron Pipe Co., Birmingham

of the published work that has been done to date on this problem. Our experiences with the 10-in. cupola have indicated an excellent correlation with actual plant practice. This 10-in. cupola which was designed by and operated for the Gray Iron Research Institute is not expected to give results which are necessarily directly comparable to those obtained in a 48- or 72-in. cupola, but is expected to provide a tool for investigating foundry coke on a pilot plant basis and to furnish results which might serve as a *basis* for prediction of results in larger cupolas. This present paper is not a description of the operations of the 10-in. cupola, but it should be mentioned at this time that our statements about this cupola are not purely hopes and conjectures. In a number of tests, this small cupola has shown a surprising ability to reproduce effects obtained in commercial cupolas.

In the matter of the number of published test results originating in England, we agree that they have probably been forced into greater activity because of the inferiority of their coals when compared to American coals, but for that very reason they have been contending for years with a problem that has just recently became of great concern in this country.

C. C. KAWIN[5]: My mind goes back 45 years, before we had by-products. In all Griffin plants they had cokes that ran as high as 16 to 18 per cent ash. It has a good deal to do with human element connected with the operation of the cupola itself. Recently I was in a foundry near Chicago. One of our clients, who had just had a disaster, was getting some coke in. He pointed to a pile of very small coke and reported satisfactory performance with it.

Our old specifications at Griffin were around 8 to 10 per cent ash, and sulphur as low as possible. I think the Doctor's idea of not too large coke mixed with some of the smaller coke is about as good as you can do today or ever do in the coke business when you are making it from coal. Our experience has been to just use a little bit more if there is more ash, and after a day or two of observation you will find it works out all right. I would like to see some of them do that today.

CHAIRMAN MULCAHY: We have to remember that in taking samples of coke from a commercial plant for assay in a baby cupola, they must be artificially sized and in so doing the identity which the coke had in its original form in the plant is destroyed. I think that the results in the baby cupola really serve as a measure of the combustion properties of coke rather than to say that it can assay the total qualities of the coke.

On the question of uniformity of size, I think none of us are too far apart in our reaction and interpretation of this factor. It is largely a question of definition. You spoke of uniformity of size and I feel that the paper should more fully define what is meant in the conclusions. For example, you can have extremely uniform size coke, between 2 and 2¼ in. and also between 5½ and 6 in. The principal factor here is the fact that while the volume of voids remains practically the same between the two cokes, the coke surface, however, is entirely different. It has been multiplied better than twice in the case of the small coke, and this enters seriously into the question of penetration of blast, distribution of gases and rate of combustion. Mr. Lownie mentioned that they have not found any test or series of tests which permit identification or predictability of coke. There are quite a few factors of coke that we are actually interested in, but not nearly so many as have been shown on the chart. Many of those, I think, can definitely be shown to have no responsibility, with respect to the average properties or combustibility of the use of coke. I feel frankly that some of these conventional tests, when modified, and I say modified in the case of foundry coke, can be of extreme value in predicting and determining when the tests are over, those properties influencing operations. I emphasized the need of modification of some tests because many of the original tests were designed for blast furnace coke, which is entirely different from foundry coke. The shatter test as designed cannot possibly be used in connection with foundry coke, and the same is true with respect to the tumbler test.

MR. LEVI: I have a few comments to make on tests which we have run in cupolas lined to 72 in., operating under conditions of regular production.

Our cupolas are of the front slagging type, equipped with balanced blast tuyeres, and the cupola blast is maintained at a uniform moisture content of four (4) grains per cu. ft. One of the things which we found and which is not particularly new is that the coke (we have used five varieties in the last few weeks) which melted at the highest temperature gave us the greatest carbon pickup and the coke which melted at the lowest temperature gave us the lowest carbon pickup. That, of course, is what we would expect.

We have also run a series of combinations of one or more kinds of coke. We have concluded that the significance of these tests lies in the fact that a combination of cokes is in some instances very effective in bringing about the desired results for a particular application in spite of the fact that at least a portion of the coke charge consists of a type, which if used by itself, would be very unsatisfactory. There is one other point which has been brought out by the authors. What we call satisfactory in our shops might not be satisfactory in another shop.

We have for the past few years been using a formula, or equation, for calculating the percentage of carbon (T.C.) in the iron at the cupola spout when the carbon in the ingoing charge is known. If the nature of the coke is such that the percentage of carbon in the iron at the cupola spout is reasonably close to the figure indicated by our calculation, we feel that the quality of the coke is satisfactory. I believe that this formula is a pretty good yardstick for evaluating the characteristics of the coke.

J. S. VANICK[6]: The point Mr. Levi brought up on control of carbon in the finished iron is probably the one that bothers foundrymen most. This matter of coke quality was one of the uppermost topics in the minds of foundrymen. The problem of controlling coke quality is firmly bound to the problem of controlling the quality of the product that is coming out of the cupola spout. Foundrymen will go along and control very carefully the blast, the metal charge, and details such as coke size and then hold their breath and wait and see what comes out of the spout, depending entirely on this undefined variable, coke.

Mr. Lownie and his associates have done a remarkably helpful job in collecting the history of the art up to date and there is no doubt that a great deal more will have to be done. For example, if the chart showing the variation in volatile matter and in fixed carbon from the surface to the core of a coke oven block is to vary as much as they show, which was something of the order of 2 to 8 per cent for volatile matter and 84 to 89 per cent fixed carbon, and if you cannot control that better in 16 in. of oven, then, perhaps you ought to cut the oven walls down to 6 in. apart and come somewhere between 87 and 89 for the value of fixed carbon. Alternately perhaps the coking time and the other factors in the operation of the ovens might be adjusted to get a higher order of uniformity in the product.

There is some question in my mind as to the statistics which the authors have quoted. This matter of 3 per cent of the coke output being foundry coke is true. It amounts to perhaps 2¼ million tons annually. But it is a premium product, it is a product that carries a higher price tag on it than any of the other cokes made. And if that premium is to be earned there should be correspondingly close attention paid to the handling and processing of coke to make it useful for foundry operations.

On the matter of beehive products vs. by-products, if you study the statistics you find that beehive is dying off rapidly and perhaps as by-product coke supplies increase it will disappear entirely.

The matter of uniformity in quality brings another case to mind. When a foundryman tries to make a closely controlled iron at the spout, he wants reproduceability. The matter of defining coke quality seems to be an obstacle in his path. If we do not have a better definition for coke at this time, we had better return to the 1916 A.S.T.M. coke specification and start from there to bring about some order in the coke industry.

In connection with the cupola tests on 12 different cokes, I wonder whether the authors had any opportunity to check the four important variables mentioned as being dominant ones in coke control? That is uniformity of size, strength and hardness, reactivity, and ash and sulphur content.

MR. LOWNIE: The cokes that were tested in the plant survey of operations of twelve foundries associated with the Gray Iron

[5] Chas. C. Kawin Co., Chicago

[6] International Nickel Co., New York

Research Institute were tested in our laboratories for proximate analysis (moisture, fixed carbon, volatile matter content, and ash); combustion characteristics as measured in a modification of the Boegehold Combustibility Test (ignition temperature, maximum temperature, temperature rise, and combustibility or rate of burning); and for impact strength. We also had available on each lot of coke the supplier's proximate analysis, sulphur analysis, size classification, and shatter test results. Because of the breakage in handling, however, the supplier's size classification often had to be modified. Unfortunately, these plant survey tests were run about five years ago when we were just getting started on the question of coke quality as a factor in cupola operation. After we have progressed a little further in our present more basic investigations, plant survey tests of this type will probably again be attempted and we hope that at that time we will have a better idea of what to look for. As it is, we have a lot of information on coke tests in the small cupola and in actual plant operation, which we have not included in this paper because we regard many of the results and data as inconclusive. We certainly hope that at some later date we will be able to publish these results with an accurate interpretation of them. That is the goal at which we are now aiming.

The Gray Iron Research Institute and Battelle Memorial Institute are eager to do everything possible to fill the voids in our knowledge of foundry coke. For that reason, they have agreed to make these data available to the Cupola Research Committee of the A.F.A., which is currently planning to undertake an investigation into this problem.

S. WEISS[7]: This paper has been most interesting. This subject of foundry coke quality is of as vital interest to the foundry coke producer as it is to the foundryman.

We organized last year a Coal and Coke Research Committee. The statement that 3 per cent of the coke production being foundry coke is rather misleading. Of the 67 million tons of coke produced in 1944, you will find that roughly 47 or 48 million tons of that was steel plant coke. In other words, it was coke produced by people who had never made foundry coke and have no interest in it. Some of the approximately 20 million tons of other coke was produced by companies who make pig iron but who also produce foundry coke. So if you calculate 2¼ or 2½ million tons of foundry coke out of 20 million, you will find that the producers of foundry coke have a greater interest than the 3 per cent figure would indicate. I give you these figures to show that foundry coke production is a much more important business to the merchant coke plant than Mr. Lownie's tabulation would seem to indicate. Even that 10 per cent average figure is somewhat misleading, because there are among the merchant plants producing foundry coke some whose foundry coke production is even a greater percentage of their total production than 10 per cent. I dare say in some cases even 30 or 40 per cent of their entire production is foundry coke. So you can see they are vitally interested in this business.

What we used to specify as high grade, good coking coals 25 years ago are fast disappearing. We have to work with different materials. This coking coal situation, due to the war, has become terribly distorted. The terrific demand for coal, the shortage of manpower, made necessary a substantial increase in the amount of mining machinery used. When you mine coal by machine instead of by hand, you just do not do the selective mining that you can do by hand. Miners' wages have gone up. The economics of the coal business does enforce the use of machine-mined coal. And because of the war and the fact that the metal was needed for war purposes, the coal cleaning plants which have to go along with machine mining are not yet built. We are going to get better coal but it is going to take a little time. These cleaning plants that have to be built to prepare machine-mined coal are not in existence to the extent they will be. When they are built you will find that we will get better coal, which will be reflected in the resultant coke.

MESSRS. KRAUSE and LOWNIE (authors' closure): The authors wish to thank Messrs. Rudesill, Levi, Mulcahy, Creusere, MacKenzie, Kawin, Vanick and Weiss for their thoughtful discussions of this paper. The discussions certainly bear out the main contention of the paper that there is considerable need at this time for organized thought and action on this problem of foundry coke. The Cupola Research Committee of the A.F.A. has begun a survey on the problem of foundry coke since this paper was written and the test results obtained at Battelle and in the plants of the Gray Iron Research Institute, have been given to this Committee.

The 10-inch cupola is regarded as a research tool which is capable of reproducing certain effects which are obtained in larger commercial cupolas. This is not merely a statement of what we hope to obtain from this cupola, but is based upon *results* which have been obtained in it. However, as with most research tools, there are certain features of the tool that do not coincide with commercial practice. In our case, the main limitation is the size factor. This factor has to be accepted under our circumstances in order to permit control of a number of other variables, that are extremely difficult to control in larger units. The 10-inch cupola is admitted to have certain limitations. A major part of the interpretation of data obtained with this cupola consists of recognition of these limitations. Similar limitations are true of most tests made on the cast iron which is produced in any foundry. The tensile test, on separately cast bars, is recognized as a generally useful tool and is widely used in spite of the fact that it tells relatively little about the strength of castings poured from the same iron. The cooling rate limitations in this case are just as real as the size limitations with the small cupola, but recognition of the limitations permits the test to be used with some success. The shatter-test for coke, chill test for cast iron, and weather predictions made from meteorological data, all have their limitations, but they happen to be the best tools that we have at the present time for each specific purpose. The small cupola is regarded in the same light. When a better tool for the purpose is suggested, and proves itself, no hesitancy will be found in adopting the new tool in preference to the small cupola.

In conclusion, we would like to discourage the tendency to group all cupolas under about 20-inch diameter, into a single class of "baby" cupolas and to judge the operation of any cupola in this class by the success or failure of any other specific unit. Small cupolas are apparently more sensitive to operating variables than are larger units. On the other hand, these same variables are easier to control. Failure of one specific unit, under one set of conditions, to produce the desired results should not be used to condemn the whole class. Likewise, the success of a specific unit should not be used to construe that any other unit of merely the same size will operate with the same success, unless similar construction and rigid attention to details are employed.

[7] American Coal and Coke Chemicals Institute, Washington, D. C.

EFFECT OF ROOM TEMPERATURE INTERVALS BETWEEN QUENCHING AND AGING OF ALUMINUM SAND CASTING ALLOYS

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Barber, N.J.

ABSTRACT

The standard solution and precipitation heat treatment of aluminum alloy castings is generally done with soaking time, temperature and quenching rate under close control. However, the period between quenching and artificial aging may vary considerably, being determined by practical but nonmetallurgical considerations. For the aluminum sand casting alloys studied, it is shown that, in general, alloys which depend for strength on the precipitation of $CuAl_2$ are unaffected under the conditions of the study. Casting alloys in which Mg_2Si is the principal hardening compound, however, are markedly affected by room temperature aging. For two alloys studied, the research described indicates that elongation is improved if castings are allowed to age one day at room temperature before artificial aging. Immediate artificial aging of these alloys develops high strength and hardness at the expense of elongation.

IN COMMERCIAL ALUMINUM HEAT TREATING PRACTICE to produce high strengths, castings are soaked at elevated temperatures (generally 940 F to 1000 F) for extended periods ranging from 4 to 20 hr depending upon the alloy and the castings being treated. This treatment brings about the solution of certain metallic constituents through the mechanism of diffusion. The castings are then quenched, usually in hot water, to retain the solid solution produced.

Since the dissolved material is much less soluble at room temperature than at elevated temperatures, an unstable condition results and most commercial alloys begin to "age." This aging is probably a precipitation of fine, dispersed particles of intermetallic compounds which under certain conditions and for certain alloys can be resolved under the microscope at a very early stage.[1,2] Continued precipitation affects many properties of the alloy, hardness and strength generally being markedly increased.

For most commercial alloys the original solution treated properties are quite transitory and large changes are apparent after only a few weeks at room temperature. To speed up the precipitation or aging process after quenching, castings are commonly reheated to temperatures of about 300 F for several hours. This "artificial aging" treatment produces in a short time approximately the properties that would require several weeks' aging at room temperature. Further, this treatment produces stable mechanical properties and no further changes occur during subsequent room temperature aging periods.

The data in Figs. 1 and 2 have been plotted on a semi-log scale to emphasize aging effects during the first few days after heat treatment.

In a successful heat treating operation, the pertinent factors discussed above are kept under close control since the effects of soaking time, temperature, and quenching rate are well known. One variable seldom subject to scrutiny is the period between the quenching operation and the artificial aging treatment. It is usually not deemed critical, being determined by practical but nonmetallurgical considerations such as furnace availability or labor shift changes.

Once a cycle has crystallized, production schedules usually require rigid adherence. In this author's experience, intervals between quenching and artificial aging varying from zero to three days have been observed. In some installations quenched castings are immediately transferred to an aging furnace while in others they are held for the night shift. With the advent of a weekend, several days often elapse before the aging treatment can be fitted into the cycle.

The effects of various room temperature intervals between quenching and artificial aging at 310 F on some mechanical properties of several commercial aluminum sand casting alloys have been determined, and it is the purpose of this paper to report the results. Wrought 4 per cent copper and duralumin type alloys have been investigated in this respect with marked effects reported.[3] More recently, specific interval recommendations were suggested for wrought 75S,[4] although it has been indicated that the effects are sufficiently minor in the light of commercial experience to warrant retraction of a rigid interval limitation.[4,5]

Experimental Procedure

Production of Tensile Specimens. The foundry practice utilized in producing sand cast tensile specimens has been described.[6] Briefly, ingots of each alloy were melted in a No. 45 clay graphite crucible in a coke fired, forced draft furnace. The heats were removed from the furnace and permitted to cool quietly to the desired pouring temperature of 1225 F. No metal flux-

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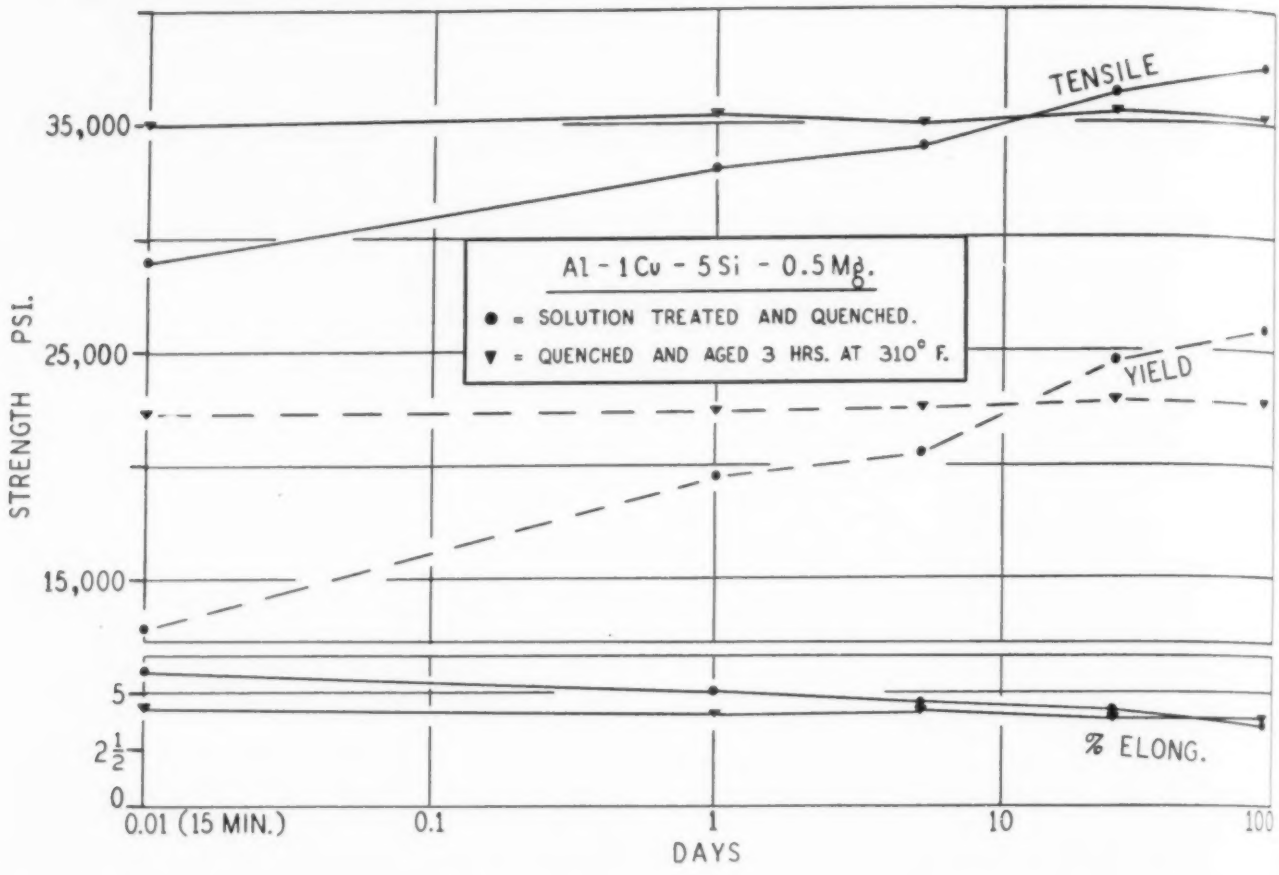


Fig. 1—Effect of room temperature aging of a heat treated aluminum alloy. Specimens treated at 310 F were first aged one day at room temperature.

ing procedure was utilized and no stirring or other deleterious agitation permitted. The alloys were cast into sand molds producing standard half-inch diameter tensile bars. These were used in the as cast shape, no machining or finishing of any kind being accorded the reduced tensile section.

Heat Treatment Procedures. Solution heat treatments were conducted in an air circulating electric furnace capable of being controlled to ±3 F. The castings were soaked for 16 hr and quenched in water preheated to at least 185 F. The quenched bars were then cooled to room temperature in tap water to make them available for immediate artificial aging where desired.

Artificial aging at 310 F was accomplished in electrically heated molten salt baths set to ±2 F. Generally at least two specimens were tested for each treatment.

Effect of Room Temperature Aging Intervals on Al, 7 Per Cent Si, 0.3 Per Cent Mg Alloy. Sand cast test bars were poured from each of three different heats of 7 per cent silicon, 0.3 per cent magnesium alloy. Their analyses are given in Table I.

The tensile bars were solution treated at 1000 F and quenched. Some were aged at 310 F immediately after being cooled to room temperature. In these instances, aging was initiated within 30 minutes of quenching. Another group of bars was similarly aged at 310 F 24 hours later, and a third group was artificially aged one week later. The effects of these room temperature intervals on the properties of the alloys are shown in the plots of Fig. 3.

Effect on Al, 1 Per Cent Cu, 5 Per Cent Si, 0.5 Per Cent Mg Alloy. Sand cast bars of the alloy described in Table II were solution treated at 980 F and quenched. They were aged at 310 F with various time intervals at

TABLE I. ANALYSES OF AL—7 PER CENT SI, 0.3 PER CENT MG ALLOYS

Element	*Alloy A Per Cent*	*Alloy B Per Cent*	*Alloy C Per Cent*
Copper	0.15	0.02	0.09
Silicon	6.87	6.04	6.88
Magnesium	0.26	0.22	0.24
Manganese	0.02	0.01	0.01
Iron	0.29	0.13	0.24
Nickel	0.00	0.00	0.00
Zinc	0.03	0.01	0.02
Chromium	0.04	0.00	0.00
Titanium	0.11	0.11	0.03
Aluminum	Bal.	Bal.	Bal.

TABLE II. ANALYSIS OF AL—1 PER CENT CU, 5 PER CENT, SI, 0.5 PER CENT MG ALLOY

Element	*Per Cent*
Copper	1.23
Silicon	5.21
Magnesium	0.46
Manganese	0.43
Iron	0.54
Nickel	0.02
Zinc	0.35
Chromium	0.10
Titanium	0.13
Aluminum	Bal.

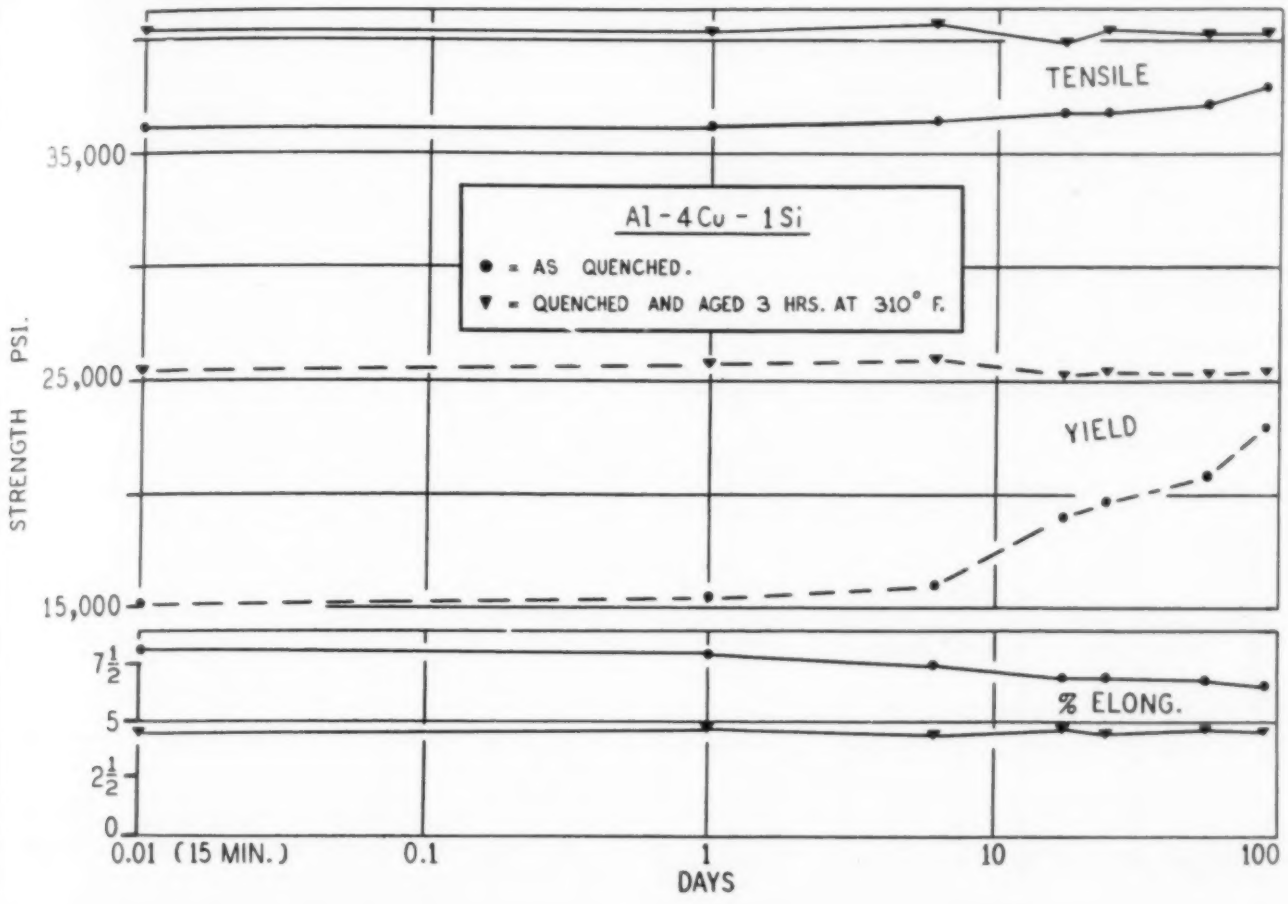


Fig. 2—Effect of room temperature aging of a heat treated aluminum alloy. Specimens treated at 310 F were first aged one day at room temperature.

room temperature being utilized as before. The effects of this interval may be noted from the plot in Fig. 4.

Effect on Al, 4 Per Cent Cu, 1 Per Cent Si Alloy. Sand cast bars of two 4 per cent copper alloys were solution treated at 960 F and quenched. The complete analyses are given in Table III. These were accorded the same treatment as the alloy previously described and the effects of room temperature precipitation between quenching and artificial aging may be determined from Fig. 5.

Discussion of Results

It is apparent that the amount of aging permitted at room temperature between the quenching and artificial aging treatments is of some significance. All aluminum casting alloys are not sensitive to this aging interval, one exception noted being the 4 per cent copper, 1 per cent silicon alloy. It would seem that commercial alloys in which $CuAl_2$ is the only precipitating compound are unaffected throughout the interval range studied.

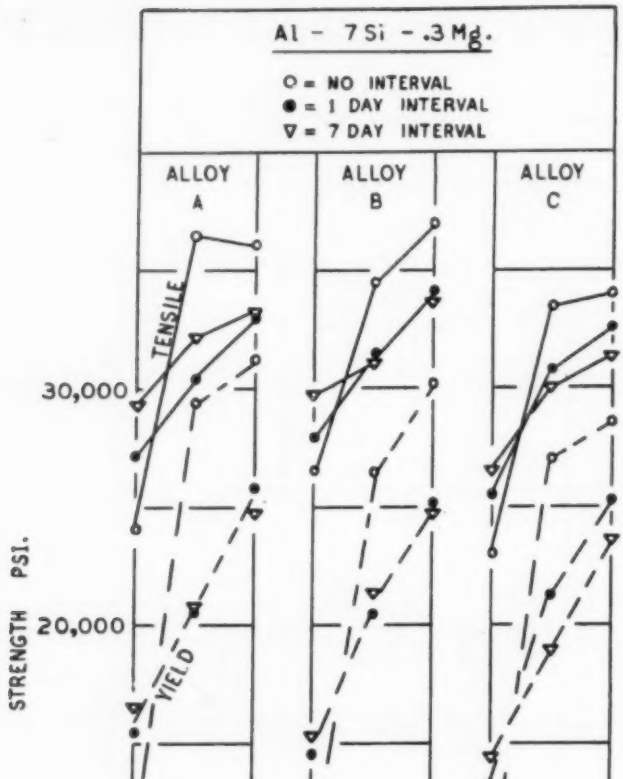
TABLE III. ANALYSES OF AL—4 PER CENT CU, 1 PER CENT SI ALLOYS

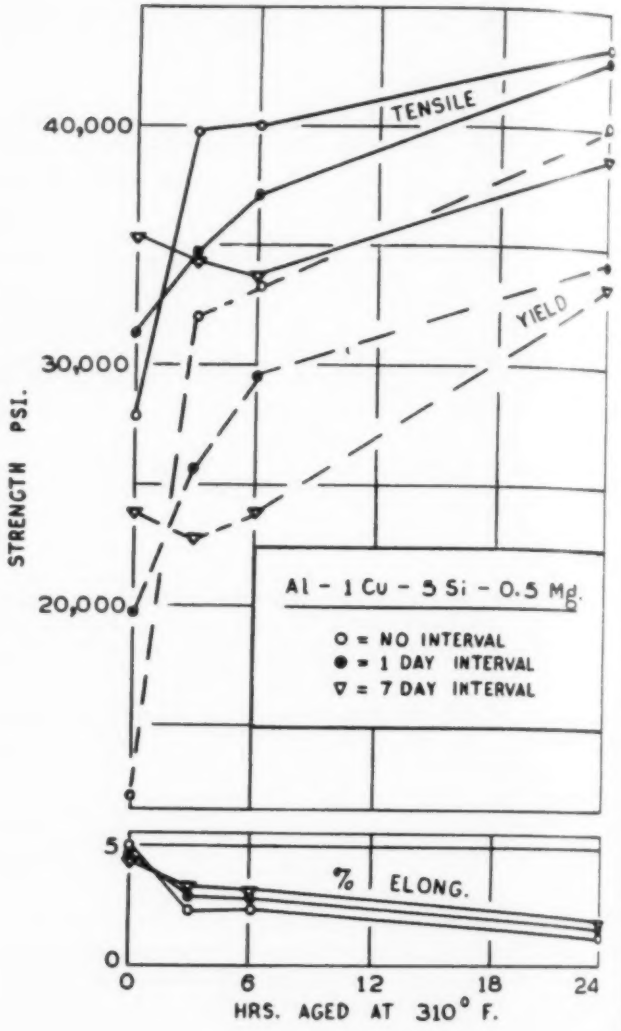
Element	*Alloy A Per Cent*	*Alloy B Per Cent*
Copper	4.34	4.10
Silicon	1.27	1.10
Magnesium	0.00	0.00
Manganese	0.23	0.02
Iron	0.51	0.45
Nickel	0.00	0.01
Zinc	0.08	0.09
Titanium	0.04	0.06
Aluminum	Bal.	Bal.

Cast alloys utilizing Mg_2Si as the principal hardening compound are markedly affected by a room temperature aging interval. If they are artificially aged immediately after quenching, high strengths and hardnesses result at the expense of ductility. It is difficult to exceed the commonly specified minimums for elongation under such conditions. If, however, castings are held about one day between quenching and aging a more desirable combination of properties is obtained.

The mechanism of the observed phenomenon is not completely lucid although considerable conjecture with regard to wrought aluminum alloys has been offered. In 1929, Rosenhain[7] suggested that hardening occurring at the lower temperature was partially nullified at the elevated temperature due to coalescence of the previously precipitated particles. The increased hardening that did proceed at the elevated temperature could be ascribed to precipitation brought on by the higher temperature and the algebraic sum of these two reactions was positive but not maximum.

In 1932, Merica[8] elaborated upon this explanation in accordance with his promulgated "knot" theory and suggested that the "knots" formed at room temperature were not favorably suited for further development at elevated temperatures and were consumed in the pro-





Figs. 3 (left), 4 (above) and 5 (adjoining page)—Effect of room temperature interval between quenching and artificial aging of aluminum sand cast alloys.

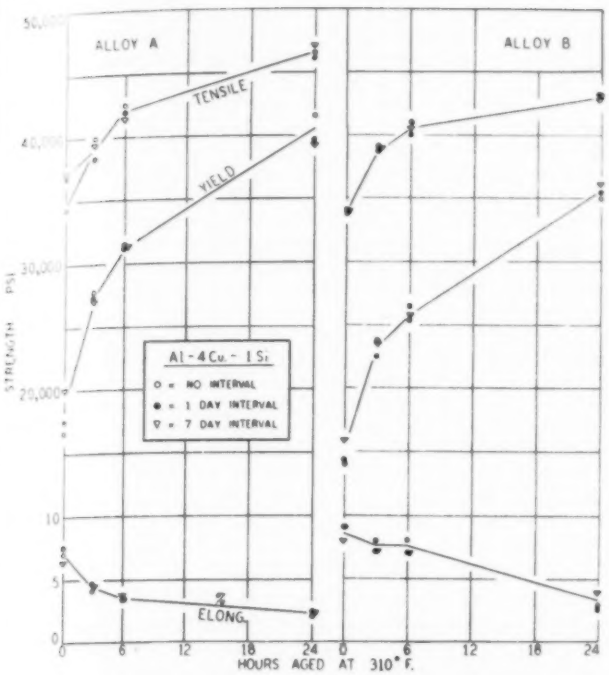
cess of the formation and growth of the nuclei particularly suited for development at elevated temperatures. He also visualized that the "knots" or peculiar segregations produced at low temperature had an effect on the hardening potentialities of particles formed at higher temperatures, producing lower hardness results.

In 1933, Masing[9] offered an explanation for the hardening anomalies in duralumin in which he resorted to the formation and re-solution of unstable nuclei as indicated by experiments in supercooled liquids. He suggested that the nuclei formed at room temperature (similar to Merica's "knots") were of nearly critical size and did not readily develop as a result of the reduced thermal energy available to the remaining copper atoms.

The unstable nuclei would be redissolved at elevated temperatures and a second type of stable nuclei was formed and developed in the conventional pattern. Presumably, again, the algebraic hardening effect of these concurrent dynamic processes is not a maximum.

These investigators all agree that the type of precipitation occurring at room temperature is different from that produced at elevated temperatures and more recent studies have concurred.[2] Internal strains developed during the quenching operation undoubtedly are contributing factors in the observed phenomena, but they have not been completely evaluated. The postulation of a working hypothesis presents intriguing possibilities but there is a compelling need for further investigation with regard to aluminum casting alloys in particular before a sufficiently complete understanding of the mechanism is realized.

In the present investigation only those alloys rapidly aging at room temperature are sensitive to a low temperature interval between quenching and subsequent aging at a higher temperature. The 4 per cent copper alloys investigated here aged relatively little over the observed 7-day period while the Mg_2Si alloys have large aging increments the first day (Figs. 1, 3, 4) after



quenching. It is well known[10,11] that relatively small amounts of iron inhibit room temperature hardening of wrought 4 per cent copper alloys, and commercial casting alloys of this type are similarly affected. A comparatively pure (0.19 per cent iron) 4 per cent copper casting alloy ages rapidly at room temperature[12] and on such an alloy different results would probably be obtained.

Summary of Results

The effects of various room temperature aging intervals between quenching and aging at elevated temperatures on some mechanical properties of several commercial aluminum sand casting alloys have been determined. The intervals studied have no effect upon the aging characteristics of commercial 4 per cent copper alloys. Alloys utilizing Mg_2Si (Al, 7 per cent, 0.3 per cent Mg and Al, 1 per cent Cu, 5 per cent Si, 0.5 per cent Mg) as a hardening compound have a pronounced reaction.

Heat treaters experiencing difficulty in surpassing elongation requirements on these alloys may improve this property by aging castings one day at room temperature after quenching before artificially aging them at 310 F. If aging at elevated temperatures is conducted immediately after quenching, high strengths and hardness are obtained at the expense of elongation.

Acknowledgment

The work reported in this paper represents part of a program of research conducted at the Central Research Laboratory of the American Smelting and Refining Company for its Federated Metals Division.

The author wishes to acknowledge indebtedness for the constant guidance and continued interest of Dr. A. J. Phillips and Mr. A. A. Smith, Jr.

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DISCUSSION

Chairman: R. E. WARD, Eclipse-Pioneer Div., Bendix Aviation Corp., Teterboro, N. J.

Co-Chairman: W. MADER, Oberdorfer Foundries, Syracuse, N. Y.

CHAIRMAN WARD: Early in the war, we supplied aluminum-magnesium-silicate alloys. I would like to substantiate your findings. Although we did not investigate it deeply enough to find an increase in the tensile properties, we did specify that the artificial aging be held for 24 hr. in order to allow us to meet the minimum elongation figures in some cases.

W. RUDIN[1]: Using the 355 type aluminum alloy as an example, did you soak at 980° F. followed by a hot water quench?

MR. QUADT: There are three different solution treatments. The 356 type was treated at 1000° F. for 16 hr. and quenched in water over 180° F. The 355 was solution treated at 980° F. and the 195 type was solution treated at 960° F. All were quenched in hot water.

MR. RUDIN: What would happen if you quenched in cold water? The theoretical conclusion would be for more compound to be retained in solid solution, thereby maintaining a higher solution potential when precipitation begins.

MR. QUADT: If you quench in cold water, I think there is no doubt you will retain more of the precipitating compound in solid solution. But it would seem to me that ought only to accentuate the situation because then you will have a higher solution potential when your precipitation starts. In other words, if some of it has come out of solid solution due to the hot water quench and you still get this reaction, then you know that the solution potential is less due to the fact you have already precipitated some compound. The effect under discussion, I would think, would be more pronounced. However, I have not investigated it and I am only theorizing. We did not go that far because we attempted to simulate commercial practice which requires hot water in order to minimize warping of the castings and we had to make this applicable to commercial practice.

MR. RUDIN: I ran a series of tests utilizing both cold and hot water quenching. Six bars were quenched in cold water and their mates were quenched in hot water over 180° F. The results showed about a 15 per cent difference in tensile strength; the bars quenched in cold water had higher tensiles and hardness, but lower elongations than their mates which were quenched in hot water.

MR. QUADT: We attempted to evaluate the effect of the room temperature interval. As far as the actual mechanical properties

[1] Wm. L. Rudin, Chicago

are concerned, there is no question but what you will get better properties by quenching in cold water for the reasons you stated. But I do not think it would make any difference on the effect of the room temperature interval. In other words, if you quench in cold water and run through the same procedure, all the properties would be higher but the effects of the room temperature interval I would expect to be the same or even more pronounced.

H. BROWN[2]: In searching for the answer to this problem, I might tell you about something I ran into. You showed two types, one apparently neutral and one which shows dropping with prolonged room temperature aging. If aluminum-zinc-magnesium alloys that are air aged are allowed to stand for 48 to 72 hr. after they are poured before you artificially age them you get a marked increase in both elongation and tensile.

CHAIRMAN WARD: In the 4 per cent copper, 1 per cent silicon, 195 type alloy, did you study the effect of the magnesium content within the impurity limits with regard to the age hardening?

MR. QUADT: Yes, but not on a complete investigation of this type. Whenever the magnesium was over, let us say 0.05 or certainly over 0.07 per cent, it acted more nearly like the magnesium-silicide hardening alloys than the copper hardening type and we found the effect of magnesium to be quite pronounced in that regard, particularly on the room temperature aging. It would speed up the rate at which it aged markedly.

CHAIRMAN WARD: We found a similar condition.

MEMBER: Approximately what is the room temperature at which these were aged? That is, conceivably there might be some difference between summer and winter performance.

MR. QUADT: The room temperature in this case was the temperature of the laboratory and that would range anywhere from 72° to 85° F. We wanted to carry it a little further and see what would happen, of course, if we kept these things cold, as might be the case under a certain kind of foundry conditions but we never did do it. But in that range, let us say, 75° F., this reaction seems to occur.

CHAIRMAN WARD: Why did you choose the salt bath method for artificial aging? Was there any particular reason?

MR. QUADT: Yes. Our reason for utilizing the salt bath is simply that we have laboratory facilities. We are not a commercial shop, and in a research laboratory we try to set up apparatus in which we can be reasonably sure of our temperatures and in a salt bath we can control with electric heating to close tolerances. It is a better method of control. Besides, it is a cheap method, we can use a number of small furnaces operating at various temperatures simultaneously in the laboratory.

MEMBER: We used a gas-fired muffle type furnace where we so-called age hardened this aluminum alloy. We worked on the 178 and the 528. I believe we used a temperature of 550° F. and it was kept there for about 1 hr. or 1½ hr. and brought them out. Then we pulled test bars, of course, which were stripped and found we increased the tensile strength considerably and that the yield point invariably increased also to within about 5,000 psi. of the tensile strength. That was very consistent. The elongation also held very constant. We did not get a drop. It was about 6 per cent on these particular alloys, with an increase in tensile strength and yield strength up to about 5,000 psi. of the tensile strength.

MR. QUADT: The alloys you mentioned are wrought alloys, they are not cast alloys, and you can do much with wrought alloys that you cannot do with cast alloys with regard to the effect of the rolling conditions themselves. The grain size, the fiber conditions, recrystallization, time and temperature of solution treatment, all have an effect on heat treatment. In the case of cast alloys we are relatively limited in these respects. Also, the 175 alloy contains both high copper and high magnesium so that the two situations are not very comparable.

MEMBER: Would you discuss sensitivity of these alloys with different magnesium content with regard to their age hardenability within the specifications?

MR. QUADT: That has been a point of some importance. Many of you are familiar with the specifications of these alloys and there is a difference of 0.2 per cent in the magnesium specification. In other words, Alcoa 356 permits a range of 0.2 to 0.4 per cent magnesium, while Alcoa 355 specifies from 0.4 to 0.6 per cent magnesium. There is not a very wide range, as you can see but there is a rather marked difference between the two ends of that specification with regard to ductility and tensile strength. Some of you who are familiar with these alloys may have noticed that in the 356 alloys that I used, the magnesium was on the low side and we got very high ductility and upon aging we nicely got over the minimum tensile strength of 30,000 psi. That should be kept in mind, too. A heat treater who wants to keep out of trouble certainly ought to watch his magnesium content. If the magnesium is on the high side, you are going to get still higher strengths with still lower elongations for the same procedure. The same thing is true whether you are considering 355 or 356. I know of instances with certain large consumers where they specify magnesium to much closer than the current specifications for that reason. In other words, these alloys are sensitive to the magnesium in that range. If you have low magnesium, you have to age longer in order to get the strength up and if you have high magnesium you have to cut your aging time in order to retain enough ductility.

MEMBER: What is the effect of copper?

MR. QUADT: Copper in those two alloys is not particularly important. First of all, the Alcoa 356 type has in some cases as low as 0.2 per cent or lower. In the range of 0.3 per cent I have not found any effect. In the 355 I have likewise never found any effect.

CHAIRMAN WARD: Not only must the heat treater, but the foundryman must watch the magnesium content since it will vary with continual remelting of the metal. There will be magnesium loss if it is held at elevated temperature for too long a period of time. Sometimes it is necessary to make magnesium adjustments in 355 and 356 to maintain the proper cohesion in properties.

CO-CHAIRMAN MADER: We should straighten out the customers on some of these problems. They will specify 356T4. The material will age harden afterwards. For instance, take the Navy specification where they require 4 per cent elongation. Certainly the test bars that you send to the customer will not meet the 4 per cent elongation requirement unless he pulls them in a very short interval after you have heat treated them. The Navy even allows the inspectors to come to your plant and pull them immediately after you have heat treated them.

CHAIRMAN WARD: Is there no time interval specified?

CO-CHAIRMAN MADER: I think there is a time interval but it does not seem to mean much. The same way, too, on aged alloys on the T6 treatment. Those alloys continue to age and it would seem to me to be more logical to specify an over-age treatment in the beginning and to hold stable properties. Now we will send them a casting supposedly heat treated to the T6 specification and then the customer tries to straighten that casting after it is aged further and he runs into trouble. I think it would be better to tell them in the beginning to use an over-aged treatment and to get a stable casting or the specifications should be revised.

CHAIRMAN WARD: Some of the committees of the Aluminum and Magnesium Division of A.F.A. are attempting to cover those subjects.

CO-CHAIRMAN MADER: When we use this method of heat treatment I think we really waste time. We are trying to meet the specification by devious variations of the heat treatment factor. It should be thoroughly understood by both parties. It is the responsibility of the knowing party to give that information and to clarify it. I think the trend should be to the over-aged treatment and more stable properties because as long as we use these type of alloys, we have to consider the aging phenomena and certainly they will continue to age. To some extent it would not matter, in other cases it may certainly have a serious effect on the final strength that you are going to have in the casting and effect its use in service.

[2] Solar Aircratt Corp., Des Moines, Iowa

ARC WELDING OF CAST IRON WITH NICKEL ELECTRODES

By

T. E. Kihlgren * & L. C. Minard **

ABSTRACT

General considerations involved in the arc welding of cast iron with nickel electrodes are discussed. The effect of such welding variables as preheat, superimposition of beads in multipass welds, bead sequence, etc., on structural characteristics, machinability and tensile properties of welded cast iron is presented. A preliminary torch "degassing" procedure is described for improving the welding response of castings containing dissolved gas. Several examples of field applications of arc welding of cast iron covering salvage of defective castings, repair welding, and fabrication welding are illustrated.

FUSION WELDING as a means of salvaging defective castings in the gray iron production foundry is gaining increasingly wider acceptance. For a number of years, little publicity was given by the foundry industry at large to this useful and important production aid, despite its growing importance. A valuable service was performed by the A.F.A. in holding a symposium on the subject of welding cast iron at the annual meeting in 1946, and the several papers [1, 2, 3] * presented at the session gave a good perspective of the general status of welding in the cast iron production foundry, as well as a helpful discussion of welding techniques and methods. Only by interchange of information can the advantages and limitations of the welding process as applied to cast iron be properly evaluated and the question of when to salvage and when to scrap a casting be answered.

Various mechanical methods and fusion welding and brazing processes are in use for the salvaging of castings, and for repairing of cast iron parts which have failed in service. It is not the purpose of this paper to deal with the problem of the choice of method of salvage or reclamation but rather to confine the discussion to the arc welding of cast iron, and more specifically to arc welding using nickel electrodes.

Three-fold Purpose of Arc Welding

The arc welding process may be used on cast iron for any one of three main purposes:

1. For the salvaging of defective castings in the production foundry,

2. For the repairing and reclamation of parts which have failed in service, and

3. As a means of fabrication, the component parts of the weldment consisting of gray iron castings only, or including castings, forgings or wrought parts of other compatible metals such as, for example, mild steel.

The first type of application is no doubt the one of major interest to the foundry industry since it provides a means of increasing the yield of acceptable castings. The second however is by no means unimportant to the production foundry since repair of equipment by arc welding provides important economies of time and labor.

Advances in welding electrode development in recent years now permit limited consideration of the third field of application. In some instances casting difficulties inherent in the design of a specific casting may be overcome by using a weldment of two or more castings. Welding may also be used to join cast iron to other metals, involving such operations as the attaching of steel brackets or fittings to gray iron castings, eliminating the need for drilling and tapping. It is apparent that the inherent characteristics of cast iron are such that good judgment should be exercised in applying welding as a means of fabrication.

Welding with Non-Ferrous Electrodes

Although arc welding of cast iron is done with both ferrous and non-ferrous electrodes, the latter possess the advantage of producing machinable welds, without the necessity of preheat. While the majority of arc welding with non-ferrous electrodes, possibly as much as 90 per cent, is thus done without preheat, there are circumstances in which preheating is helpful, or even necessary. When preheating is employed with the latter type of electrode, the preheat temperature is much lower than with ferrous electrodes and is usually for the following purposes:

1. To decrease the cooling rate of the cast iron adjacent to the weld and thereby prevent or minimize the formation of a hard zone in the heat affected area of the cast iron, with an accompanying reduction in machinability and

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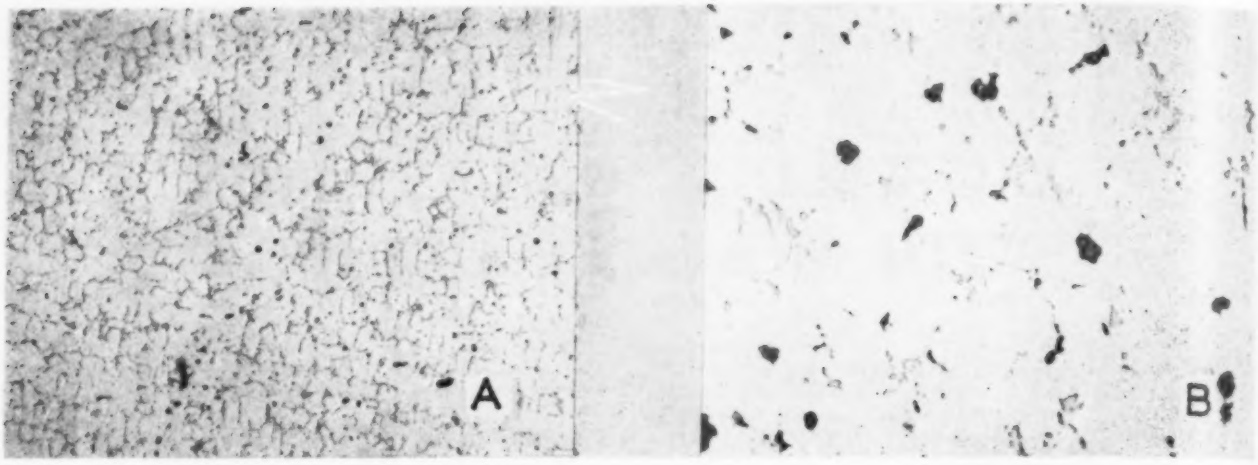


Fig. 1—Microstructure of nickel welds on cast iron. A, left, at a magnification of 250X and B, right, at a magnification of 750X.

2. To reduce the temperature gradient and the consequent stress concentration, thus diminishing weld and fusion zone cracking tendencies.

Observations, made over a number of years, had indicated commercially available nickel electrodes to have technical advantages over nickel-copper electrodes, when applied to welding of cast iron. However the nickel electrodes had been designed primarily for the welding of wrought nickel, and the slag characteristics were not entirely satisfactory when the electrodes were used on cast iron. Attention was therefore directed to the development of a nickel electrode specifically designed for the welding of cast iron on either DC or AC equipment.

A nickel electrode meeting the requirements was developed as a result of laboratory studies and ultimately put into commercial production. It has since been used in considerable quantities for salvage of defective castings and for repair welding. The electrode consists of a nickel core wire coated with a carboniferous lime-type of flux, containing a moderate amount of reducing agents. The electrode operates at considerably lower amperages than the nickel electrode customarily used for welding wrought nickel, an advantage in welding cast iron. Sufficient carbon is introduced through the flux coating to obtain graphitic carbon in considerable amounts in the fusion, resulting in improved shrinkage solidification characteristics, as well as a structure imparting a degree of machinability similar to that of the cast iron. The electrode is generally used without preheat of the casting, although special circumstances may make a moderate preheat desirable.

Several Nickel Electrodes Available

The experiences and welding techniques discussed in the balance of the paper are largely concerned with this electrode, and are based partly on laboratory studies and partly on observations made in the field. It is recognized that several other nickel electrodes designed for the welding of cast iron are now commercially available. It will be apparent that many of the observations, particularly those dealing with the effect of welding operations on the base metal are also applicable to those electrodes, as well as the various nickel-copper electrodes. However, inasmuch as electrode characteristics and optimum welding techniques vary from one electrode to another, the manufacturer's instructions as to correct welding procedure should be consulted for the specific electrode to be employed.

It is proposed to discuss a few general considerations to be observed in arc welding of cast iron using nickel electrodes, prior to giving illustrations of applications of the electrode to the three phases of cast iron welding previously outlined.

Structural Characteristics of Welded Joints

As far as the weld metal itself is concerned the structure is independent of welding conditions, since no phase transformations are involved. The structure consists simply of graphitic carbon dispersed in the alpha nickel matrix. Figure 1a shows the structure of weld metal deposited by the nickel electrode, in a 60° included angle X joint on 1 in. thick cast iron plate. Figure 1b shows the weld metal at higher magnification. While different conditions of welding, and variations in cooling rate may alter somewhat the dendritic pattern, the structure of the deposited metal will remain essentially unchanged.

However, the structure of the cast iron at the line of fusion, and in the heat affected zone immediately adjacent to it is determined by the composition of the cast iron, and the cooling rate following welding. The latter, in turn, is governed by such factors as section size, welding current, speed of welding, and preheat and interpass temperatures. Mention should be made of the tempering effect of subsequent passes on the cast iron adjacent to previous beads, in multi-pass welding. Further reference will be made to this important effect.

The effect of preheat, and multi-pass tempering effects may be observed by a comparison of two welded joints, No. 1586 and No. 1594, made under the same conditions, except for the use of a 600 F preheat in the latter. Both welds were made on 4 x 5 x 1 in. thick plates, using X weld butt joints with a 60° included angle and a $\frac{3}{16}$ in. root gap. The welds were made with $\frac{5}{32}$ in. electrode at about 120-130 amperes, welding con-

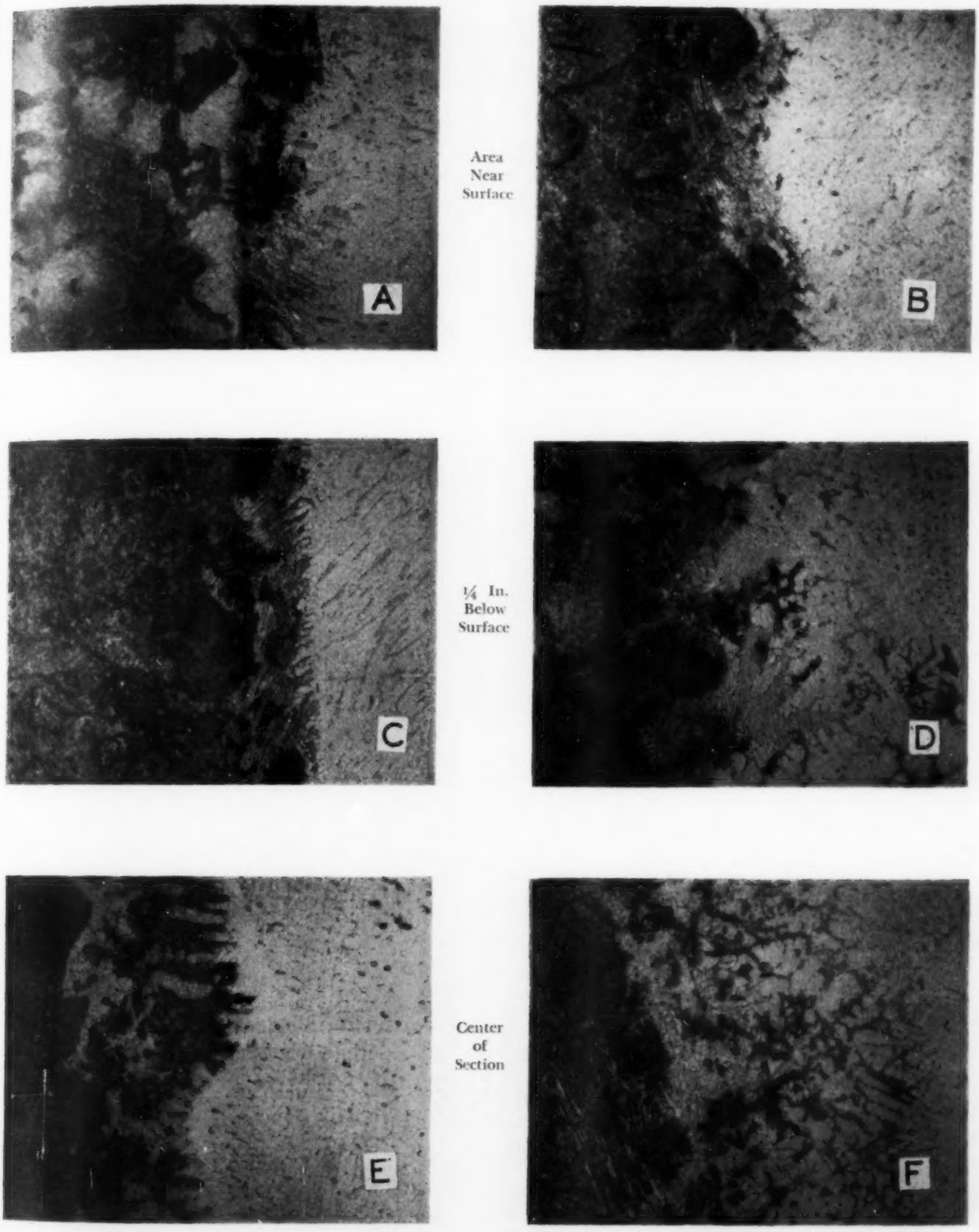


Fig. 2—Structural characteristics of cast iron adjacent to nickel weld. Left, no preheat; right, 600° F. preheat.

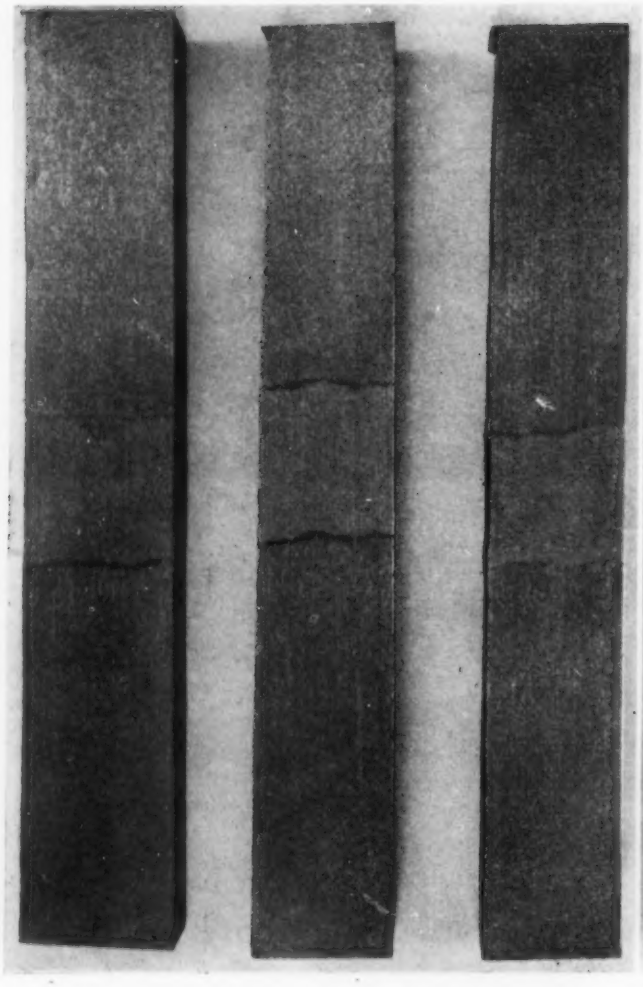


Fig. 3—Machined surfaces of 1-in. thick cast iron plates, welded without preheat. Left, $\frac{3}{16}$-in., (center) $\frac{5}{32}$-in., and (right) $\frac{1}{8}$-in. diameter electrode used.

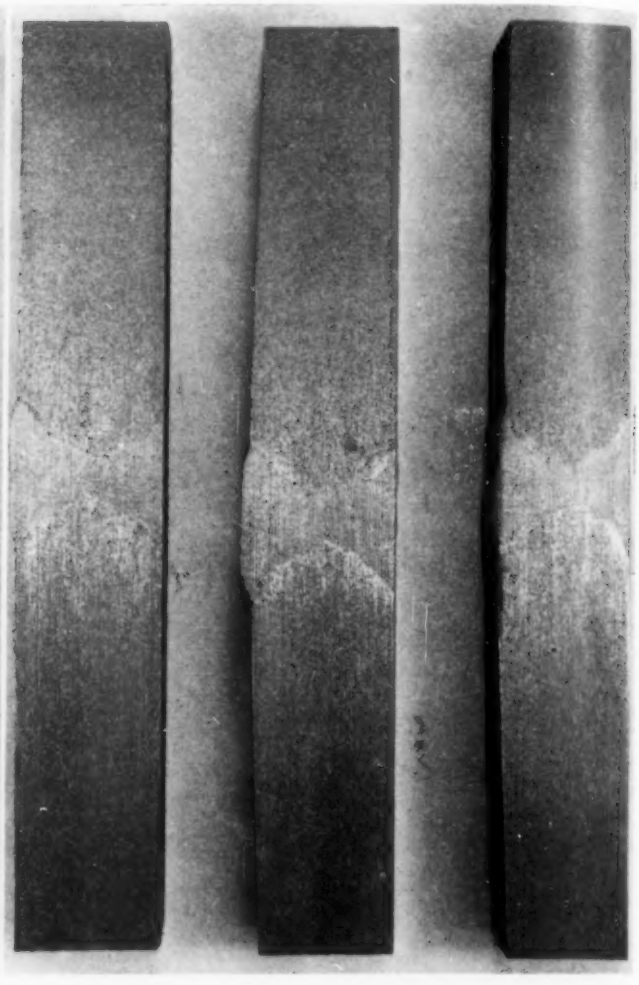


Fig. 4—Machined cross sections of welds on 1-in. plate made without preheat. Left $\frac{3}{16}$-in., (center) $\frac{5}{32}$-in., and (right) $\frac{1}{8}$-in. diameter electrode used.

tinuously, pausing briefly to remove slag, and examine for possible defects. The cast iron plate contained about 3.25 per cent total carbon and 2.25 per cent silicon along with about 0.16 per cent phosphorus and 0.12 per cent sulfur.

Figures 2a to c show the structure of the joint made without preheat, the successive figures referring to areas close to the surface of the plate, about $\frac{1}{4}$ in. below the surface, and about the center of the welded joint respectively. Figures 2d to f show corresponding sections for the preheated weld. The figures show both the effect of preheat on the structure of the fusion and heat affected zones, and the annealing effect of superimposed passes.

A comparison of Figs. 2a and 2d shows that although the preheat has had little effect on the amount of carbide flash in the fusion zone near the surface of the joint, the heat affected zone of the preheated weld shows less acicular (martensitic) structure in this area. Below the surface of the plate, the preheated weld shows less of a carbidic area at the fusion line, the carbide having decomposed to give graphite. The tempering effect of subsequent passes on the structure of the heat affected zone is quite well shown, especially in weld No. 1586. Compare 2a and 2c.

Inasmuch as both welds were made by continuous welding, the test plates had sufficiently slow cooling rates so that no zones of excessively high hardness were encountered even near the top passes. Little difficulty would be expected in machining either of the welds.

There are many more factors that merit attention in a study of microstructural characteristics of welded cast iron. The foregoing illustrations demonstrate the effect of preheat and multipass welding. Other welding variables which affect the rate of cooling, and extent of plate dilution will also affect the structure of the cast iron in critical areas and their effects should therefore be considered in selecting techniques.

Machinability of Nickel Welds on Cast Iron

The term "machinable welds on cast iron" is frequently rather loosely applied. Actually any weld made on cast iron with the various non-ferrous electrodes is machinable as far as the weld deposit itself is concerned, although the degree of machinability will vary somewhat with the hardness and structure of the

deposit. The machinability of the welded joint is therefore primarily determined by the condition of the cast iron at the fusion line, and in the heat affected zone. The structure of the cast iron in these areas as already mentioned is primarily determined by the composition of the cast iron and the cooling rate.

For most purposes, welded joints made on cast iron with nickel electrodes, without the use of a preheat, possess adequate machinability. Figures 3 and 4 show machined areas of welded joints in a cast iron of 30,000-35,000 psi tensile strength. The joints were made with $\frac{3}{16}$, $\frac{5}{32}$ and $\frac{1}{8}$ in. electrodes respectively, without use of preheat. Figure 3 shows the result of shaper cuts across the surface of the welds, the cut being just sufficiently deep to remove the surface skin. Figure 4 shows shaper cuts across the section. The cuts were all made at 33 strokes per min, and 0.020 in. feed, and a 0.015 in. depth of cut.

Figure 3 shows a narrow zone of increased hardness at the surface of the plate, which zone however did not interfere noticeably with machining. Figure 4, illustrating the machined cross section, depicts clearly the effect of superimposed passes in tempering the heat affected zone adjacent to previous beads, the center of the joint showing virtually complete absence of a hard zone. The annealing effect of superimposed passes can be employed to reduce or eliminate hard zones adjacent to the weld, at surface of the casting.

This is done simply by completing the joint flush to the surface, cooling to about 70-200 F and then using cover passes, confined entirely to the weld, to reheat the surface of the plate. Figure 5 shows the effect of such a procedure. The top plate has not been subjected to the "cover pass" tempering bead technique, while the two below were so treated, the second with a single and the third with a double cover bead. The effect of the treatment is apparent from the photograph. This technique is recommended by several manufacturers of non-ferrous welding electrodes.

Preheat Sometimes Required

In some instances, where machining requirements are especially severe, the use of a preheat may occasionally be indicated, a preheat of as much as 450-600 F sometimes being employed. The effect of preheat on the structure in the heat affected zone was shown in Figs. 2d to f inc. A preheat may be particularly useful on a light section not lending itself well to the "cover" or annealing pass technique.

It should not be overlooked that post annealing may occasionally be usefully applied. A post heat sufficient to temper martensite may be helpful in reducing the hardness of the heat affected zone, thus increasing the ability to resist service stresses. This is not common

Fig. 5—Machined cross sections of welds on ½-in. cast iron plate showing annealing effect of cover passes.

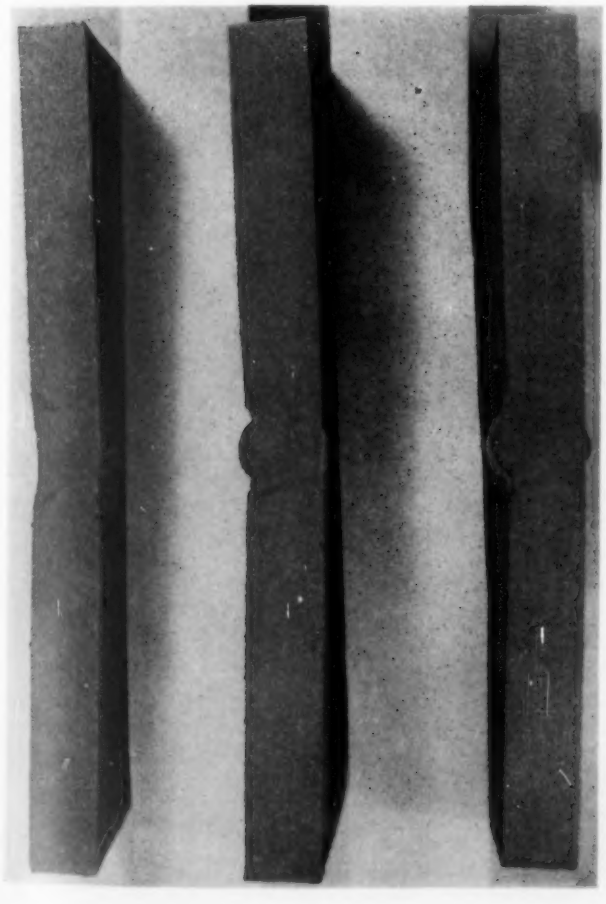
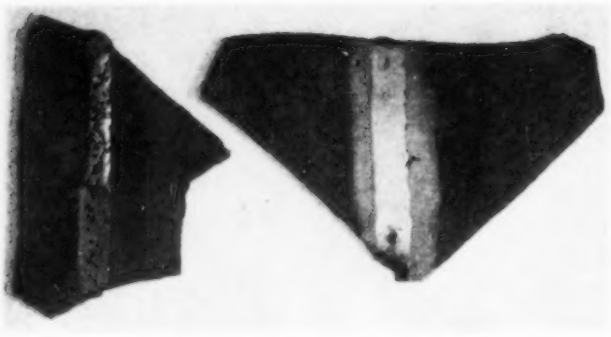


Fig. 6—Tensile specimens grooved to increase unit stress in weld cross section at breaking load.

Reduction of Weld Cross Section by Grooving.	None	49%	62%
Calculated Stresses, at Failure:			
in Weld	30,200	61,500	68,000 psi
in Plate	30,200	31,500	25,500 psi





As Cast Degassed

Fig. 7—Effect of torch degassing treatment applied to high gas content cast iron, prior to welding.

practice, but probably merits consideration where conditions warrant. Post heating may be accomplished with a torch if necessary.

Cooling following the completion of welding or post heating operations can be further retarded by covering the casting with insulating material, such as rock wool or asbestos or, when size permits, by burying in diatomaceous earth.

Strength Properties of Welded Joints

Using a 60° included angle $\frac{3}{16}$ in. root gap X weld butt joint, several 4 x 5 x 1 in. thick plates were welded along a 5 in. edge. The plate materials were of the following composition:

Melt—T16862 3.25 C, 2.75 Si, 0.6 Mn, 0.07 P, 0.16 S
Melt—T16863 3.00 C, 1.85 Si, 0.6 Mn, 0.07 P, 0.16 S

All welds were unpeened and were made with $\frac{5}{32}$ in. diameter electrodes at 120-130 amperes, without preheat and with 300 F and 600 F preheats. Standard 0.505-in. tensile specimens were machined transverse to the weld, with the weld located in the center of the reduced section. The test results are tabulated below:

Plate	*Preheat, ° F*	*Breaking Strength,* psi*
T16862	none	32,350
	300	30,000
	600	32,500
T16863	none	32,100
	300	34,250
	600	39,000

* All fractures occurred in the cast iron.

Since all of the fractures occurred in the cast iron, the data primarily demonstrate the weld tensile strength to be in excess of that of the parent metal.

An attempt was made to force the fracture in the weld metal by reducing the weld cross section of the 0.505 in. diameter specimen. The specimens were machined from plates welded without preheat using plate material from heat T16862 (above). With the weld area sufficiently reduced, fracture was forced to take place in the weld at a calculated stress of 68,000 psi, without allowance for notch effects. Figure 6 shows the regular tensile specimen, and specimens with weld cross sections variously reduced, after fracture.

Degassing of Castings

Occasionally, castings are encountered which contain dissolved gas. While such castings may be perfectly satisfactory from a service viewpoint, attempts to repair casting defects by welding lead to weld porosity, due to release of the gas. This difficulty can be overcome by prior subjection of the casting to a simple degassing treatment. The treatment consists of torch heating the *immediate area* against which metal is to be deposited to a dull red, holding briefly and allowing to cool.

Figure 7 demonstrates the efficacy of the degassing treatment. Both butt joints were made on the same iron (in this instance a "bath tub" iron of the analysis 3.40 TC, 2.40 Si, 0.65 Mn, 0.75 P, 0.12 S). The joint on the left was made without prior treatment of the iron, and that on the right after degassing.

Where casting dimensions permit, degasification of the entire casting can be accomplished by furnace annealing at 1000-1050 F for 15 to 30 min., the time depending upon the section size and the condition of the casting. Temperatures in excess of 1050 F are undesirable because of the detrimental effect on the structure and properties of the cast iron. Localized torch "degassing" treatments, confined to the immediate area against which weld metal is to be deposited, are less critical in this respect, since the area affected is relatively small, and for the most part, is reheated in the subsequent welding operation to above the transformation temperature. Generally, it is probably preferable, for this reason, to use the torch treatment instead of furnace degassing.

In cases where no pre-treatment of the iron is possible, where suitable facilities are not available, or where the requirements are not strict, the first passes can be used to drive off the gas in the vicinity of the

Fig. 8—Gear teeth partially restored, with and without prior degreasing and degassing.





Fig. 9—Defective casting reclaimed by welding. As welded.



Fig. 10—Defective casting, salvaged by welding, after machining and hand filing to original contour.

weld, the bead chipped and welding continued. This is a less satisfactory procedure than torch degassing, but circumstances may make it acceptable.

It should of course be recognized that degassing treatments are not needed in a majority of salvage and repair welding applications. However, the above procedure should be found effective in overcoming such difficulties when encountered.

Oil Impregnated Castings

Castings which have failed in service, in some cases, will have been used under conditions causing them to be saturated with oil and grease, which must be removed before any satisfactory welding can be accomplished. The use of a suitable solvent such as carbon tetrachloride, sometimes in conjunction with cleaning by steam, may often be sufficient where the penetration is superficial.

In some instances, particularly where the cast iron has a rather open structure, the casting may be so heavily oil impregnated that it is necessary to heat it at a temperature sufficiently high to char the oil and drive off volatile matter. A temperature of about 750 F will accomplish this, the time required depending on the condition and size of the casting.

When a torch is used to decompose the oil and grease and drive off volatile matter, the casting, or at least the portion in the general vicinity of the weld, should be heated at about 750 F and held until visible evolution of fumes has ceased. At this stage it is often useful to apply the above described degassing treatment to the area against which metal is to be deposited. It may not always be necessary, as far as "de-greasing" is concerned, but it takes little longer to do so, and saves reheating if degassing should ultimately be found desirable. Figure 8 shows a gear, one tooth of which was partially restored without preliminary treatment. The other partially rebuilt tooth was welded after driving off oil from the casting and degassing.

Cast Iron Arc Welding Applications

The illustrations which follow briefly cover salvage welding, maintenance and repair and fabrication welding, and are intended primarily to indicate the scope of application of arc welding of cast iron with nickel electrodes. As mentioned before, by far the larger portion of the cast iron welding with such electrodes is accomplished without preheat. The examples cited show castings welded both with and without preheat.

A certain percentage of defective castings are inevitable in the production foundry. In many cases, a reasonable proportion can be salvaged by arc welding, thus reducing the percentage of rejects. Such defects as

Fig. 11—300 lb. pipe guide barrel with four projections built up by welding to correct pattern error.



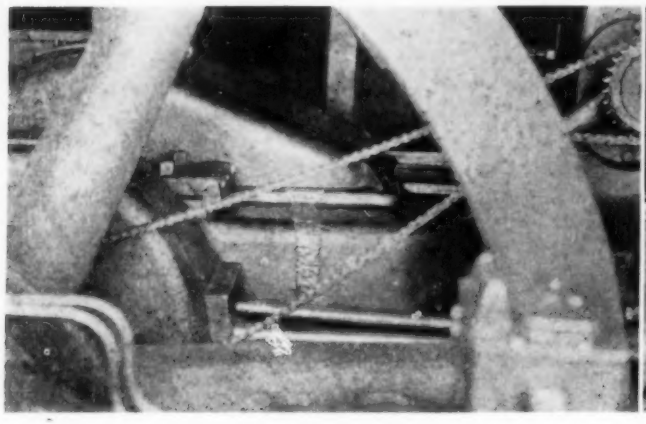


Fig. 12—Large air compressor showing repair of portion of 42-in. long crack.

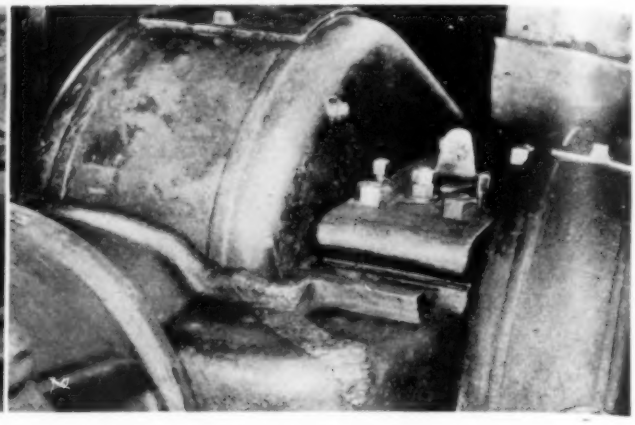


Fig. 13—Large air compressor showing repair of 14-in. long crack.

misruns, sand wash, cold shuts, external shrinks, etc., often can be corrected satisfactorily. Such factors as size, shape, initial cost of the castings, the magnitude, nature and location of the defect, etc., must be weighed in deciding, first, upon the feasibility of welding salvage and secondly whether or not the reclamation is economically justified.

Figure 9 shows a small casting which had a small defect in the form of a cavity on the top surface. The defective area was chipped out to clean metal, and then welded continuously, without preheat, using a $\frac{5}{32}$ in. diameter electrode at 130 amperes. After completion of welding, the excess weld metal was removed by turning in a lathe, followed by hand filing to restore the original contour (Fig. 10). Frequently the re-finishing in such an application will be accomplished entirely by grinding.

Figure 11 shows a pipe guide barrel after salvaging by welding, in the areas indicated by the arrows. After the casting had been made, it was found that the pattern had been made $\frac{1}{4}$ in. too short on the four projections. Using $\frac{5}{32}$ in. diameter electrodes at 130 amperes, d.c., reversed polarity, without preheat, the projections were overlaid with sufficient metal to permit machining to the correct over-all dimensions. Seven passes were used to build up three layers on each projection.

The first of these two examples consumed only one electrode, while the other required a consumption of about nine pounds. Generally, the more common defects can be repaired without preheat, using moderate quantities of electrode.

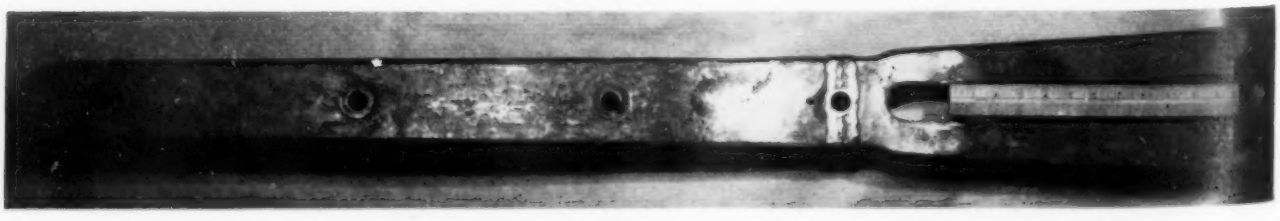
Maintenance and repair welding of gray iron castings constitutes an important use of nickel electrodes. Often the service failure may put equipment out of commission upon whose continued operation production is dependent. Speed of repair is therefore often of paramount importance and the use of arc welding becomes imperative. Frequently such repairs may be followed by re-finishing operations, necessitating the use of non-ferrous electrodes. However, even where machining is not contemplated, such electrodes are often preferred, deposits made with them being readily ground or even hand filed to restore original contours.

Figures 12 and 13 show a welding repair in a large air compressor which had been in service about 25 years. Two cracks appeared in the base, in a section that served as an oil crank case, one crack being about 14 in. long and the other about 42 in. long. The casting was about $\frac{3}{4}$ in. thick, and was oil saturated. An estimate was made of a week to dismantle and a week to rebuild, plus several days to repair by conventional methods.

The repair was made with nickel electrodes without dismantling. The cracks were chipped out to form a vee, degreased with carbon tetrachloride and steam, and skip welded. The initial passes were made with $\frac{1}{8}$ in. and the balance with $\frac{5}{32}$ in. diameter electrodes. The welding time consumed about two days, the casting never being allowed to become too hot to touch. After six months in service, an inspection revealed no signs of failure. The repair was made about two years ago and the compressor is still giving satisfactory service. Figure 12 shows a portion of the 42 in. joint and Fig. 13 depicts the repair of the 14 in. crack.

Figure 14 shows a repair on the cross-head frame of a 20,000 lb. tensile testing machine, fracture having occurred through a bolt hole section. The sections to be

Fig. 14—Repair weld on tensile machine cross head frame.



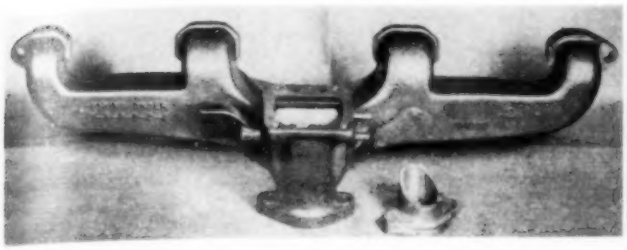


Fig. 15—Manifold before attaching secondary port seen in foreground.



Fig. 16—Manifold showing secondary port welded to casting.

joined were ground to form a single vee joint with a 60° included angle. A short trial bead indicated the casting to contain dissolved gas. The surfaces of the vee were therefore torch degassed as described in a previous portion of this paper. The area in the vicinity of the joint was then preheated to 600 F and the joint made by continuous welding with a $\frac{5}{32}$ in. electrode, the frame being rigidly restrained during welding to preserve alignment.

The preheat was used since it was felt that the location of the fracture, and the service conditions indicated the desirability of diminishing the plate hardness in the heat affected zone. After welding, the bolt hole was reamed out and the portion of the weld overlaying the cross head guide surface was removed in a few minutes by hand filing, eliminating the need for machining to restore the original contour.

Welding a Means of Fabrication

Some judgment should be exercised in this phase of application of welding. Under conditions involving moderate sections, and joints which are not subjected to excessive restraint, welding fabrications involving castings only, or cast and wrought parts, can be satisfactorily accomplished.

Figure 15 shows a cast iron automotive manifold, as purchased from a foundry. The company concerned wished to cut a hole in the manifold and attach a secondary port to it. The separate casting used for the latter port is shown in the foreground. Figure 16 is a photograph of the casting after the welding had been completed. The fabricator indicated the following procedure: inspection of parts, preheat of manifold to 450 F, port set in fixture and mounted on manifold, tack welding of port in three places, removal of fixture followed by continuous welding, and lastly, a post heat to 450 F, removal of slag, and air cooling.

An AC welder was used with a reported welding current of 160 amperes, using $\frac{5}{32}$ in. diameter electrodes. The position of the port was maintained accurately at a 31½° angle, ± ½° and all other dimensions were held within ±0.003 in. Boring of the port was easily accomplished.

Figure 17 illustrates an application of welding of steel to cast iron, the steel supporting bars on which the switch box is mounted being arc welded to the cast iron base of the stock turret. Formerly the bars were bolted to the base, installation of the box involving chipping of the casting at four positions, and drilling and tapping of bolt holes. The present procedure consists of single pass welds, using $\frac{5}{32}$ in. electrodes, without preheat. The electrode is also used to build up the surface where the top bar is attached, in order to bring both supporting bars into the same vertical plane.

It is anticipated that this last phase of cast iron welding, namely fabrication welding, will receive increasingly greater attention. The illustrations used may suggest other applications of fabrication welding which are technically sound and offer economic advantages.

In closing, it is recognized that many factors involved in welding operations on cast iron must yet be evaluated, and the advantages and limitations of the arc welding process, both in its general aspects, as well

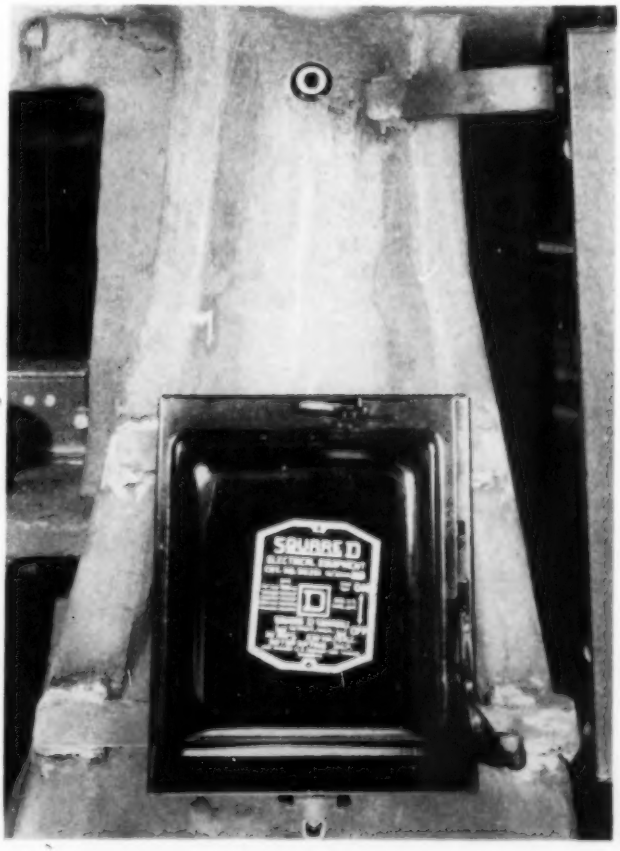


Fig. 17—Showing method of attaching switch box supporting bars of mild steel to cast iron base.

as with special reference to non-ferrous electrodes be more clearly outlined. It is hoped that this paper will be of assistance to the foundryman in surveying possible applications of arc welding in the iron foundry industry.

Acknowledgments

The authors wish to express their thanks to J. S. Bryner of the laboratory staff for metallographic studies. The assistance rendered by J. F. Ryan and S. S. Robinson of the New York office of The International Nickel Company, Inc., in supplying photographic illustrations of field applications of cast iron welding is also gratefully acknowledged.

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DISCUSSION

Chairman: R. G. MCELWEE, Vanadium Corp. of America, Detroit

Co-Chairman: W. W. LEVI, Lynchburg Foundry & Machine Co., Radford, Va.

CO-CHAIRMAN LEVI: What is the relative or the approximate hardness of the weld material in the repaired gear tooth in Fig. 8?

MR. KIHLGREN: On a cast iron of about 190 Brinell, the weld material runs from 160 to 165 BHN. The weld hardness on the gear is probably of the same order.

I. N. KITCHEN[1]: Would you deem it advisable as a safety measure to preheat and slowly cool the welded casting in a case of this particular type?

MR. KIHLGREN: There is a pretty extensive record of a large number of salvage operations done without preheat.

I think there are a number of applications where some preheating would be desirable. In the specific illustration of the fabrication welding of the manifold shown in Figs. 15 and 16 the fabricator tried it out with and without preheat and felt that the 450° F. preheat and postheat were necessary.

I believe the desirability of using a preheat or postheat will depend a great deal upon the service condition to which the casting is to be subjected and upon the design of the casting.

D. P. FORBES[2]: At what temperature is the weld metal deposited?

MR. KIHLGREN: The melting point of pure nickel is about 2650° F. There is quite a bit of carbon in the weld fusion, which drops the melting point and I would estimate it would be possibly around 2450° F. Of course, the arc welding process involves pretty rapid solidification and immediately when deposited the weld metal freezes.

W. R. JENNING[3]: What effect in analysis does the parent metal have on these welds? Is there any relationship between the chemical analysis and what you may except of the machinability of the weld?

MR. KIHLGREN: In the first place, the composition of the cast iron will play an important role in determining how high the hardness of the heat affected zone will be. The irons on which these electrodes have been used vary from around 3.50 carbon and 2.40 silicon down to about 3 per cent carbon and 1.85 silicon.

MR. JENNING: I think that our experience has worked out that way.

MR. KIHLGREN: As far as the machining operations are concerned, in some cases that we are aware of, the salvaged castings are put back in the production line and subjected to the same machining operations as the unwelded castings.

J. L. YARNE[4]: In talking about the strength of these welds, we notice that the failure occurs at the junction of the weld and the parent metal. How does that strength compare with the parent metal?

MR. KIHLGREN: The particular irons we tested had a tensile strength of 30,000 to 35,000 psi. In the test bars that were pulled in connection with this whole series of tests, it is quite true that most of the fractures occurred in the heat affected zone. I think it is probably true that the ones that were made with a 600° F. preheat were more inclined to break further from the weld.

MR. YARNE: You said the parent metal had a strength of around 30,000 to 35,000 psi. What tensile strength did you find in the case of failure in the cast iron in the welded specimens?

MR. KIHLGREN: Failures occurred at 31,000 to 34,000 psi. I should again like to emphasize that the notched tensile specimens were for the purpose of forcing failure in the weld and do not necessarily represent the true strength of the weld itself.

G. W. ZABEL[5]: What is the normal practice which you have followed in welding cast iron by the electrode method. Generally, heavy castings are fitted with studs. This is particularly true of maybe 2 or 3-in. sections and 6 or 10-in. sections, where you run into a large hole or even a cracked casting. You introduce steel studs into the casting and interweave your weld. Do you recommend all studs be left out in repairing that type of casting, with that type of a weld?

MR. KIHLGREN: We have made joints of mild steel to cast iron and there is no particular difficulty as far as making the joint is concerned. I do not see any particular objection to welding without removing the stud, although I have never tried it.

MR. ZABEL: In large castings you have heavy segregations of silicon, under which conditions you do not get a good weld and to offset that you use steel studs.

I was just wondering if your metal was more adhesive and forms a better fusion between the two metals than you naturally get with a normal welding rod. I have used nickel rods and have used steel rods and we have also used studs. This is something new and I would certainly be interested in the type of material and how it is applied if you have that information.

MR. KIHLGREN: I cannot answer that question, but it is something that should be considered. I appreciate your bringing up that point which we have overlooked in our test runs.

J. A. GRIFFIN[6]: When you talk about welding with studs or rivets you are talking about two different types of welding. One is automotive welding and the other is welding of fixtures and jigs and machine bases and such as that, heavy casting that may run from 500 up to 2,500 lb. and it takes in an entirely different welding field. I certainly would not be in favor of eliminating the studs on any procedure we have in welding those heavy castings and your nickel electrodes are not going to replace that.

I believe the author of the paper means to present this mainly for automotive salvage and you take that field in itself and it is certainly a worthwhile article that he has written for us.

W. S. BINGHAM[7]: This discussion reminds me of the time I went into a blacksmith shop and an old colored fellow who used to bend plowshares down at the point to make better suction and increase its life, and I asked, "Can you tell me what heat you use to bend it down so that you won't crack them?" He says, "No, suh, I can't tell you that. I knows that heat when I sees it. All I know when I sees it." All I know about cast iron is I know by seeing it. I have welded a lot of cast iron.

In the old days before a lot was known about building up railroad cross bars, I was one of the foolish fellows that went up and down the railroad with a carload of oxygen acetylene tanks and welded railroad crossing frogs with a torch. Of course, there is a little trick in doing that but I could not get anyone else to do it. It seems they were afraid of a big flame.

[1] Sweets Foundry, Inc., Johnson City, N. Y.
[2] Gunite Foundries Corp., Rockford, Ill.
[3] Deere Mfg. Co., Waterloo, Iowa
[4] Chain Belt Co., Milwaukee, Wis.
[5] Fairbanks Morse & Co., Beloit, Wis.
[6] Pontiac Motor Co., Pontiac, Mich.
[7] Centrifugal Machine & Engineering Co., Kalamazoo, Mich.

A lot has been said about the test bars that were shown. You know that test bar thing is getting to be quite a joke around the entire foundry industry, but, of course, you fellows may not have heard that before, but the point is the fracture will show at the side of the weld in the casting. If the test bar was not reduced in order to minimize the amount of stress placed in that particular area, the fracture would show at the side of the weld. But, there is a change in the carbon structure of cast iron in the weld area. We soon learned if we put in studs we did not have to puddle the parent metal.

The welding industry has recognized the two types of welds. One is puddle weld and one is fusion weld. In the old blacksmith days we used to think if you did not puddle weld, if you did not have the weld up to a dripping welding heat, it would not work. We learned later, possibly the metal did not need that much heat. If I were a purchaser of castings that had been repaired by stud welding, or, if I was a buyer of the castings, I would refuse the castings thus welded. I will say, however, in many cases, and I might almost say 25 per cent of all of the larger scrap castings could be saved if there is enough cost in the casting to justify the weld.

I studied nickel alloy in gray iron and its effect naturally upon centrifugal motion, in which I am very much interested, and I do know, however, when I see a good job of welding that someone back of it has given it some thought. When I go into a foundry and see how they try to repair their castings, I do not care whether it is a nickel rod or any other kind of rod, without proper consideration of the weld, I know what they are going to get and so should the foreman of the repair welding department.

MR. KIHLGREN: The application of welding to salvage of gray iron castings requires the combination of competent supervision and a workman who has pride in doing a good job and can follow instructions given. The characteristics of cast iron are such that you have to give thought to the welding procedure to be used.

MEMBER: Have you found the machinability of the weld at the line of union was affected by preheating?

MR. KIHLGREN: We have generally found that a weld that has been preheated will have a somewhat better machinability at the fusion line on the surface of the casting. However, I think I should emphasize again that the so-called "cover pass" technique, that can be applied to castings welded without preheat, is effective in tempering the heat affected zone, thus improving the machinability.

APPLICATION OF A SINGLE SLAG PROCESS TO BASIC ELECTRIC STEEL

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General Electric Co., Schenectady, N.Y.

AN APPLICATION in the plant with which the authors are associated requires a considerable number of medium to large sized molybdenum alloy castings which are made from either basic electric or basic open hearth steel. A brief comparison of basic electric with basic open hearth steel castings follows. The castings from basic electric steel usually have been made in the Schenectady foundry of the company. The castings from basic open hearth steel have been purchased from an outside vendor.

The castings made in the foundry of this company have been of metal from a six-ton, Heroult-type, basic lined furnace. Charges have varied from 22,000 to 30,000 lb. The steel making process used was the standard double-slag process in which a short boil was obtained lasting for probably 15 to 20 min. removing possibly 0.10 to 0.15 per cent carbon.

The use of aluminum has been prohibited in the manufacture of the steel for these castings. The heat-treatment applied consisted of a normalize and draw. An acicular structure of three to four ASTM grain size was desired.

In routine checking of microstructures of attached test bars and in surveying the castings generally, the following was noted:

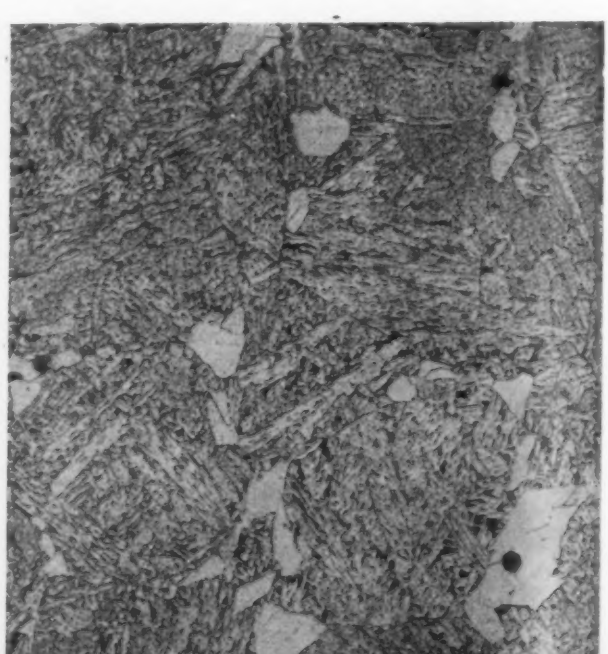
a. The basic open hearth product invariably attained the desired grain size on being heat-treated at 1742 F (950 C). Heat treating at 1922 F (1050 C) gave a proportionately larger grain. Figures 1 and 2

* Works Laboratory and ** Foundry Division.

Fig. 1—Basic open hearth molybdenum alloy cast steel. Heat treated at 1742 F; 2 per cent nital etch. ×100.



Fig. 2—Basic open hearth molybdenum alloy cast steel. Heat treated at 1922 F; 2 per cent nital etch. ×100.



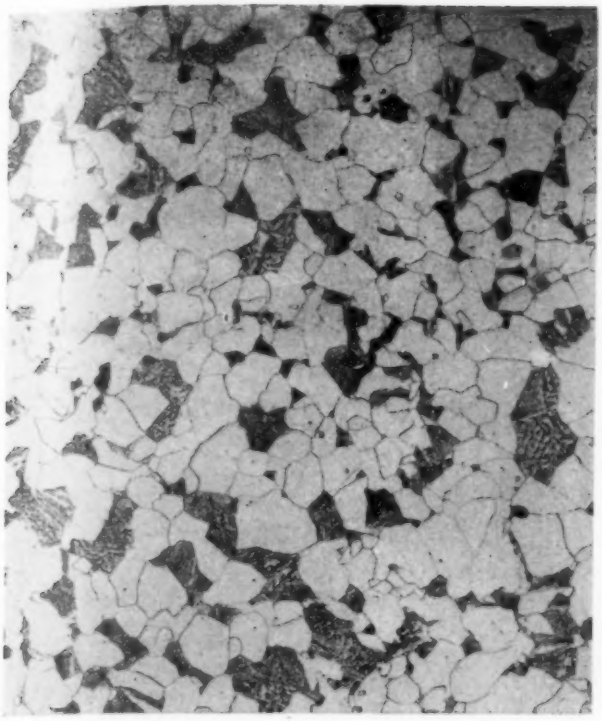


Fig. 3—Basic electric molybdenum alloy cast steel produced by double slag process—20-min boiling period. Heat treated at 1742 F; 2 per cent nital etch. ×100.

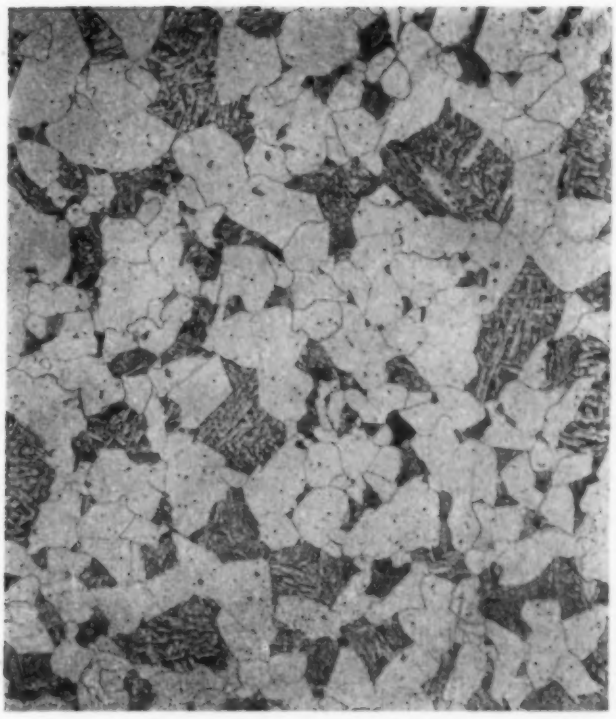


Fig. 4—Basic electric molybdenum alloy cast steel produced by double slag process—20-min boiling period. Heat treated at 1922 F; 2 per cent nital etch. ×100.

are typical microstructures of the open hearth steel.

b. The basic electric product made by the double slag process, generally, was nonuniform and had a grain size much finer than was desired even when heat-treated at 1922 F (1050 C). Figures 3 and 4 are typical microstructures of this steel.

c. Basic open hearth steel castings made in dry sand molds generally were quite free from pinholes. Basic electric steel castings, made under similar conditions, showed considerable tendency to form pinholes. Since the castings from the two types of steel were made in different foundries, it is impossible to give any idea of the relative tendencies of the two metals to form pinholes. It is possible that mold conditions largely determined whether or not pinholes were produced.

d. The analyses, in all cases considered, were similar. A typical analysis is as follows:

Element	Per Cent
Carbon	0.18
Manganese	0.70
Silicon	0.38
Molybdenum	1.10
Sulphur	0.015
Phosphorus	0.020

In order to minimize differences in microstructure resulting from differences in heat-treatment, all microstructures referred to herein were obtained by heat-treating two-inch samples from test bars in a small laboratory furnace whose control couple was adjacent the samples treated. The holding time was six hours in all cases. The samples were furnace cooled at a rate which was estimated to be equivalent to the air cooling of a large casting, or about 900 F (500 C) per hour through the critical.

Improvement in the Double Slag Process. It was first thought that the desired microstructures might be obtained from basic electric steel castings if the boiling period were lengthened so as to be more comparable to that of the basic open hearth. It was also thought that the reducing period should be as short as practicable to minimize pick-up of gases after the end of the boil.

Accordingly, the following requirements were placed on the steel making process.

a. The carbon in the charge should be 0.55 to 0.60 per cent.

b. The boiling period should be lengthened to approximately one hour, removing an estimated 0.40 per cent carbon.

c. The boil should be stopped when the carbon reaches a value equal to or only slightly below that desired in the finished product.

d. Forty-five minutes should be ample time for the reducing period.

Considerable improvement was noted in the basic electric product when the heats were boiled thoroughly and finished quickly. The microstructures showed decidedly better grain growth (Fig. 5) and it appeared that pinholing was lessened. However, it may be seen by comparing Fig. 5 with Fig. 2 that the performance of the open hearth was not equalled.

Single Slag Process. At this point it appeared that the possibilities for improvement of the double slag basic electric process had just about been exhausted without bringing the product to par with basic open hearth steel.

The following reasoning was then developed with respect to basic electric steel making.

a. The difference between basic electric and basic open hearth steel must be due to something beyond the elements normally measured by chemical analysis.

b. The difference could be caused at least in part, by neutral or reducing gases residual in the bath after an insufficient boil or reabsorbed after the end of the boil. Of these gases, nitrogen is most likely the trouble maker. It has been used on occasion to promote fine grained structures in steel. Furthermore, there is considerable likelihood of nitrogen absorption during the reducing period.

c. The apparently greater tendency of basic electric steel to pinholing could be due to greater quantities of hydrogen (and nitrogen) either residual in the bath after an insufficient boil or reabsorbed after the boil.

d. The boiling period should be of sufficient duration to eliminate residual neutral and reducing gases. Until it is possible to measure accurately gas concentration in molten metal this must of necessity be a cut-and-try procedure.

e. Since gas elimination ceases with the end of the boil and since re-absorption can begin immediately, the reducing period offers an opportunity for increasing the gas content of the metal.

f. The reducing period, save for the recovery of oxidizable alloys, performs no apparent useful function in the manufacture of steel.

g. If the reducing period were eliminated the resultant basic electric process can be quite similar to that of the basic open hearth.

h. Some cost reduction can be accomplished by the elimination of the reducing period.

As a result of the above reasoning the following basic electric steel making process was devised. This process should be classified as a single slag process in which the metal is tapped under an oxidizing slag.

a. The heat should be boiled thoroughly for good elimination of neutral and reducing gases. For the particular application in mind, a boil of about one hour's duration removing approximately 0.40 per cent carbon was thought necessary.

b. The carbon in the charge should be 0.55 to 0.60 per cent so that after the removal of 0.40 per cent, the concentration of that element should be approximately that desired in the finished product.

c. The boil should be blocked with 0.10 to 0.15 per cent silicon as soon as the carbon concentration reaches the desired value.

d. The manganese addition should be made immediately as low-carbon ferromanganese.

e. Since the end of the boil generally finds the bath somewhat colder than desired, approximately five minutes must be allowed for temperature adjustment. (Applying current after the end of the boil admittedly is against interest.)

f. The final silicon addition should be made to the ladle.

A number of molybdenum alloy steel heats produced in the basic electric furnace by the single slag process brought out the following complaints.

a. Since the usual fluidity measurements were not applicable, there was some confusion in judging temperature. It is believed that this complaint has been answered by the installation of an immersion thermocouple for accurate temperature measurement.

b. Erratic manganese recovery was experienced in heats whose boiling period was prolonged until the carbon value reached a low figure.

Fig. 5—Basic electric molybdenum alloy cast steel produced by double slag process—60-min. boiling period. Heat treated at 1922 F; 2 per cent nital etch. ×100.

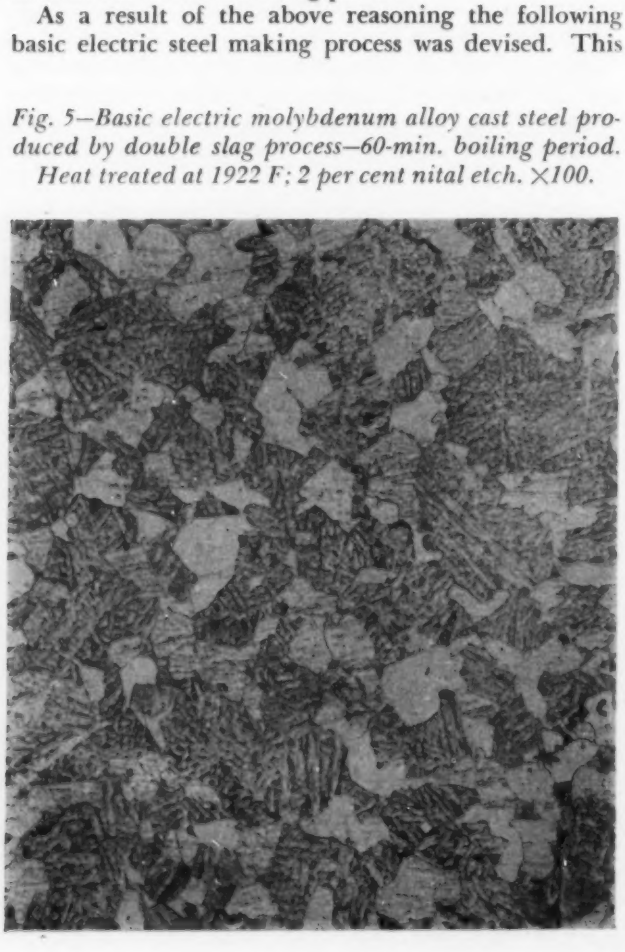
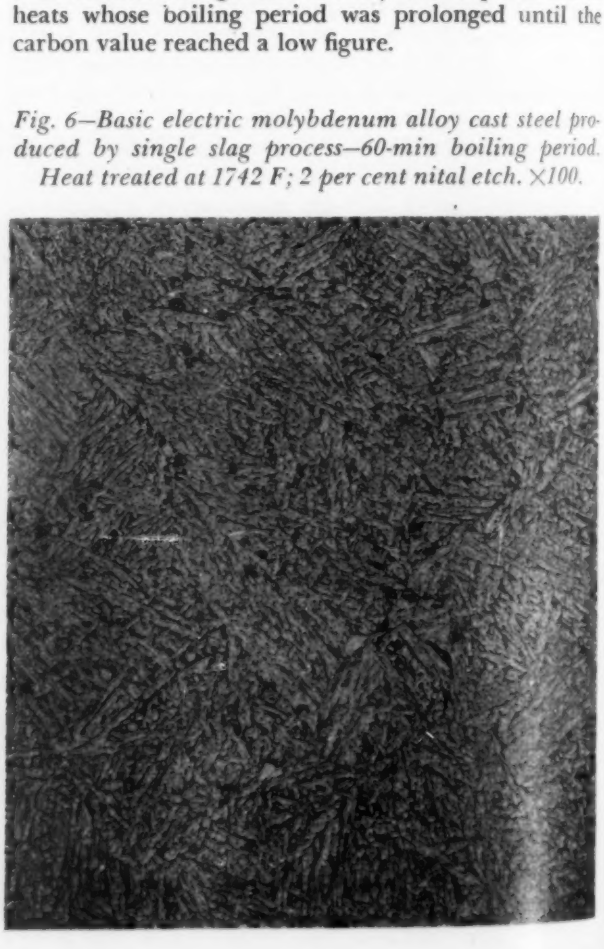


Fig. 6—Basic electric molybdenum alloy cast steel produced by single slag process—60-min boiling period. Heat treated at 1742 F; 2 per cent nital etch. ×100.



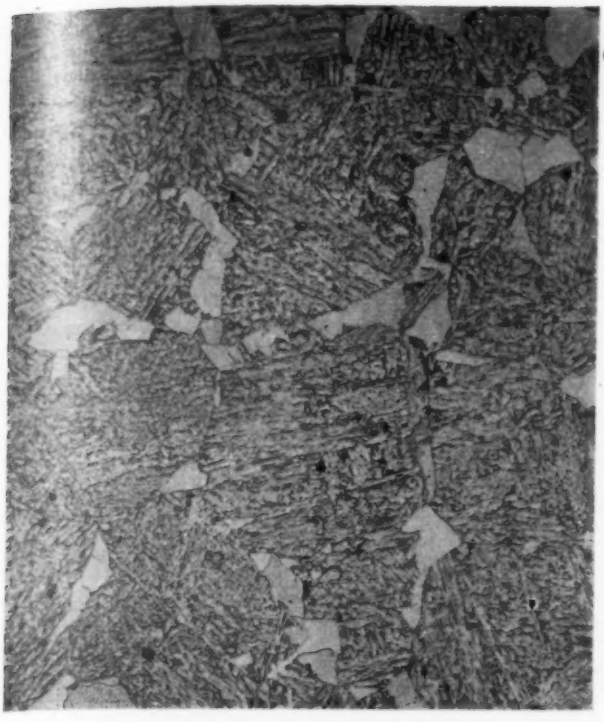


Fig. 7—Basic electric molybdenum alloy cast steel produced by single slag process—60-min boiling period. Heat treated at 1922 F; 2 per cent nital etch. ×100.

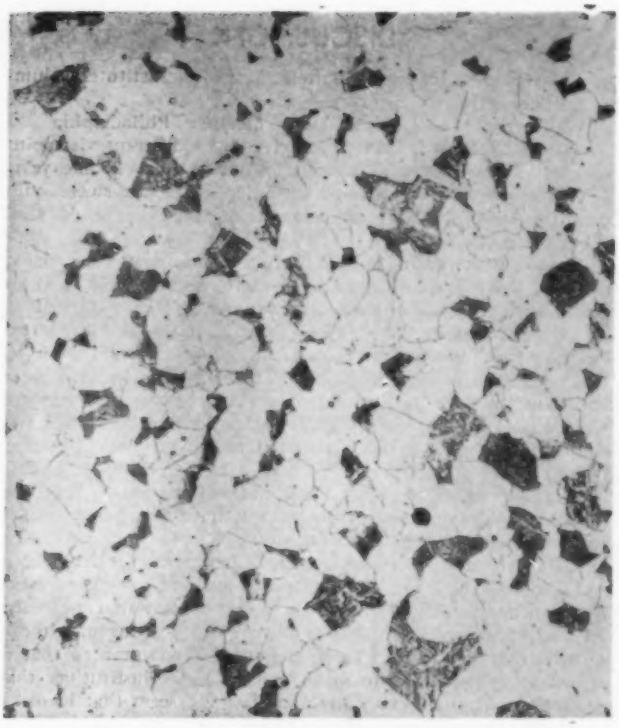


Fig. 8—Basic electric molybdenum alloy cast steel produced by single slag process—20-min boiling period. Heat treated at 1922 F; 2 per cent nital etch. ×100.

Figures 6 and 7 are photomicrographs of samples of steel produced by the single slag process in which a boiling period of one hour was used. Figure 8 is a photomicrograph of a sample from a heat which was boiled 15 to 20 min., but otherwise finished in the prescribed manner. It is evident that adequate boiling is necessary if full benefit is to be realized.

No data of value was obtained as to the relative tendency to form pinholes of the steel produced by the single slag process. Attempts to determine gas content, which should be a measure of the tendency of the metal to form pinholes, were not successful. The writers were further handicapped by the fact that there was no better definition of mold conditions than "dry sand" and "green sand."

Metal and Mold Standards

In the absence of the use of aluminum, probably both metal and mold should subscribe to certain standards, if pinholing is to be avoided. The opinion is ventured that the single slag process should produce steel of lower gas content than does the double slag process and hence the metal so produced should have less tendency to form pinholes.

A large number of medium to mild carbon steel heats also were produced by the single slag process. In these heats the boil was limited to about half the time duration and carbon drop used in molybdenum alloy steel heats. An aluminum addition of 2 lb per ton was made to the ladle about two minutes after pouring from the furnace was complete. Castings from these heats have caused no adverse comment and may therefore be considered satisfactory.

The saving in time and electricity realized by the elimination of the reducing period, obviously was the amount of each normally expended in the period.

The following conclusions appear to be justified.

a. The single slag process is capable of producing steel approaching the product of the open hearth in grain growth characteristics.

b. The reducing period in the basic electric furnace performs no useful function in steel making except possibly that of recovering oxidizable alloys from the slag.

c. Some reduction in the cost of a heat is accomplished by the elimination of the reducing period.

d. The single slag basic electric process is not a guarantee of freedom from pinholes if mold conditions are to be ignored. It is the opinion of the writers that this process produces metal of lower gas content than does the double slag process. How much lower is not known.

e. Measurement of gases in liquid metals is necessary if the tendency of the metal to form pinholes is to be evaluated.

f. This development is offered in its present stage of completion for the purpose of eliciting such criticism and suggestions as may be offered.

Acknowledgment

The authors wish to acknowledge the advice and assistance generously given by Messrs. F. W. Hanson and J. N. Ludwig, Jr., of the Electro Metallurgical Company and by Messrs. R. S. Archer and T. D. Parker of the Climax Molybdenum Company. Acknowledgment is also made of the cooperation of the superintendent and personnel of the General Electric foundry at Schenectady.

DISCUSSION

Chairman: C. H. LORIG, Battelle Memorial Institute, Columbus, Ohio

Co-Chairman: E. C. TROY, Dodge Steel Co., Philadelphia

CHAIRMAN LORIG: This paper gives us a different viewpoint on the relative merits of electric and basic steels. Some years ago, the consensus was that perhaps electric furnace steel would excel but today we have indications that basic open hearth steel is equally good.

It is also interesting to note that some emphasis is now being given to the role of nitrogen in steel. It is of interest to know that some of the emphasis on hydrogen is being shifted to include nitrogen in the study of gases in steel. There are reasons for this. Work by Benedicts and Loftquist, and work by other investigators has indicated that nitrogen may play a more important role in the control of grain size than does oxygen. This leads to new concepts of the mechanism of grain size control. We know so little about it as yet. It does illustrate, however, that grain size control is an extremely complex mechanism about which we must know more if we are to have the degree of grain size control the authors have found so necessary in their application for steel castings.

C. W. BRIGGS[1]: I appreciate the Chairman's remarks about nitrogen and I would appreciate the authors presenting some nitrogen analyses. I hardly see how one can say that grain size and other variables are due to nitrogen, or hydrogen, without quantitative information being presented. It is granted that it is not an easy analysis to make, but there are institutions that are making the analyses daily and it would seem that in order that an understanding of the situation is obtained that information on gas analyses should be provided.

[1] Steel Founders' Society, Cleveland

MR. HEALEY: Unfortunately, we cannot give any figures on gas content. We have tried gas analysis but the results to date are not worth reporting. We have not said exactly that nitrogen causes inhibition—we have suggested that it does, but have not proven it.

MR. BRIGGS: Well, how can you even suggest it.

MR. HEALEY: If you put a heat through a reducing period, there is more inhibition than if the reducing period is eliminated. What other than gas added during the reducing period could cause this?

MR. THOMAS: We were partly influenced by the fact that nitrogen-bearing ferro-alloys are often used to obtain fine grained steel.

MR. HEALEY: I hope at some future date to have more data that will bring out the answer to the question asked by Mr. Briggs.

J. K. MCBROOM[2]: I have had no experience with nitrogen in this type of steel, but while trying to control nitrogen in a higher alloy steel which I know was definitely influenced by the chromium content, I found that in 5-ton heats of steel, containing in the neighborhood of 20 per cent chromium, it was impossible for me to melt down a heat without picking up from 10 to 12 points of nitrogen. Those figures were checked in another laboratory, so we felt fairly sure of the results we obtained.

We also were convinced that this nitrogen pick-up was in general due to the high chromium content. In other words, the action in the arc of an electric furnace is essentially the Haber process for fixation of nitrogen, where you pass air through an electric arc and cool it rapidly after it passes through the arc. There always is air in your electric furnace, and you have all the essentials for the Haber process for fixation of

[2] Stainless Foundry and Engr. Co., Milwaukee

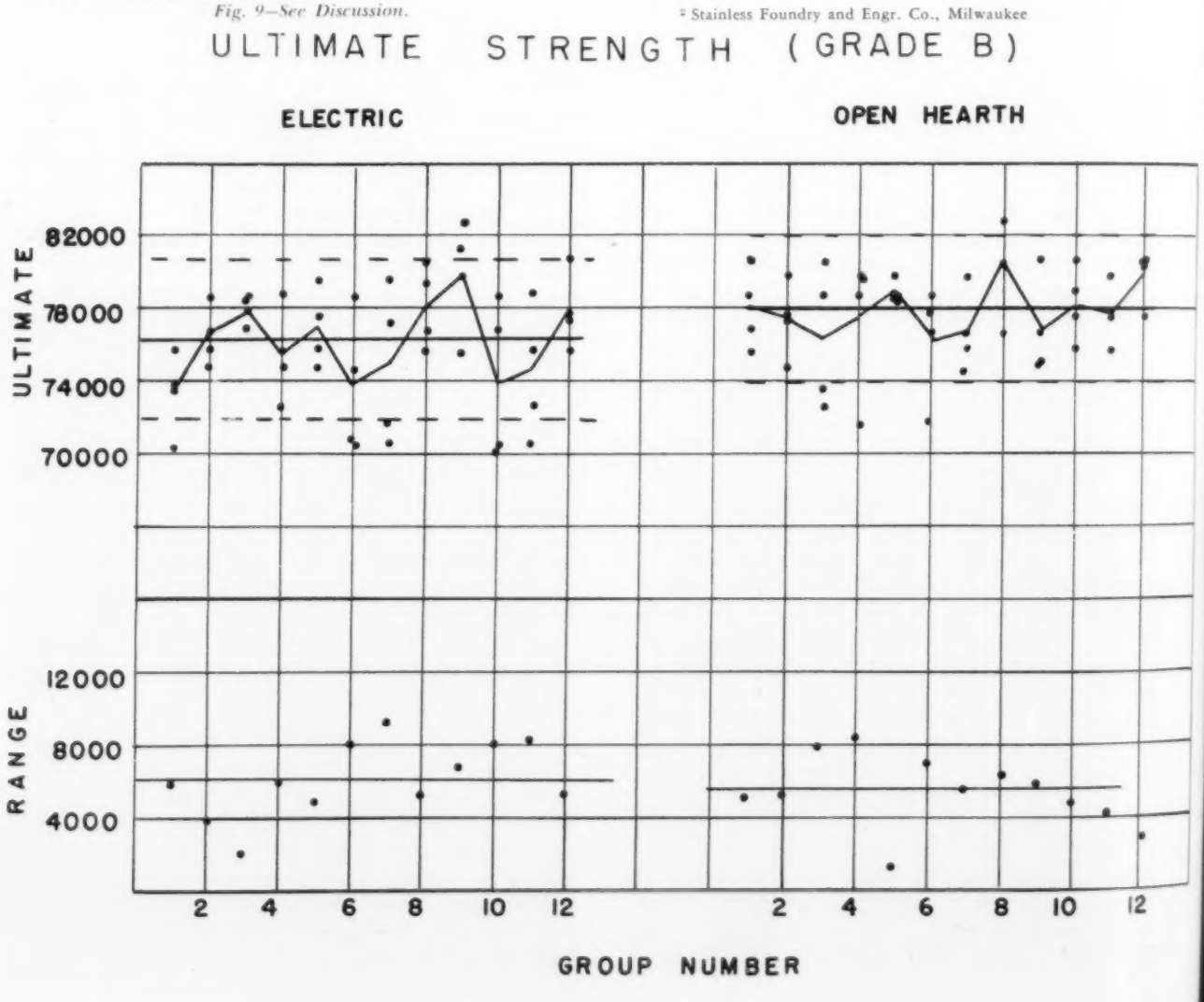


Fig. 9—See Discussion.

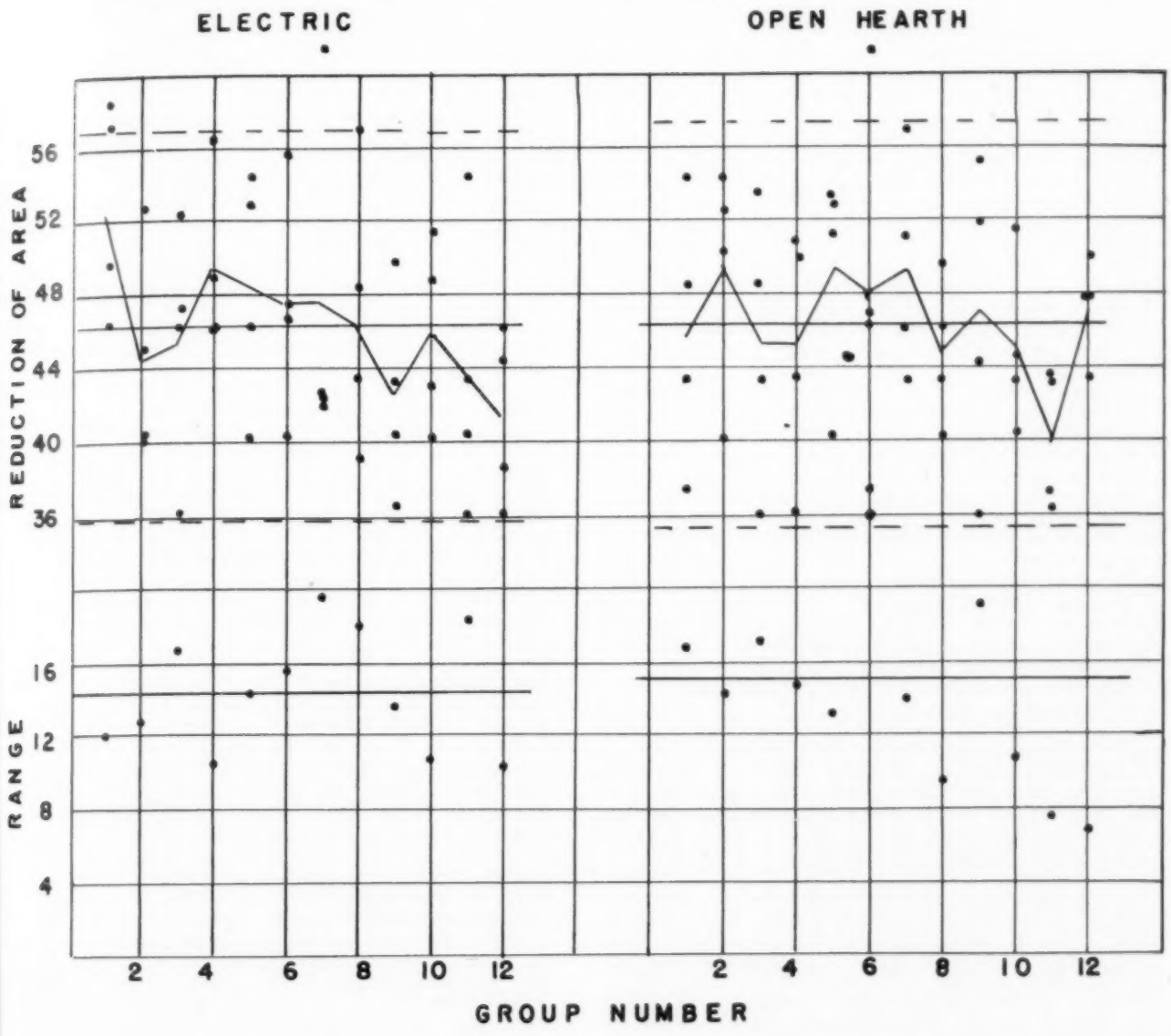


Fig. 10—See Discussion.

nitrogen except the cooling. The air cannot be cooled but if you have some means of holding this nitrogen fixed, it will not revert back, it can be fixed in your steel. A high chromium or high silicon steel will fix the nitrogen as some sort of nitride which is retained in the steel. Now, whether that or a similar reaction takes place in the steel that you have under consideration, I do not know, but I do know it did happen in the steel containing about 20 to 25 per cent chromium.

Mr. Healey: You think if we watch our residuals we might find a key to some of our problems. I think you have a very good point. It could be that residuals like chromium might hold the nitrogen combined. In our attempts at gas measurement, we did try to measure combined nitrogen along with total nitrogen and hydrogen. We believe our failure to get any consistent results was due to our equipment. We are now getting new equipment. If we are successful in gas measurement we should pick up the relationship to residuals.

H. H. Johnson[2]: The single slag basic electric process is of interest to us because we have converted one of our acid electric furnaces to a basic-lined furnace and in our work with this furnace we are trying out the single slag process.

[2] National Malleable & Steel Castings Co., Sharon, Pa.

I would, therefore, like to offer comments on the paper, largely based on our experience.

The first has to do with the prolonged boiling period to which the authors refer. We feel that the intensity of the boil rather than its duration is the important factor. If the boil is so violent that you practically have to lift the furnace roof, then it is doing some good. If the boil is slow, it can last for an hour without doing too much in the way of flushing out the gas in the metal, in our opinion.

We, too, would be very much interested in the gas analysis. We have indications that the gas content of heats made using the single slag process is perhaps slightly lower than that of heats made by the double slag process, but we have very meager data on that.

In connection with the measure of the properties of the steel produced, the authors have compared the basic open hearth product with that of the single slag basic electric by a study of microstructures. We would like to present Fig. 9 and 10 to show the comparisons by a study of the physical properties of the steel produced. In Fig. 9 we compare the ultimate strength values for test bars produced by the two processes. You will note that the data are presented in the form of quality control charts.

The level of strength for the steel from the electric furnace is slightly less than that from the open hearth. The control

limits are practically the same distance apart, indicating that the two processes give a product that is at about the same level of uniformity.

Figure 10 comparing the reduction of area for test bars from the two processes shows a more erratic condition so far as scatter of results is concerned than Fig. 9. The range or spread is practically the same for both processes, however, as is the level of values.

It would seem from these two comparisons that we are producing about the same quality of steel by the single slag basic electric process as we are by our basic open hearth process.

MR. HEALEY: Because we did not know how else to measure the boil, we decided on a certain carbon drop. Therefore, we specified that the bath should contain 0.60 per cent carbon at the start of the boil and that the boil should be stopped when the carbon reached 0.20 per cent. We have had some thought to the effect that a mild boil would affect the metal near the surface leaving that at the bottom unaffected. Probably we will have to make a better definition of boiling than carbon drop.

In regard to Fig. 9 and 10, we have had the same experience for the same composition and heat treatment. The large-grained material will have higher tensile and yield properties.

G. A. LILLIEQVIST[4]: How much phosphorus and sulphur reduction did you have?

MR. HEALEY: We have found, starting with an estimated 0.03 per cent sulphur with the basicity of the slag in order and with a good boil, that we should get down to 0.015 per cent sulphur and lower.

We have had sulphur under this figure and also a little above it. I suppose sulphur elimination is a sort of catenary. As you get down to 0.015 per cent and certainly to 0.010 per cent, the line flattens a bit and further elimination becomes harder. We have found that if the sulphur is brought to a low figure in the oxidizing period, there is little further reduction in the reducing period.

J. B. CAINE[5]: It is my impression that these steels are more or less special steels, coarse-grained steels for high temperature service, primarily, for creep resistance. Then would you recommend this practice for ordinary steels, and how do the low-temperature impact properties of these coarse-grained, silicon-killed steels compare with fine-grained fully deoxidized steels?

MR. HEALEY: Anything that is good in creep seems to be low in shock resistance.

In running a single oxidizing slag heat in preference to a double slag heat, a certain amount of saving is accomplished which is roughly the time consumed in the reducing period. That probably is the main advantage in using this process for producing plain steel for room temperature service. I suspect that plain steel produced by this process will be fine rather than coarse grained if the usual aluminum addition is made.

C. E. SIMS[6]: The authors favor elimination of the second slag and the use of only a single slag. In that I am in hearty accord. They have shown in the last statement that they accomplish very little during the second period, even in desulphurization, and that compares well with other data that have been published. What it amounts to is that the second slag, or the reducing period of the heat, is merely a prolonged block. Now, if the elimination of the second slag is good, why not go a step further in the same direction and eliminate the block on the first slag?

MR. HEALEY: You say if this is true, why not go further? I do not think anybody can argue with that. Was your idea to eliminate the block and add manganese and silicon both to the ladle?

MR. SIMS: Yes, that would be one way of doing it.

MR. HEALEY: You will note that after we blocked the heat we continued to apply current to the furnace for a short time. If the arc is supplying nitrogen to the bath, this application of current is against interest. What you suggest would be an improvement.

MR. THOMAS: I think Mr. Sims is perfectly right. If we only knew we could have definite control, we would certainly like to eliminate the block.

CHAIRMAN LORIG: In regard to this matter of time of boil, I just wondered whether anyone had experience with the use of oxygen gas to shorten the boil and perhaps obtain the same result? Those who were at the open hearth conference in Cincinnati heard a great deal about oxygen, not only for use in the open hearth but also in the blast furnace. There has been much work done in steel mills on the use of oxygen in the electric furnace. I am sure that within a year we will hear a great deal about its use in the steel foundry.

R. H. JACOBY[7]: We have done a little work using oxygen on the acid-electric process, with pressures running 75 lb., using a manifold set-up with a ½-in. line. As to our results, it is a little bit early to say. It is possible to know the carbon down and knock it down fast, but it is rather erratic. We were not able to duplicate the results early in the heat. We were told that we could start the carbon down shortly after obtaining a flat bath, but we did not find that to be true. We have found it best to wait for the first indication of boil. At the present time we are not using it in a production set-up but are using it to assist in getting the low 0.05 to 0.07 per cent carbon required on stainless alloy. We use the oxygen last to get that carbon down when it is hanging, knocking it usually from 0.12 or 0.13 per cent down to 0.06 or 0.05 per cent in less than five minutes. We found excessive oxygen pressures, going as high as 150 lb., were detrimental to the bottom of the furnace in melting about a 6,000-lb. heat in a 1½-ton furnace.

MESSRS. HEALEY AND THOMAS (authors' closure): We would like to say a little more about gas analysis. Our results to date have been disappointing. We do hope to be able to measure combined and total nitrogen and hydrogen. We think when we can do this we will be able to determine the relationship of these gases to normality or grain size. We also think with gas analyses we will be able to define an adequate boil.

We wish to thank those who have participated in the discussion. The remarks of Messrs. McBroom, Johnson, and Sims are of particular interest.

[4] American Steel Foundries, East Chicago, Ind.
[5] Sawbrook Steel Castings Co., Lockland, Ohio
[6] Battelle Memorial Institute, Columbus, Ohio
[7] The Key Co., East St. Louis, Ill.

IMPORTANCE OF RADIOGRAPHY TO INSPECTION

By E. L. LaGrelius *

ABSTRACT

Inspection departments can use standard radiographic techniques to great advantage in the preliminary and final inspection of castings.

Advantages of inspection by x-ray include rapid non-destructive examination of an entire lot of castings, assurance that defective castings have been properly repaired, and the availability of an immediate check on uniformity of foundry technique.

Of particular importance is agreement between the purchaser and the foundry as to the number, size and location of radiographs and the direction from which they are taken. These parties should also have an understanding regarding the interpretation of the films.

RADIOGRAPHY, as practiced by many foundries, has developed into a useful inspection tool and is becoming more frequently used as its applications and advantages are better understood.

Formerly, radiography was used almost entirely as a development tool to shorten the time required to get a pattern into production. In this connection, it is used in place of sectioning a casting which is time consuming. In addition, defects which would be discovered by x-ray are sometimes missed entirely when a casting is sectioned.

As the use of x-ray methods becomes more widespread in the castings industry, radiography is assuming increased importance in the inspection of castings. When used wisely, radiography can aid inspection in the following ways:

1. Radiographs of sound castings assure the inspection department it is receiving sound castings, similar to the pilot castings previously accepted by the purchaser.

2. Radiography assures the inspection department that a uniform casting production procedure is being followed.

3. Radiography is the only rapid method known which assures maintenance of a quality standard of internal soundness.

4. It aids in ascertaining whether or not a defect has been entirely removed through improved foundry technique.

5. It assures the inspection department that repairs made by welding are free of defects and that the entire defective area has been properly repaired.

6. Radiography aids the inspection department to ascertain in many instances which foundry department is chargeable for the defect.

7. It enables the inspection department to find defects before considerable labor costs have been expended in cleaning or finishing.

8. Radiography has an appeal for the customer. The inspection department is generally the liaison between foundry and customer, and radiography aids inspection in presenting its problems to a customer. For example radiography may be used to explain why a particular casting design will lead to a shrinkage cavity or other type of defect.

When such a problem arises, it is possible for the foundry to show a radiograph of the casting to the customer immediately after pouring, before any work of cleaning is done. The customer then has an opportunity to decide whether that defect will be detrimental to his product or whether he wishes to pay an additional cost to remove the defect in question or to alter his design to prevent it.

Although in recent years, much information has been published on this subject, a review of the more important factors involved in radiography is worthwhile.

Equipment. The development of radiographic equipment has progressed rapidly, during recent years, making it possible to radiograph all types of materials in different thicknesses. Generally, a foundry can choose radiographic equipment which is suitable for products now in production and products which may be produced at some future date.

At the present time, it is possible to secure a variety of equipment classified generally according to operating voltages. Typical classifications are:

For light magnesium and aluminum, 3,000 to 50,000 volts.

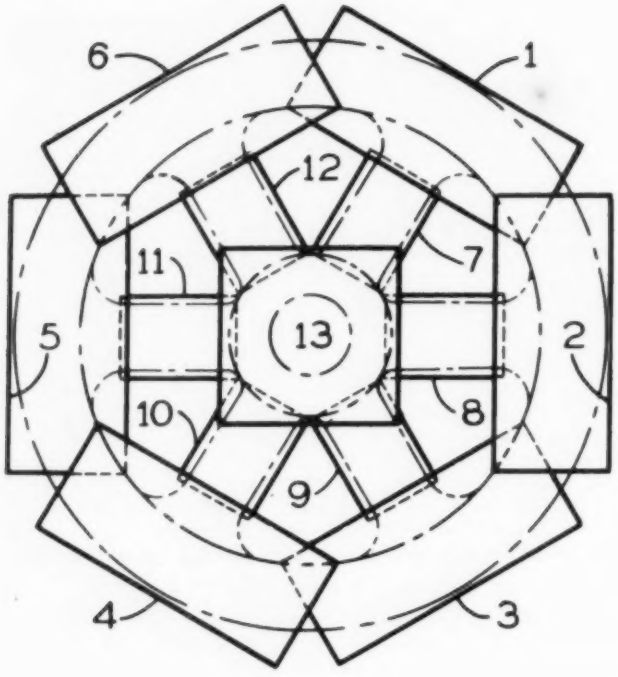
For heavier sections of magnesium and aluminum, 30,000 to 150,000 volts.

For light steel sections up to 2 in. in thickness, 60,000 to 250,000 volts.

For steel up to 5 in. in thickness, 1,000,000 volts.

For steel up to 8 in. in thickness, 2,000,000 volts.

* Research Metallurgist, American Steel Foundries, Research Laboratory, East Chicago, Ind.



ROUTINE POSITIONS	STANDARD
1, 4, 13	II
RANDOM POSITIONS	
2, 3, 5, 6	II
7, 8, 9, 10, 11, 12	III

NO. OF POSITIONS	POSITION NUMBERS	FILM SIZE
6	1,2,3,4,5,6	7 X 17
6	7,8,9,10,11,12	5 X 7
1	13	11 X 11

Fig. 1—Radiographic position chart, indicating areas to be tested; applicable standards for such positions, which have been established through agreement of all parties concerned with inspection procedure, and sizes of film recommended in the case of each position.

Fig. 2—Radiograph showing indications of gas porosity, which appear as round or elongated, smooth-edged dark spots which may appear individually, distributed through the casting, or in clusters. Such defects are generally attributed to imperfectly deoxidized metal, gas formed when moisture or volatile material on mold surfaces comes in contact with hot metal, entrapment of air in mold cavity, or excessive gas from improperly baked cores.

In addition to this equipment, there is the betatron, which has not yet come into common use. The betatron can be used for x-raying thicker sections than are possible with the highest voltage x-ray units or the largest practical amounts of radium.

Radium may be rented or purchased in amounts convenient for the examination of medium sections of steel and copper-base alloys.

Fluoroscopy, for examination of light metal castings and for exploratory examinations of thin section ferrous castings, is being used increasingly in cases where visual examination is sufficient and a permanent record is not required, and where the lower sensitivity of fluoroscopic images is not a disadvantage.

Methods of Application. An inspection department is concerned primarily with interpretation and is not interested to a large extent in the mechanics of producing radiographs. Nevertheless, certain elements of radiographic technique must be agreed upon between the inspection department and the customer.

First, it is essential that all know which area of a particular casting is being discussed at any given time. This can be accomplished by means of a sketch or drawing called a radiographic position chart. This position chart should include:

1. Radiographic areas, properly numbered and properly shown in respect to all other areas, and the correct size of film.
2. Direction of radiation.
3. Applicable standard agreed upon by all interested parties. Figure 1 shows a typical radiographic position chart.

Second, it is essential that all parties have the same concept as to the type of defects or discontinuities shown on a radiograph. It is helpful, when discussing

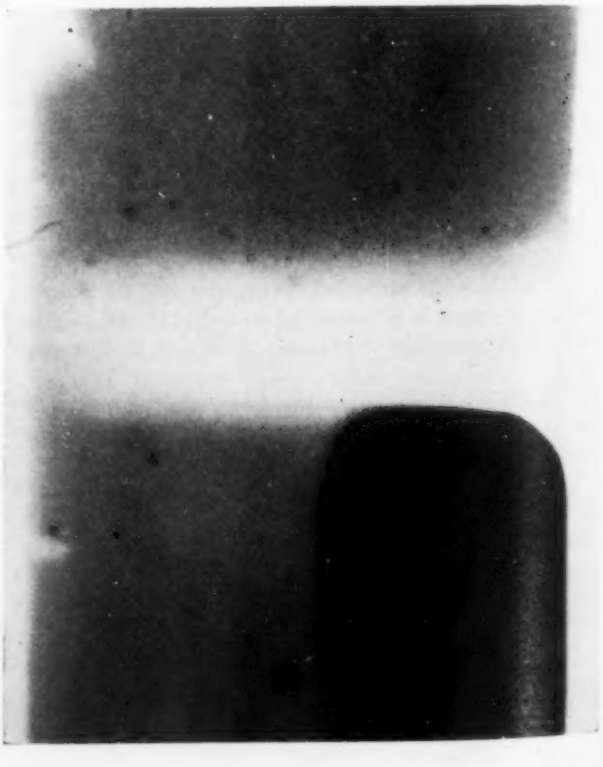




Fig. 3—Indications of foreign material in a radiograph. Isolated irregular or elongated dark areas, not corresponding to variations in thickness of material nor to cavities, may be observed on the film when particles of sand, refractory material, slag or oxides trapped in the metal during pouring, are present in the casting.

a casting, to have that casting on hand so that surface markings and discontinuities can be identified on the radiograph. In reading radiographs, only those defects which are internal should be considered. The common defects with their probable causes are enumerated below.

Gas Holes or Porosity. As shown in Fig. 2, gas holes appear as round or elongated, smooth-edged dark spots. These may occur individually, in clusters or distributed through the casting. This condition may be the result of imperfectly deoxidized metal, of gas formed by the vaporization of moisture or volatile material from the mold surface, of excessive gas from a core because of insufficient baking or venting, or of the entrapment of air in the cope surface of a mold cavity.

Foreign Materials or Inclusions. These appear as isolated irregular or elongated dark areas, as shown in Fig. 3, not corresponding to variations in thickness of



Fig. 4 (above)—Exposure taken through section of a casting containing shrinkage areas. These appear as dendritic, filamentary or jagged dark lines on the film, are generally found at the center line of a section or at a junction of thick and thin sections.

Fig. 5—Hot tears appear as ragged dark lines of irregular width, as seen in this radiograph. They may start at the surface or be wholly internal, may occur in groups, although with no definite line of continuity.



material nor to cavities. This condition is caused by particles of sand from molds or cores, entrapped slag or refractory materials, or oxides trapped in the metal during the pouring operations.

Shrinkage. Shrinkage appears as a dendritic, filamentary or jagged darkened area on the film, as in Fig. 4. This condition indicates insufficient compensation for liquid shrinkage and is generally found at the center line of a section, under improperly gated or shaped risers, or at the junction of thick and thin sections.

Hot Tears. These appear as ragged dark lines of variable widths as illustrated in Fig. 5. They may

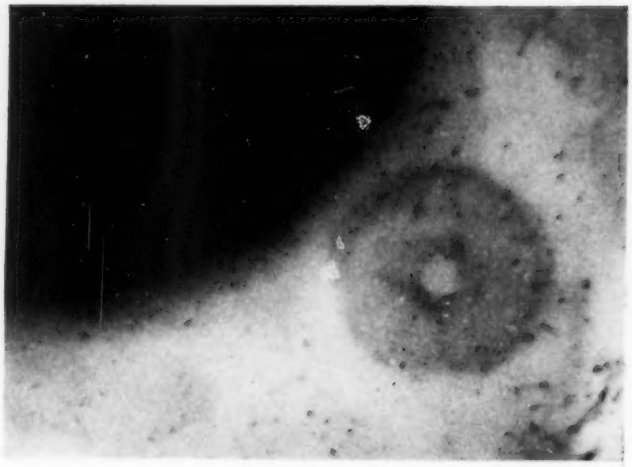


Fig. 6—An unfused chaplet may be clearly seen in this radiograph. Generally, chaplets which fail to fuse with the metal are outlined by a dark line. Note, also, that this exposure reveals numerous small gas holes.

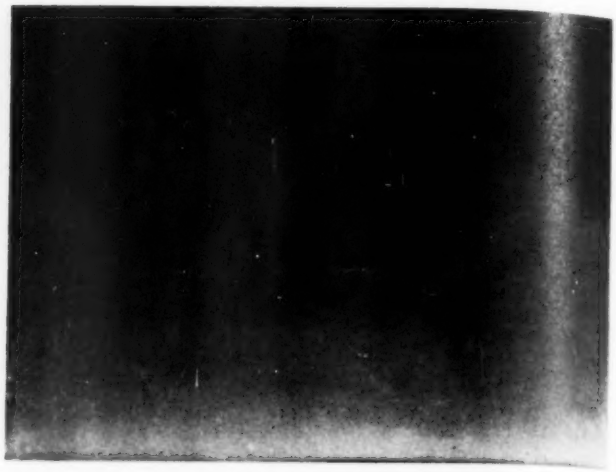


Fig. 7—Unfused internal chills often appear, as in this case, in their original shape, outlined by a dark line. Cold metal or oversized chaplets or chills are generally considered responsible for such lack of fusion.

have numerous branches, but have no definite line of continuity although hot tears may exist in groups. They may start at the surface or be wholly internal.

Hot tears result when the normal contraction of the casting is unduly restricted by the mold and/or core during or immediately after solidification. Under these conditions, the magnitude of the restricting forces may be greater than the strength of the metal, which is low during and immediately after solidification. Improperly designed castings are subject to hot tearing.

Unfused Chaplets. Chaplets which fail to fuse with the casting are generally outlined wholly or in part by a dark line. In some cases, as in Fig. 6, the chaplet may be readily identified. Figure 6 also shows numerous small gas holes, some of which are associated with small shrinkage defects. The irregular black area may be an inclusion or a surface blemish.

Unfused Chills. These often appear in their original shape as a dark area or outlined by a dark line. This is shown in Fig. 7. Unfused chaplets and internal chills are the result of cold metal or oversized chaplets or chills.

New Uses of Radiography in the Foundry. The field of radiography has not been entirely investigated and many new uses no doubt will be discovered as better equipment becomes available and its applications are further explored. Two applications which have been explored partially, but are not, as yet, in general use in the foundry are:

1. The examination of complex mold assemblies to ascertain whether or not they have been fitted and assembled properly.
2. The study of the flow of metals in molds.

The latter is purely a research application, but the examination of complex mold assemblies falls logically into the field of process inspection by radiography. This and other new uses for the x-ray, as well as the applications enumerated earlier, can be expected to increase the value of radiography to the castings industry through more thorough inspection.

DISCUSSION

Chairman: H. YOUNGKRANTZ, Apex Smelting Co., Chicago
Co-Chairman: H. C. STONE, Belle City Malleable Iron Co., Racine, Wis.

C. L. FREAR[1]: Some of you who have worked on Navy castings requiring radiography will agree that we are badly in need of standard interpretation of radiographs. I am going to appeal for help here to try to work out some method of obtaining uniform interpretation in order that, wherever made and inspected, the defects in the casting would be identified the same, the same reason for rejection given, and the same process of repair authorized. At present we are far from having that standard interpretation.

MR. LAGRELIUS: During the war, radiography was required by both the Ordnance and the Navy Departments. Considerable controversy was noted between local inspectors and producing units. Because of this variance of interpretation, radiography was dispensed with for a short time.

But, when it became evident that radiography would be a requirement in new specifications, our company formed an interplant radiographic committee. This committee was primarily for the education of our own employees, but in my opinion the same procedure could be carried out between a producer and the Bureau of Ships.

The first work done by the committee was to select typical defect standards (shrinkage, gas holes, tears, etc.) noting which defects had caused failures under service conditions or failures during static or dynamic tests.

The committee next had to recognize that certain areas of a casting were more highly stressed requiring better solidity than areas requiring lower stresses or loads. Following this procedure castings were zoned; zone 1 for most highly stressed areas; zone 2 for less highly stressed areas, etc.

You will note that we are not endeavoring to produce a casting free of defects but a casting which will perform satisfactorily and which can be produced economically.

Also, during the early part of this committee work, we noted considerable misinterpretation which was due to improper film density. We had also noted this during the war, where local Navy inspectors requested very light radiographs. Some having H&D densities of less than 1.00. We have now standardized on an H&D density of 1.75, and would probably use a higher density if satisfactory viewers were available.

MR. FREAR: You will find that the next specification to be issued will specify a density of approximately 1.50.

MR. LAGRELIUS: I worked with the Navy quite a bit during the war and there seemed to be a considerable difference be-

[1] U. S. Navy Dept., Bureau of Ships, Washington, D. C.

tween the Navy Yards themselves and the inspectors in the field.

MR. FREAR: That is why I am asking for a standard interpretation.

MR. LaGRELIUS: We must first start with a standard film density.

MR. FREAR: That is one of the things we are doing. Another much more important thing we have to do is to train the inspectors to recognize the defects that they are looking at, which is not the case now. In different districts the inspectors will interpret the same discontinuity as different types of defects which makes the use of standards useless.

MR. LaGRELIUS: That would not be the case if they had standard radiographs to compare them with.

MR. FREAR: I think we have some good standards.

MR. LaGRELIUS: I have not seen your latest ones but your first ones were built around high-pressure castings.

MR. FREAR: Our present standards cover all castings used by the Bureau of Ships. This includes high pressure and structural castings of all sizes.

MR. LaGRELIUS: The number of standards can become so large that the inspector could not leaf through them and tell which type of defect standard is applicable. The main point is to interpret a film for the major defect, whether it is shrinkage, gas, foreign material, etc. You can interpret the minor defects later and add them together and find out whether the area is serviceable or not. This may not be the answer to get all to read them alike, but I believe that if we could make up standards of a small enough number that inspectors could memorize them, you might arrive at a more definite result.

MEMBER: What type of casting is this mostly recommended for? We are in malleable iron casting production.

MR. LaGRELIUS: It can be used with any type casting. Just what defect standards you would require for malleable castings would have to be determined by either static or service tests. Radiography can be used with any metallic materials, whether it is aluminum or cast iron. We have X-rayed cast iron and it has been very informative. It all gets right back to how sound a casting is needed. For some particular job in which the casting is going to be put under certain service conditions, that soundness may be entirely different than the rough casting that is going to be used as a plate where no stresses are applied.

On castings that have to be machined, radiography will disclose where you are having cavities, so that you can either change your heading and gating and put the cavity some place else or eliminate it altogether.

Years ago we used to section castings into 1-in. and ½-in. pieces and maybe even criss-cross and, lo and behold, we would miss the defect that we were trying to eliminate. We would think we had a sound casting and send it out to a producer or a manufacturer and he would find the cavity in his machining process. We had missed it in sectioning. With radiography as one of the inspection tools, especially if it is an internal defect, it would have been located. You can take radiographs of an entire casting and locate all internal defects.

As to the sensitivity and how small a defect you can locate, you can obtain a sensitivity of 2 per cent. Some say 1 per cent, but I still think 2 per cent.

MR. FREAR: I would not guarantee better than 3 or 4 per cent in most cases with radium, and not better than 2 per cent with X-ray, although you can get down to 1 or 1½ per cent with X-ray if you have the right kind of X-ray equipment and use the right technique.

MR. LaGRELIUS: Two per cent is about the ultimate for a tool that you can use every day.

MR. FREAR: Yes, and for gamma rays it would be about 3 per cent. Although the specifications call for two per cent, I do not think you can guarantee better than 3 per cent.

W. E. THOMAS[2]: I would like to bring up the matter of interpretation vs. evaluation. The two are very different. You have a problem of interpreting what you see on the film to determine whether the indication on the film is of a crack, shrink, inclusion or whatnot. You have a separate problem, then, in evaluating the effect of that discontinuity on the serviceability of the casting. Are you looking for standards of interpretation of radiographic films or evaluation of the effect of various discontinuities on the service life of the particular casting?

[2] Magnaflux Corp., Chicago

MR. FREAR: We are looking for both, although the standards really state the evaluation.

MR. THOMAS: There are two specific problems, both of which must be solved. It seems to me that you have a much bigger problem in your large field force than Mr. LaGrelius has with his 20 men in teaching first the interpretation of the radiographs. Is Mr. LaGrelius teaching both interpretation and evaluation?

MR. LaGRELIUS: The metallurgical department teaches only interpretation. Evaluation is done by the engineering or design department. They tell us how much a section can be reduced and still meet service stresses. The Navy Department does the same thing, I believe. They have two separate departments, one that teaches the type of defect that may be found in castings, and another that tells how much of that defect can be tolerated. Interpretation is easy. You can teach an intelligent man or woman whether an internal defect is shrinkage, gas, a hot tear, crack, cold crack, etc. But how certain defects will affect the serviceability of the casting must be first determined by static or service tests. The producer must instruct his inspectors as to purchaser's requirements. What does he want? He says a sound casting. If he means SOUND casting, then, in my opinion, he does not want a casting. If he says a sound casting for a specific use then you can arrive at what he wants by a certain amount of testing and then set up definite specifications. All of our standards are set up on 5 x 7 areas. I do not know whether that will work for Navy castings, but it works for railroad castings, as far as testing is concerned.

MR. THOMAS: You are doing well to get into evaluation. It seems to me that is primarily the purchaser's job.

MR. LaGRELIUS: Have any of you tried to draw a line between interpretation of a specific defect and how much could be had in a specific area for certain service conditions?

CO-CHAIRMAN STONE: We have considerable experience in X-raying malleable castings. In our plant the foundry in setting up on a new job has a definite procedure to follow. The job is gated and made in the fashion that it is going to be made in production, i.e., if it is made on a squeezer machine, the sample castings are made on a squeezer machine. After the foundry is satisfied that a sample casting is sound on breaking it up in the hard iron state, a sample is also X-rayed. That casting cannot be put in production until it is passed on by the X-ray laboratory. The final X-rays are passed on by the foundry superintendent and in many cases we find shrinks by means of X-ray where they were entirely missed by means of breaking up the hard iron castings.

Generally the problem is in heavier sections such as bosses. It is easy to break a spoke off of a hub, for example, and assume there is no shrink in the hub, but by going through the hub, we often find shrink in the casting.

It is surprising what we can show in malleable. We have had cases where the hard iron fracture will look brown but there is no actual open shrink. Sometimes you cannot account for a shrink in a soft iron casting because you did not see it in the hard iron. Yet that same shrinkage had occurred in the hard iron casting as a brown spot, an open shrink. It looks much worse in the soft iron casting. The X-ray can pick out that immediately and you can avoid that condition. As a safeguard to that, we start no production jobs whatsoever without the X-ray check, and that has proved invaluable to us.

MEMBER: We do all we can to make a new job as sound as possible. Then we take it to the buyer and instead of just machining it as a regular casting, we insist on cutting it down in our own machine shop. We have success with that procedure.

We have never X-rayed any castings. We have had no occasion to do it. We have worked on war jobs and have had no trouble. Perhaps the jobs we worked on were not as critical as those other people made. Our procedure is the old method. Here and there we got a job that we did not do so well with for a while, but we finally got into stride.

MR. LaGRELIUS: During the time you were machining that casting to find the defects, if you had some method of X-raying it, you would have found any defects in it in less than an hour and could have proceeded. In my opinion it is impossible to machine some castings.

R. A. LODER[3]: By the use of X-ray we were able to inspect

[3] Erie Malleable Iron Co., Erie, Pa.

castings in an hour during the war. We had to take the castings to a nearby plant that had X-ray equipment to have the X-ray work done since we did not have our own.

Some shrinkage spots in white iron do not have a tarnish color. Under the microscope, however, we find a dendritic structure. The X-ray will show that but fracturing the hard iron does not always reveal it.

You would not recommend X-ray inspection strictly as an inspection tool but as a development tool. Is that right?

MR. LAGRELIUS: One hundred per cent inspection is ridiculous.

MR. LODER: If you were making small parts requiring 100 per cent inspection it would take a long time to produce those parts. That is why I asked the question. If the people who have developed the X-ray would give us something similar to a fluoroscope where we could pass and inspect castings under the viewer as they go through, then you would have a rapid 100 per cent inspection.

MR. LAGRELIUS: So far, that has to be on very small castings, aluminum, magnesium or thin steel castings. To me, radiography is one of the best tools to develop a new casting procedure; to know where to put your gates and risers. I am not an advocate of 100 per cent inspection or using radiography entirely as an inspection tool. To me, it is a development tool, and once you develop a procedure and spot-check castings you can produce castings of similar nature which are acceptable for service conditions indefinitely, unless you change foundry practice in some manner.

NEW TENTATIVE STANDARDS FOR GRADING AND FINENESS OF SANDS

By R. E. Morey, Metallurgist

Naval Research Laboratory
Washington, D. C.

ABSTRACT

Accurate methods of measuring fineness and of grading mold materials are becoming increasingly important because of the trend toward finer molding materials. Fineness may be determined for particles too small to be screened by means of a hydrometer. Under readily standardized conditions the operation is simple and rapid. The hydrometer test data combined with the information obtained by sieving the coarser material gives complete information on particle size distribution. In this way size distribuion for particles from less than one micron to the coarsest material normally considered as part of a molding sand, may be measured.

When plotted on semi-log paper, these data form a smooth curve which permits exact comparisons among mold materials. A cumulative curve also may be drawn from data obtained using screens only. In either case, size distribution can be determined by inspection, and an index to grain distribution can be found by means of a simple calculation.

Methods of grading and of determini g the fineness of foundry sands have never been entirely satisfactory. To improve present methods and discover new methods is the goal of the Grading and Fineness Committee of the AFA Sand Division.

Recently this committee recommended that the hydrometer method of determining fineness and the cumulative curve method of grading be adopted by the Association as tentative standards. The Executive Committee of the Sand Division has approved this recommendation.

The discussions of the two new tentative standards cover the theory and the procedure for making the hydrometer test and the theory and application of the cumulative curve. They were prepared by R. E. Morey, Metallurgist, Naval Research Laboratory, chairman of the Grading and Fineness Committee. Other members of the committee are: B. H. Booth, Foundry Engineer, Carpenter Brothers, Milwaukee; A. I. Krynitsky, Chief of Experimental Foundry, National Bureau of Standards, Washington; Stanton Walker, Consulting Engineer, National Industrial Sand Association, Washington, D. C.; H. J. Williams, Sales Manager, New Jersey Silica Sand Co., Millville, N. J.; and E. C. Zirzow, Core Room Foreman, National Malleable & Steel Castings Co., Cleveland.

The HYDROMETER METHOD for Obtaining Size Distribution of Small Particles

THE HYDROMETER METHOD may be used for determining the size distribution of particles too small to be measured effectively by sieving. This includes material usually called silt and clay. The method depends on the fact that when the particles are suspended in a liquid they settle downward in accordance with Stokes' Law, and as a result the specific gravity of the suspension changes.

This change can be measured with a sensitive hydrometer. If one knows the specific gravity of the particles; the specific gravity, temperature and viscosity of the suspending liquid; the settling time, and the depth at which the hydrometer measurement is made, the effective diameter of the largest particles at that level may be determined by the formula:

$$d = \sqrt{\frac{30\,nL}{980(D_P - D_L)T}} \qquad (1)$$

where

- d = Effective diameter of the particles. In reality, this is the diameter of a sphere which would fall at the same rate as the particle of irregular shape.
- n = Viscosity coefficient of the suspending liquid in poises. For water at 19.4 C (67 F) it is 0.0102 poise.
- L = Effective depth, the distance from the surface of the suspension to the point where the specific gravity measurement is made.
- D_P = Specific gravity of the particle. Sand or silt averages about 2.65.
- D_L = Specific gravity of the suspending liquid at the temperature used. For water at 19.4 C (67 F) it is so close to 1.00 that this figure is used.
- T = Settling time. The elapsed time from the beginning of the test, when the sample was stirred to produce a uniform suspension, until the reading was taken.

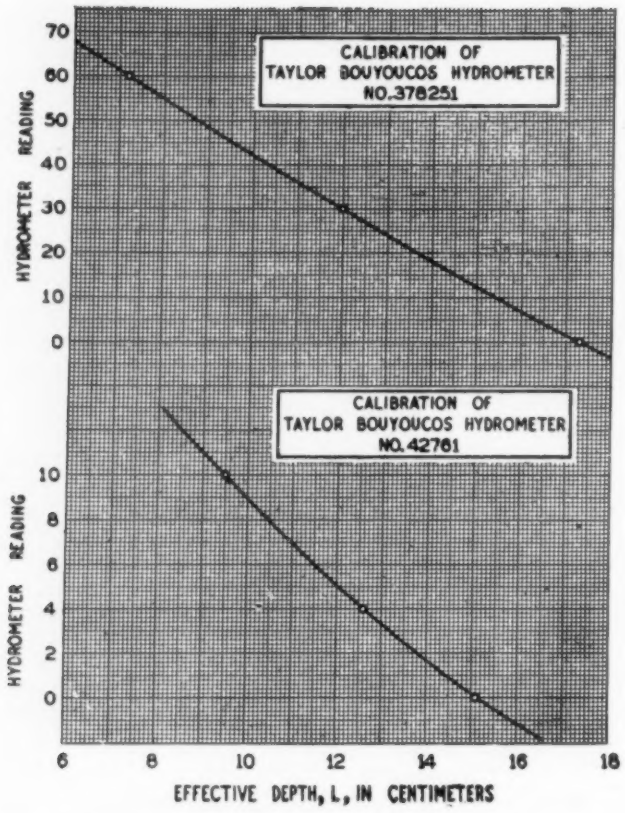


Fig. 1—Chart showing typical calibrations of hydrometers from which the "effective depth" can be determined readily for any hydrometer reading.

Effective Particle Size Determined

Solution of the above equation gives the effective diameter of the particle but does not tell how much is coarser or finer than this size. Size distribution measurements made with a sieve determine the amounts of material coarser or finer than the size of the sieve.

In the case of the hydrometer test, the particles still in suspension at the depth L are causing an increase in the density of the suspension and, according to Stokes' Law, are present in their original concentration. The Bouyoucos hydrometer, therefore, measures the amount of material finer than the size computed from the above formula. The hydrometer is calibrated in grams per liter so if a 50-gram sample was used in making one liter of suspension, the hydrometer shows the number of grams still in suspension at the time it is read. Multiplying this reading by two gives the percentage finer than the effective diameter (d) from the formula.

By the above method, particles of any density may be measured in any suitable liquid at any convenient temperature and depth. It is only necessary to substitute the proper values in the formula and solve for d. However, to simplify the process it is possible to control the temperature and to use water as the suspending liquid. In this case the viscosity becomes constant at 0.0102 poises.

Measurement of many samples of sand, silt and clay has indicated that they have an average specific gravity of about 2.65. For most work, this figure can be used but for very accurate work the specific gravity of the sample should be measured. The specific gravity of water at 67 F is slightly less than 1.000, but this figure is satisfactory in most cases. Substituting these quantities in the formula leaves only L and T, the effective depth and the settling time, as variables. These are the quantities measured in each test.

Hydrometer Must Be Graphed

The effective depth (L) from the surface of the suspension to the point at which the specific gravity measurement is made, is variable in the hydrometer method. The hydrometer bulb extends above and below some point from which the total volume of the bulb must be considered as acting.

This point is the "center of buoyancy" and unfortunately, as the specific gravity of the suspension changes, the level at which the hydrometer floats also changes and this changes the effective depth. It is, however, a simple process to plot a graph showing the relation between the hydrometer reading and the effective depth (L). This must be done for each hydrometer, since no two are exactly alike in shape and weight.

Graphing Method Detailed

To determine the points for such a graph, a graduated cylinder is filled about three-quarters full of water and the amount of water in milliliters is noted. The hydrometer then is immersed in the liquid and held steady by hand or in a suitable clamp at some reading,

TABLE 1.—SOLUTIONS OF FORMULA FOR VALUES OF L FROM 7 TO 18 CM.

L in cm.	T in min.	d in microns
7	1	36.4
7	100	3.64
7	10000	0.364
8	1	38.9
8	100	3.89
8	10000	0.389
9	1	41.3
9	100	4.13
9	10000	0.413
10	1	43.5
10	100	4.35
10	10000	0.435
11	1	45.6
11	100	4.56
11	10000	0.456
12	1	47.6
12	100	4.76
12	10000	0.476
13	1	49.6
13	100	4.96
13	10000	0.496
14	1	51.5
14	100	5.15
14	10000	0.515
15	1	53.3
15	100	5.33
15	10000	0.533
16	1	55.0
16	100	5.50
16	10000	0.550
17	1	56.7
17	100	5.67
17	10000	0.567
18	1	58.4
18	100	5.84
18	10000	0.584

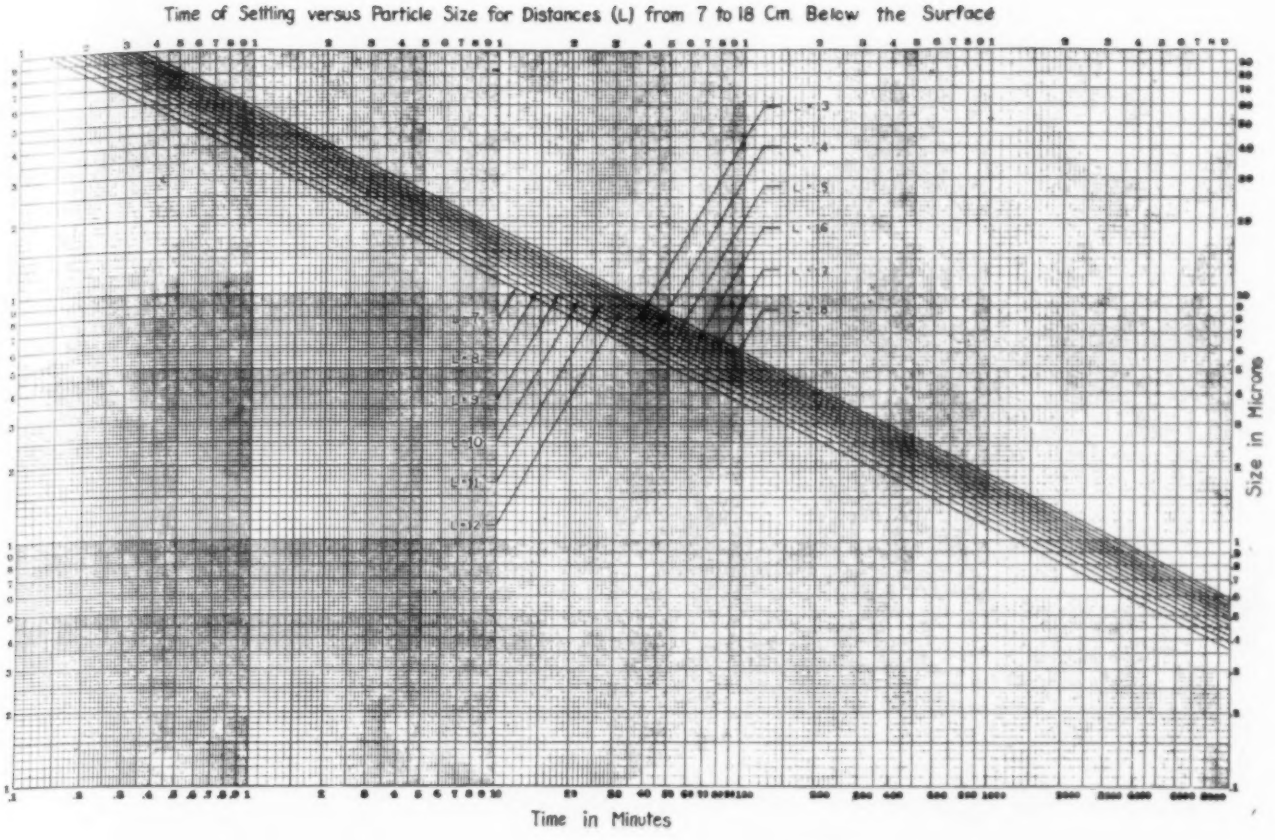


Fig. 2—Graph showing time of settling vs. particle size for various values of L (effective depth) from 7-18 cm. below surface.

such as 20 grams per liter. As it is inserted, the water level will rise in the graduate. The new reading on the graduate in milliliters should be noted.

Now, if the hydrometer is slowly withdrawn until the water level in the graduate has dropped halfway to its original level, the point which was the center of buoyancy during the second reading will be at the surface of the water. Holding the hydrometer steady at this point, measure the distance in centimeters from the water surface up to the 20-grams-per-liter mark on the stem of the hydrometer. This is the effective depth for a reading of 20 grams per liter on the hydrometer.

Repeating this process at a few other points such as 0, 40 and 60 grams per liter will provide sufficient data for plotting the relation. This graph should be kept as long as the hydrometer lasts and, whenever a reading is made, the corresponding effective depth can be found immediately. Such a graph is shown as Fig. 1.

In the formula (1), the variables can be reduced to three—d, L and T—enabling the plotting of a graph which will solve the equation for all values encountered in actual practice. This is done by assuming some value for L, assigning a value for T, and solving for d. For the conditions where $n=0.0102$, $L=7$ cm., $D_P=2.65$, and $D_L=1.00$, the formula becomes

$$d=\sqrt{\frac{30\,nL}{980(D_P-D_L)T}}=\frac{\sqrt{30\times.0102\times}}{\sqrt{980(2.65\text{-}1.00T7}}$$

$$d=\sqrt{\frac{.00132}{T}} \qquad (2)$$

If T = 1, d = 0.0364 mm. = 36.4 microns.
If T = 100, d = 0.00364 mm. = 3.64 microns.
If T = 1000, d = 0.000364 mm. = 0.364 microns.

If these points are plotted on logarithmic paper they form a straight line, as shown in Fig. 2. L then is increased to 8 cm. and the computation is repeated.

Table 1 shows solutions of the formula for values of L from 7 to 18 cm. inclusive, and times from 1 to 10000 min. Figure 2 shows a group of parallel lines for various values of L from 7 to 18 cm. inclusive. This of course applies only when the temperature is held at 19.4 C (67 F), with a water suspension of particles having a density of 2.65.

Equipment Required for Test

(1) Bouyoucos soil hydrometer. Two types are available, one with a range from 0 to 10 grams per liter which is satisfactory for most molding sands; and one with a range from 0 to 60 grams per liter, which is suitable for sands with high clay content and for other fine materials such as silica flour.

(2) Several 1-liter cylindrical graduates.

(3) One 10-milliliter graduate.

(4) Clock or watch with second hand.

(5) Tank for keeping samples at a constant temperature. A stirring motor and a thermostat with a heating element is desirable. Insulation around the tank helps also to keep the temperature constant.

(6) Rapid sand washer with adapter ring and baffles so that it may be used with 1-quart jars.

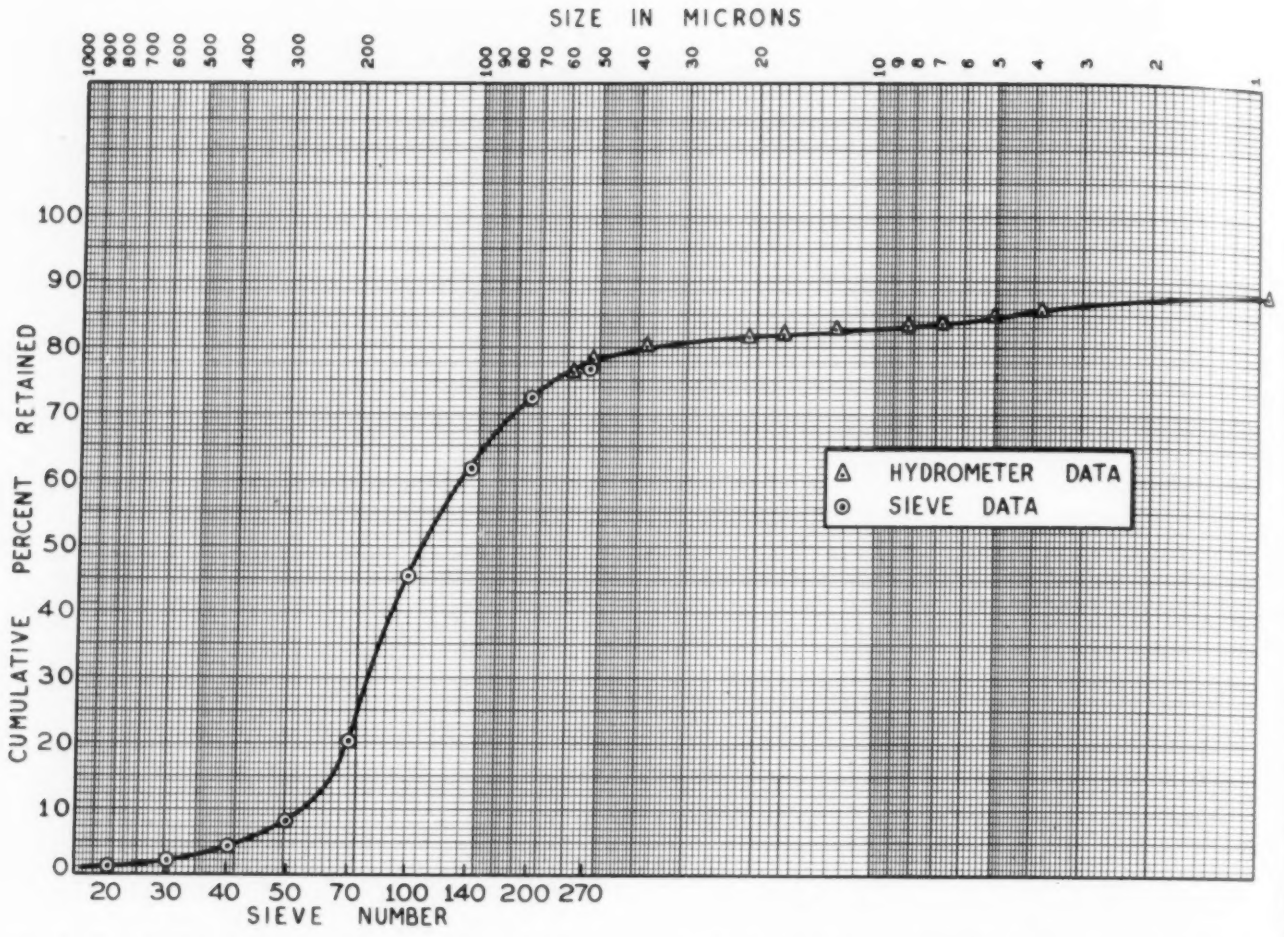


Fig. 3—Graph illustrating how both sieve and hydrometer data may be plotted as a cumulative curve.

(7) Wash bottle for transferring sample.
(8) Sieve, 8-in. diam. 325-mesh, half of full height.
(9) One-tenth normal sodium silicate solution.
(10) Distilled water.
(11) Oven.
(12) Sieves.
(13) Sieve shaker.
(14) Balance.

Directions for Performing Test

(1) Obtain a representative sample (about 60 grams) of the material to be tested, dry it in an oven at 105 to 110 C for 1 hour, then weigh out 50 grams.

(2) Place sample in a 1-quart jar. Add 5 ml of a normal solution of sodium silicate, made by dissolving 14.21 grams of sodium silicate, meta, ($Na_2 SiO_3 \cdot 9H_2O$) in 90 ml of water and then bringing the volume up to 100 ml at 20 C with distilled water. Add 500 ml of distilled water.

(3) Attach the rapid sand washer to the jar and stir for 5 min.

(4) Remove the washer, and wash the sand from the propellors and baffles into the quart jar, using a stream of distilled water from the wash bottle.

(5) Transfer the suspension from the jar to a 1-liter cylindrical graduate. Use a stream of water from the wash bottle to make sure that none of the sample is left in the jar. Add enough distilled water to bring the level in the graduate up to the 1-liter mark.

(6) Place the graduate in the water bath and bring the temperature to 19.4 C (67 F).

(7) When the suspension has come to temperature, remove the graduate and put the sand in uniform suspension by up-ending the graduate, using a stopper or holding one hand tightly over the open end. A plunger made from a perforated disc with a long handle has also been found satisfactory for getting the particles in uniform suspension.

(8) The test begins as soon as the stirring is finished. The time should be noted and the hydrometer inserted carefully. There is no need for making readings at any exact time so long as the time is recorded when the reading is made. However, it is convenient to make readings at 30 sec., 1 min, 2 min, 4 min, 8 min, 15 min, 30 min, 1 hr and 2 hr. With some materials, readings at 4 hr and 16 to 18 hr (overnight) may be desirable.

With each reading of the hydrometer, the elapsed time of settling should be recorded. The top of the meniscus on the hydrometer stem should be read and the stem should be kept clean so that the liquid will form a good meniscus. If a light is placed near eye level and the hydrometer is viewed from a low angle, a bright pin point of light will be seen on the stem of the hydrometer at the top of the meniscus, which makes it possible to read the instrument accurately.

(9) When the readings have been completed, transfer the suspension to the 325-mesh sieve and wash the

clay and fine silt through with a gentle stream of tap water. The sieve will retain the grains over 44 microns in diameter.

(10) Dry the sample by placing the sieve in an oven. Drying may be hastened by removing excess moisture with a soft towel or blotter placed beneath the sieve wire.

(11) Transfer the dried sample to a set of AFA sieves and sieve in the usual manner.

(12) The data for each reading will consist of a reading of the hydrometer and the elapsed time from the start of the test to the time reading was made. Use the hydrometer reading and a graph such as Fig. 1, to find the effective depth *(L)*, at which the reading was made. Using L and T, the time in minutes, find the effective particle diameter in microns from a graph such as Fig. 2.

(13) Multiply the hydrometer reading by 2 to convert to "per cent finer" than the size determined as above. Subtract the "per cent finer" from 100 per cent to find the "per cent coarser" than the size. "Per cent coarser" may be plotted on semi-log graph paper (Codex Book Co., Norwood, Mass., No. 3115, or 3215) versus the size in microns.

Fig. 4—A natural steel molding sand, an Albany sand and a silica flour plotted to show hydrometer and sieve data.

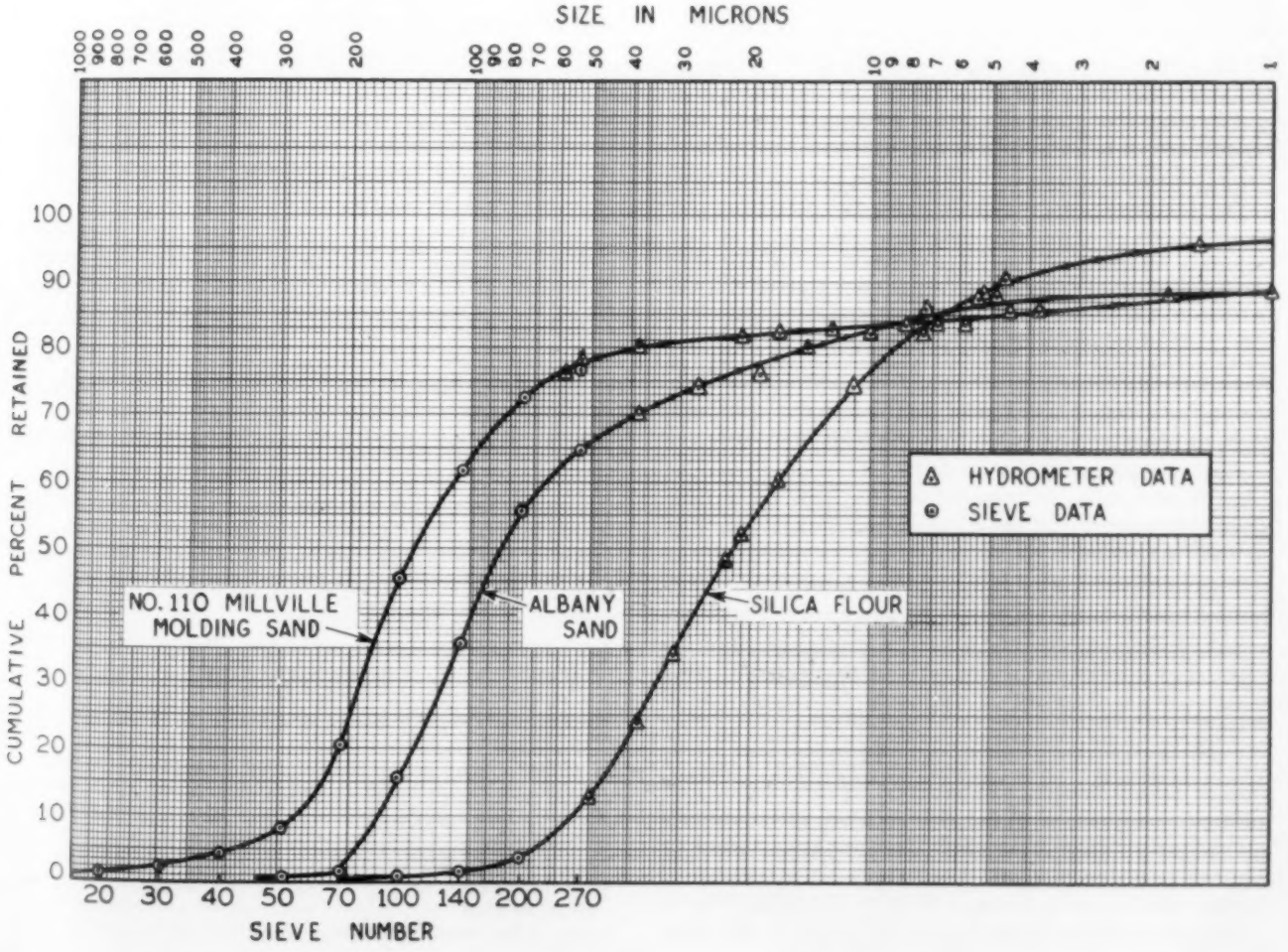
(14) In weighing the sand retained on the various sieves in the sieve test, use the cumulative method; that is, weight the amount on the coarsest sieve, then add the portion on the next sieve, weigh the total, and so on. An enameled pan slightly larger than the sieve is convenient for emptying the sieve. The sieve is inverted over the pan and the wire is then brushed with a 1-in. varnish brush, to remove the loose grains.

(15) When the weighing is completed, multiply each cumulative weight by 2 to convert to cumulative per cents retained, and plot them on the same graph with the hydrometer data. Figure 3 shows a graph with both sieve and hydrometer plotted as a cumulative curve.

CUMULATIVE CURVE for Indicating Size and Distribution of Foundry Sands

The usual way to obtain data on the size of sands is to pass a representative sample through a series of sieves stacked with the largest sieve at the top and progressing down to the smallest sieve at the bottom. A pan at the bottom of the stack catches the material which passes the finest sieve, and a cover over the top sieve keeps particles from escaping.

The number of sieves may be small or large and the ratio between the openings of consecutive sieves may or may not be the same. In the AFA sieve series the ratio



is not the same throughout the series. It will readily be seen, therefore, that the amount of material passing one sieve and retained on the sieve below, has, in itself, little importance. The amount depends on the arbitrary selection of the sieves as well as the material being tested.

Usual Method of Sieve Analysis

A more satisfactory concept of the process may be obtained by considering each sieve separately. When a sample is placed in a sieve and shaken sufficiently, some grains fall through the square openings, while others do not. Those which fall through have a minimum projected area smaller than the square opening in the sieve. Shaking must be continued until the grains near the critical size have several chances to pass an opening by falling on the opening in different positions. When sieving is complete, the sample has been separated into one part which is coarser than the sieve and one part finer than the sieve.

If the weight of sand coarser than the sieve is computed as per cent retained by that sieve, the two fractions may then be mixed and divided by another sieve with a different size opening. In this way a sieve analysis may be made, sieve by sieve, and the results with each sieve are seen to be independent of the size, spacing or selection of the other sieves. The per cent retained by each sieve may be plotted as a function of the size of the openings in that sieve.

To save time, however, sieve analyses usually are made as stated above by means of a set of nesting sieves, the weight of material passing one sieve and retained on the next finer sieve being reported. Usually an effort is made to present this information in the form of a graph which is generally unsatisfactory because the ratio between sieve sizes was not uniform throughout the sieves used.

Satisfactory results may be obtained by weighing the amount retained on any sieve and adding to it the amounts retained on all the coarser sieves. In this way the total amount coarser than the sieve may be found.

Outline of Cumulative Method

Another method which is even more satisfactory is to weigh the amount of material on the coarsest sieve and then to place the material from the next finer sieve on the balance pan with the coarse material and weigh the total. This total is the amount coarser than the second sieve. The material on the third sieve is then added, and so on until the analysis is completed.

This method is called cumulative weighing and, when reduced to percentages, gives the cumulative per cents retained by the various sieves. These data may be plotted as a function of the sieve opening in microns, which usually is found on the nameplate of the sieve.

The use of many types of graph paper over a period of years has indicated that the most satisfactory type is one in which the cumulative per cents retained are plotted as a function of the size of the sieve in microns on a logarithmic or ratio scale. Most materials found in nature, as well as many of those which are processed, as in the washing and sorting of foundry sands, have their particles distributed over such a wide range of sizes that two or three cycles along the logarithmic scale are required to make a graph of the major part of the material.

Figure 4 shows a natural steel molding sand, an Albany sand and a silica flour plotted on a chart paper (No. 3115 or No. 3125, made by the Codex Book Co.). This paper has 20 divisions per inch (120 divisions) along one axis, and three 3-in. cycles of ratio or logarithmic ruling along the other axis.

Advantages of Cumulative Method

The cumulative method of plotting has several advantages over the methods previously used:

(1) It may be drawn as a smooth curve instead of a broken line.

(2) Sieves can be used where they are necessary. That is, sieves may be omitted from the series where only small amounts of material are retained, and extra sieves may be added where the material is concentrated. These omissions and additions serve to eliminate useless work and to increase the accuracy of the analysis.

(3) Faulty sieves are indicated by breaks in the curve which appear at the same sieve in more than one analysis.

(4) The cumulative curve permits the use of certain parameters as a kind of shorthand description of the size distribution. For instance, the point at which the curve crosses 50 per cent retained is called the median size and can be determined by inspection of the curve. It is a reliable measure of sand size and does not involve any calculation.

An index to the sorting or distribution of the sand may be obtained from the formula for Sorting Coefficient:

$$S_O = \sqrt{\frac{Q_1}{Q_3}}$$

Where Q_1 is the particle size at which the curve crosses the 25 per cent retained line, and Q_3 is the particle size at which the curve crosses the 75 per cent retained line.

(5) Exact comparisons may be made by superimposing two or more curves.

(6) They may be used for specification testing by having the specification limits plotted as two curves on a piece of graph paper. The sand to be checked will fall between the two curves if it satisfies the specification.

(7) Size data for silt and clay particles may be obtained with the hydrometer or other methods and plotted on the same graph to supplement the sieve data. This is important because the present trend is toward the use of fine molding materials.

DISCUSSION

Chairman: G. R. GARDNER, Aluminum Co. of America, Cleveland.

Co-Chairman: K. J. JACOBSON, Griffin Wheel Co., Chicago.

MEMBER: Why do you go only up to about 90 per cent retained (Fig. 3 and 4)?

MR. MOREY: Simply because these measurements are carried only down to one micron. Ten per cent of this sample was finer than one micron, in the true clay range. Actually, the present A.F.A. 20 micron clay point is much coarser than the size of true clay. Other organizations have gone as low as one micron in de-

fining their clay points. Some of them use two microns. One or two use four, and there is one that uses five. The size used by the American Foundrymen's Association is much larger than that used by any of those other organizations. A sample, for instance, might have about two or three per cent clay, and the 20 micron material might be 15 per cent. That would indicate that we had 12 per cent of fine silt which would be inactive as a bond but would fill the pore spaces in the sand.

MEMBER: In testing silica flour, you refer to material of one micron in size as clay, and refer to the coarser particles as being silt. In the grinding of silica flour, we have some one micron particles of pure silica floating in the air. That actually would show up as clay, whereas actually it would be silt in your sand, would it not?

MR. MOREY: It depends on how we define clay. It is unfortunate that clay has two types of definition. One is the definition by size. If we define it by size only, then the fine one micron material would be defined as clay. That is the minus one micron material. If we add to that definition the fact that it is plastic when wet, then the silica particles of one micron and less would not be clay.

MEMBER: This test does not indicate whether it is true clay or whether it is very fine silt.

MR. MOREY: That is right. It just gives us a particle size and enables us to find out something of the surface area of that material. If we want to find out whether it is true clay or silica particles of small size, then we have to employ petrographic analysis or X-ray analysis.

GEORGE WATSON [1]: We worked with this test and found with regard to clays and natural sands that if you plot the percentages below about two microns against optimum strength you can accurately portray the bonding qualities of any material whose particle size is known. You can qualify it within one pound of compressive strength just from that one size.

We have also used this test for determining clay content in dust collector refuse. I would not know of any other method of determining how much clay would be present unless we had this type of test.

It can also be used in conjunction with the total carbon determination, to find out how much sea coal you might have in those materials. We also use it in testing new natural sand that we buy. It is the only way that a foundry can determine that each shipment of their natural sand is the same. If a pit runs out and you have to have a new source, it is the only way that you could be sure that your new source of sand has the same qualities as far as clay content, silt and the grain size as you have been using. It has worked out very nicely for us. Can you determine pitch content in sea coals?

MR. MOREY: We have not gone into that. Our work is mostly in steel molding sands, and it is seldom that we have occasion to determine either sea coal or pitch. We use some iron sand, but we have been using a synthetic type of sand with very little sea coal in it.

F. P. GOETTMAN [2]: This method cannot be used to determine minerological clay. In natural molding sands the material minus two microns will be largely clay, because any fine particles of silica of that size would tend to go into solution in the geologic history of the deposit. However, when you check dust collector materials and are using facing, silica flour, etc., some particles might be down to one micron. You cannot tell whether it is silica or bond or clay by this method. These materials will show up in this analysis, so you will have a heterogeneous mixture of material that is one micron and finer. In that material, you cannot say that the minus two micron fraction is clay. This method of particle size analysis can be used for pitch and graphite, if the deflocculent is changed. Sodium metasilicate is an excellent deflocculent for clay. It will not deflocculate graphite. If you want to analyze facings and other graphite materials some other deflocculent such as tannic acid must be used. For limestone, sodium silicate does not give very good results—sodium pyrophosphate is superior. I would like to ask Mr. Morey if he has determined that the 5 ml. of a normal solution of sodium silicate is the optimum deflocculent.

MR. MOREY: That electrolyte was taken from the work done by the Bureau of Public Roads. I have not done much experimenting with electrolytes. The sodium silicate seems to be satisfactory for most clays and most foundry sands.

MR. GOETTMAN: I mean as to quantity.

MR. MOREY: As to quantity, it might be necessary to change that in some cases. If the material all settles out of suspension and you get clear liquid on top, you are getting flocculation. Then you have to start experimenting to find a deflocculent which will hold the material in suspension. If you are comparing different deflocculents, you can use identical samples of the same clay and that deflocculent which gives you the highest percentage at any particular size, say ten microns, is the best for that sand. If one deflocculent gave you 15 per cent and another gave you 17 per cent, the one giving 17 per cent would be the most effective deflocculent.

When you put the hydrometer in the suspension, the particles tend to settle on the upper shoulder of the hydrometer, and after several readings have been taken the hydrometer should be rinsed in running water, and reinserted gently in the suspension. If material accumulates on the hydrometer, it will cause an error in the readings.

MEMBER: Have you made any tests on northeast Ohio sand or northwest Pennsylvania sand? It is very fine and silky, about 280 grain in size with 2.5 per cent A.F.A. clay, more or less.

MR. MOREY: I have tested some sand from the Zanesville area.

MEMBER: It would be interesting because that is silky, dusty sand and yet there is no clay in it that is real clay. It will have at least two per cent real clay, with little bond.

MR. MOREY: That is something we might have to go into—even finer particle sizes. If we could go down to half a micron with very long settling periods, we might pick up the last two per cent of that material and find that it was also silica.

R. C. WAYNE [3]: Do you think that there is a possibility of separating the fine silica fraction of the sand from the true clay fraction by means of a special flocculent? Could this separation be accomplished by a chemical precipitant? This seems to be a profitable field for further work.

MR. MOREY: I never heard of any method like that for the separation of clay and silica particles. The work of Grimm at the University of Illinois is the nearest thing that I know of that has been done on that. He separates his samples into size fractions in a centrifuge, so that he can obtain the one micron to one-half micron material, the one-half to one-quarter, and one-quarter to one-tenth micron. Then he analyzes those samples by X-ray diffraction and petrographic methods, and he can estimate the amount of true clay, the amount of silica, and the amount of any other materials that may be in the sand.

MR. WAYNE: That would not be of much practical use as a control test in the foundry.

MR. MOREY: It is not a foundry method. It is a very extended, complicated analysis.

DR. H. RIES [4]: Many people are interested in the sizing of grains. I am wondering, if you have silica flour of one or even two micron sized particles, whether this material would have any plasticity when it is wet.

MR. MOREY: In grinding silica flours, they grind very rapidly down to a size of 40 microns or so. Then, as the grind grows finer and finer, it takes a tremendous amount of additional work to effect a reduction of just a few microns. An ordinary 200-mesh silica flour is plotted in Fig. 4. That one indicates about three per cent of material smaller than one micron. I think that is the mesh most of us use for silica washing, about the 200 mesh. I would not expect silica flour of this size to have any plasticity when wet.

MEMBER: You speak of temperature in half degrees for your bath. Is it necessary to control it that closely, or are there correction tables which might be applied to this hydrometer?

MR. MOREY: There are correction tables which may be used. It adds to the complication of the test. In fact, if you go into it, there are four different correction factors which you pick out to about three or four places and multiply each of these correction factors by your test results. You correct for temperature, density, and other factors. That is in the original method that was worked out by the Bureau of Public Roads, published in *Public Roads Magazine* Oct., 1931, Aug., 1933 and March, 1939.

[1] American Brake Shoe Co., Mahwah, N.J.
[2] Geo. F. Pettinos, Inc., Philadelphia.
[3] Steel Founders' Society of America, Cleveland.
[4] Cornell University, Ithaca, N.Y.

MECHANIZED MALLEABLE FOUNDRY FINISHING AND INSPECTION

By
D. F. Sawtelle
Metallurgist
Malleable Iron Fittings Co.
Branford, Conn.

IN THE WRITER'S MECHANIZED FOUNDRY, molds which have been poured, are dumped on a metal tray-type conveyor which takes them to a vibratory shakeout table. Here the castings are separated from the core material and most of the molding sand into which they were poured.

The shakeout table can be considered a crude sprue removing and cleaning operation in addition to its main function of separating the castings from the molding sand, as some of the castings break off at the gates. All but a small percentage of sand is removed at this point of the operation.

Sprue Removal and Cleaning

Castings are transferred from shakeout by another metal apron-type conveyor to two tumbling barrels connected with each other in series. Many of the larger castings are still red hot when they leave the shakeout. In the loading cycle, the conveyor elevates the castings so that they fall into the first of these barrels by gravity. This barrel, which is half full of water at approximately the boiling point, rotates counterclockwise for six minutes at the speed of 18 rpm. During this time the castings are continuously elevated and tumbled upon each other in and out of hot water.

At the end of six minutes this barrel reverses its direction and rotates clockwise for a period of four minutes. During this time the castings are ejected into the second barrel. While the castings are being ejected, the conveyor from the shakeout doubles its speed in order to quickly reload the first barrel.

The second barrel, in reality a large helix to level out the batches to a continuous flow, rotates continuously in a clockwise direction at a speed of 9 rpm. It takes in castings every four minutes from the first barrel and gives them a further tumbling and rinsing as they pass through. The castings are ejected in a fairly continuous flow, onto a rubber belt conveyor, which carries them directly to the inspection room.

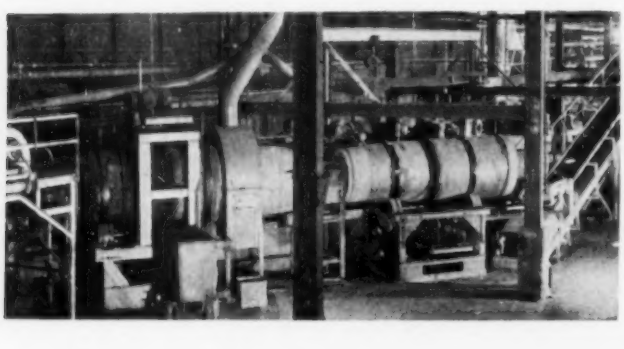
Figure 1 is a view of these tumbling barrels. At the extreme left can be seen an elevated platform and motor which is the end of the apron conveyor from the shakeout to the tumbling barrels. The castings travel through the barrels from left to right. At the end of the right hand or second barrel can be seen the rubber belt conveyor onto which the cleaned castings are ejected.

In Fig. 2, at left, is a casting with an average amount of sand still adhering to its surface, after it has passed over the shakeout table, but before it has passed through the tumbling barrels. Casting on the right shows the appearance after passing through the cleaning and spruing barrels. It may be noted that the sand nearly obscures the diamond shaped MIF trademark in the casting at left, while in the casting on the right it is plainly visible.

Preliminary Inspection

In passing through these tumbling barrels, the castings not only have had the sprue removed and have been cleaned well enough for inspection, but they have been cooled from nearly red-hot temperatures at which they entered the barrels, to around 120 F as they start their travel on the rubber belt conveyor to the inspection room. As they come from the barrel they are still wet but become dry after traveling only a short distance. The castings dry without rusting because a rust inhibiting compound is added to the water in the first barrel.

Figs. 1 and 2—Tumbling barrels and a typical casting.





A preliminary inspection is given the castings on the conveyor belt just as they come from the barrels. At this point, an inspector can detect trouble and bring it to the attention of the proper foreman soon after it makes its appearance. In this way defects can be discovered and corrected in current production. It is only 30 min. from the time the castings are poured until they reach this point. This is a great advantage over batch-type melting and floor molding practice where inspection takes place on the following day, when it often happens that the entire day's output of one or more molders must be scrapped.

From the barrels, the castings and sprue travel on the belt conveyor past the cupolas where the iron is melted. The first handling takes place at this point. The sprue is picked off the belt by 2 or 3 men, depending upon the volume of the day's melt which varies from 50 to 75 tons, and is tossed into a bin. From this bin or hopper it falls into the weighing buggy which makes up the cupola charges. The castings remain on the belt and proceed to the inspection and sorting room.

Figure 3 shows a view of the inspection room looking from the end at which the belt conveyor enters. The belt can be seen covered with castings at the extreme right in the foreground. It is clean before it has traveled half the length of the room. The girls at the right sort the different items into the pans as they come along on the belt. Some of the larger castings are also inspected at the same time. Directly underneath the belt and not visible in the photograph is a shelf which holds pans of work. Also, underneath the belt on the floor, and in back of the girls, are pans of sorted work on the floor.

The more difficult inspection jobs and castings which cannot be inspected as they are sorted, are inspected by the line of girls to the left working between the roller conveyor lines. This group of inspectors sounds many of the castings for cracks and uses chipping hammers, where advisable, to save finishing operations later on the annealed castings.

Steel Shot Cleaning

After inspection, the castings are weighed. The good castings pass in pans on roller conveyors to the annealing pot packing station, which is located just outside the inspection room. The emptied pans, after the castings have been loaded into pots for annealing, travel back to the inspection room by roller conveyor, entering to the right of the belt conveyor and running along the brick wall as can be seen in Fig. 3.

After annealing, the castings are given the final blast cleaning with steel shot in one of several types of machines. In these machines, the shot is impelled by centrifugal force onto the castings which are being tumbled about in a rotating barrel. The castings are transported from the annealing department in buckets suspended on an overhead monorail. These buckets hold all the castings contained in one stack of annealing pots. The load is transferred into skip hoists which in turn load the cleaning machines, one bucketful making one load.

Figure 4 shows the skip hoists, the cleaning machines, and between them a rubber belt conveyor, on



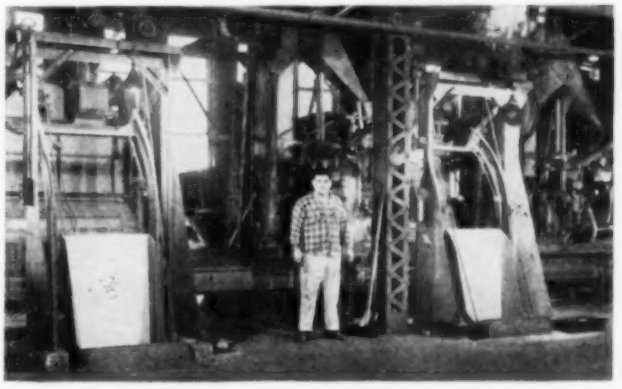


Fig. 3 (above)—View of inspection room. Fig. 4 (below)—Machines for final cleaning of castings after anneal.

which the castings are dumped after cleaning. They travel a short distance on this belt where they are given a final sorting, for they become mixed to a slight extent when they are packed in pots for annealing.

The castings are now ready for the removal of the gates. This is done by grinding or by shearing. The gates on over 60 per cent of the castings being made at the present time, are not ground, but removed by a shearing operation. Most of the grinding is done on double-ended stands. Each stand is equipped with a 10 hp motor with three changes of speeds. By use of these speed changes, the surface speed can be kept reasonably constant as the wheel wears down. A surface speed of 9,000 to 9,500 fpm can be maintained. The wheels as purchased are 24 in. in diameter, 3 in. wide, with a 12 in. center.

Grinding and Shearing Gates

Some of the larger castings are ground on a swing frame grinder using wheels of 20 x 2½ x 6 in. which also run at a surface speed of 9,000 ft. per min. Several portable, air-powered grinders which can be equipped with a wide variety of wheels shaped for getting into places not reached by the regular grinding wheels are also used. These air-powered grinders run at speeds of 6,000 to as high as 17,000 rpm, varying with the size of wheel with which they are equipped.

Figure 5 shows an operator at a machine used for shearing gates from the castings. This machine raises

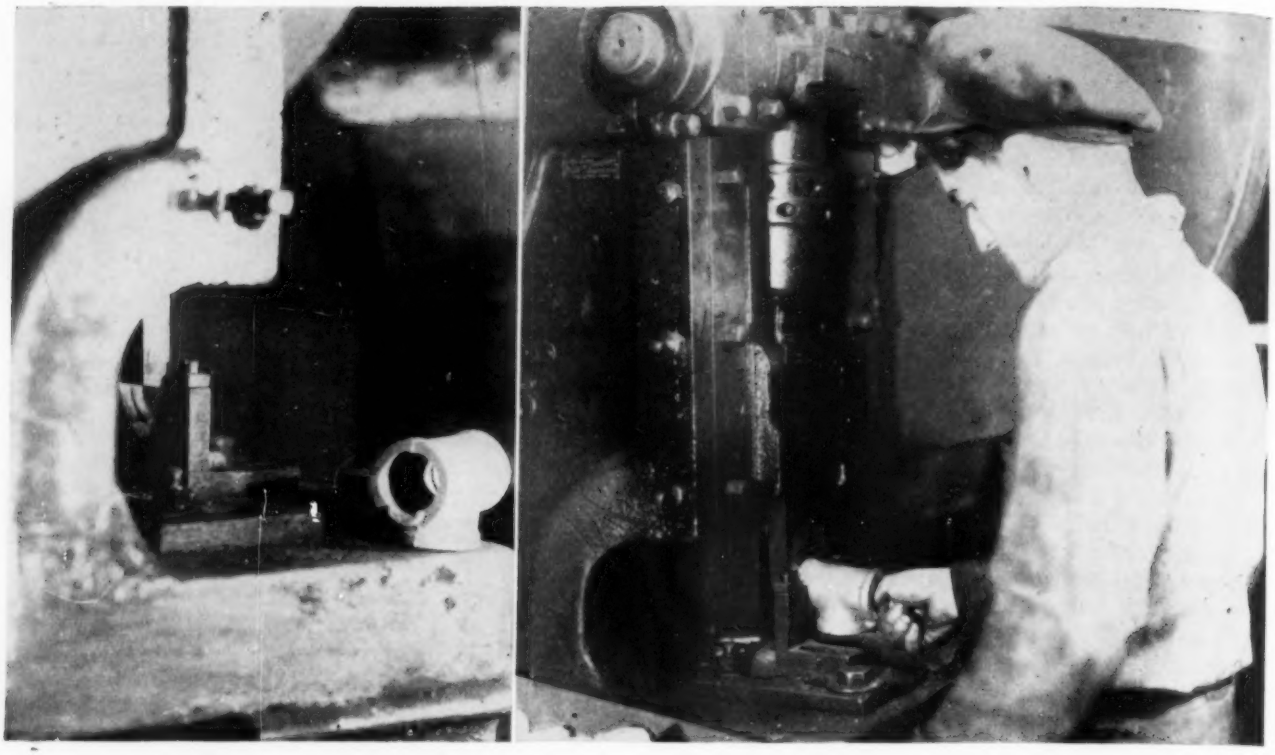




Fig. 5—Shearing machine (above) operation. Fig. 6—Close-up (left above) of shearing bed and blade. Fig. 7—Valve casting (left) before and after shearing.

and lowers a hardened steel blade a distance of ¾ in. at a rate of 80 strokes per min. The operator places the casting, gate down upon the semi-circular bed and up against the moving blade. As the blade descends, the protruding gate is evenly sheared off.

Figure 6 shows a rear view of the bed and the blade. The blade is made of a low-alloy tool steel of the following analysis: 0.90 per cent carbon, 0.50 per cent chromium, 1.25 per cent manganese and 0.50 per cent tungsten. These blades are hardened by quenching into oil at a temperature of 1475 F. The bed is also faced with a steel of the same composition and heat treatment.

The cutting edge of these blades is ground at an angle of three degrees. However, they are one and a half degrees from the perpendicular, therefore, the angle or rake of the cutting edge is only one and a half degrees. The blades and beds are made and heat-treated in the machine shop. The average blade life is one year. When new, they are 4½ in. long, and are discarded when they have been reduced by repeated sharpening to a length of 2½ in.

Figure 7 shows two castings, at left, before the gate has been sheared off and, at right, after shearing. In between the castings are shown a few of the pieces which are sheared off in this operation.

The beds and blades can be changed on any one machine in a few minutes to take castings of a different size or shape. In many cases, it is necessary to change only the bed. The speed of the blade can also be changed from 80 to 160 strokes per min., to suit conditions of the particular job. While Figs. 5 and 6 show a machine set up with a bed and blade for shearing, the same machine can be quickly fitted with a punch and die. This combination is also used in shearing the gates from some types of castings. The punches and dies are made of the same steel and given the same heat treatment as the blades and the steel with which the beds are faced.

The speed with which gates are sheared is two, three, or even four times faster than grinding, varying with different jobs. Very small gates can be ground nearly as fast as they can be sheared, though even the small light gates require more physical effort in grinding than in shearing. Gates are sheared off castings as large as 3 in. pipe fittings. The shearing operation on a casting of this size is four times as fast as grinding, and requires much less physical effort.

As to comparative costs, a blade, or a bed facing, or a punch and die combination, can be made in the machine shop, ready to be used, in about eight man-hours, and, as has been stated, the life averages about one year. In comparison a grinding wheel costs $44.00 at present, and its average life is only one week.

DISCUSSION

Chairman: J. A. DURR, Albion Malleable Iron Co., Albion, Mich.

MEMBER: What happens to the gates if they pull out?

MR. SAWTELLE: We stop the batch in which this occurs and re-anneal it. If you do not sharpen your blades enough, use the wrong kind of bed or if your machine has become loosened so that it does not cut closely, you will tear the gates out instead of shearing them.

MEMBER: Are the cutters driven mechanically or hydraulically?

MR. SAWTELLE: They are at present belt driven from one large motor. The speed can be regulated from 80 to 160 strokes per minute. The speed and dexterity with which an experienced operator performs the operation is amazing.

MEMBER: Are those castings considered ready for the finishing operation after the shearing, or are they salable castings?

MR. SAWTELLE: They are salable castings.

MEMBER: What machining operation does the casting receive at the location where the casting is gated?

MR. SAWTELLE: The machining operation is tapping. It also has a tapered thread with a slight bevel at the point where the casting had been gated.

MEMBER: Does that interfere with the gate?

MR. SAWTELLE: No. We have developed what is known as an internal gate which we are using on several different sizes of our fittings. It breaks off on the inner circumference which has to be tapped later. These we do not grind or shear at all. The tapping operations remove the marks of that broken-off gate.

MEMBER: What do you do with shifts and strained castings?

MR. SAWTELLE: Do you mean the castings which we scrap in the inspection department?

MEMBER: I refer to the castings that you might salvage.

MR. SAWTELLE: We have a salvage department where we salvage some of the larger size fittings rejected because of a small shift. We do not carry that too far, and do not do it on the smaller size castings because we do not think it economical. One thing we have been experimenting with lately with some measure of success is welding by the heliarc method. When you heat a malleable casting near its melting point and cool it fast, you harden it, but the area of contact between the nickel weld metal and the malleable casting where it has been hardened is so thin that our tap can go through it quite readily without any signs of harm to the tap. That salvage operation is still experimental, but it seems to have possibilities.

R. V. OSBORNE:[1] How do you handle your castings to and away from such operations as shearing and grinding?

MR. SAWTELLE: The castings are carried by belt conveyor to the inspection room; from the inspection room to the annealing packing in pans on roller conveyors; from the pot dumping station to the final shot cleaning in buckets which are dumped into skip hoists at the shot cleaning machines; out again onto rubber belt conveyors for a second sorting; from this final sorting to the shearing or grinding in pans. Some use is also being made now of the electric dump-type gondolas. We have one or two of those in use now as well as barrels for the larger size castings.

W. E. JONES:[2] At what temperature do you put these castings in the water?

MR. SAWTELLE: We have to keep the largest ones out of the water. They would be too hot at 1300° or 1400° F., but we put some castings in water at a dull cherry red heat, about 1200° F.

MR. JONES: Why do you prefer the dual mill arrangement to a continuous mill? If you put another unit in would you put in a continuous mill?

MR. SAWTELLE: In the second barrel the castings also get a final rinsing. There is a rust inhibitor in the rinse water at this point so that the castings do not rust on the way to the inspection room. It also evens out the flow of material. I am not sure whether it would be an advantage to have the work ejected from one barrel. I prefer the two barrels which gives more uniform flow of materials.

MR. JONES: Why do you pack these fittings? We have practically no warping and do not pack ours at all. The cylindrical shape seems to protect them in the annealing.

MR. SAWTELLE: I imagine you use so-called bottom pots where every annealing pot has a bottom, with no packing. Then to prevent scaling of your fittings without atmosphere control you seal the pots.

MR. JONES: That is right. We seal with mud. I thought it may save you some handling and necessity of knocking the packing out to try some without it.

MR. SAWTELLE: We have tried it a few times experimentally but we get a little warpage, and hence are a little apprehensive in going into it. We dump the contents of a stack of annealing pots onto a vibrating screen by clamping the bottom pot, lowering the bottom on which the pots rest, and withdrawing it from beneath the stack of pots. The castings and packing material then fall by gravity onto the vibrating screen. We would not be able to use this method of dumping if we use so called bottom pots.

[1] Lakeside Malleable Casting Co., Racine, Wis.
[2] Stockham Pipe Fittings Co., Birmingham, Ala.

MANAGEMENT'S STAKE IN TRAINING FOREMEN

S. G. Garry
Caterpillar Tractor Co.
Peoria, Ill.

THE ABILITY OF ANY INDUSTRY to survive is measured by the extent to which it produces profits, without which capital will be withdrawn and materials, equipment and labor can not be purchased. A successful industry must produce goods which give satisfaction. Superlative advertising and sales effort alone are worthless—only a high-quality product efficiently produced will support a successful enterprise.

A successful industrial organization must be closely knit, must have internal unity of purpose and must be guided by intelligent leadership from the topmost executive to the foreman and his assistants. The importance of quality places the entire responsibility on the production division.

In the training and education of foremen, management has a stake of great import to industry. It is to the foreman that industry looks in seeking to control costs upon which profit and the successful continuance of the enterprise depend. It is upon the foreman's shoulders that the responsibility for quality and quantity efficiency rests, for it is under his direct supervision that the work of production is done. It is the foreman who must instill standards of quality workmanship into the habits of his men.

Foremen are in an excellent position to help their men to think straight. Lack of understanding is one of the greatest causes of industrial controversies. A sane and intelligent point of view on vital questions can develop efficiency, satisfaction and contentment in the workers and thus insure a smooth, steady output.

Cost Classification

There are three classifications of cost in industry:

1. Direct and indirect labor costs: 2. Direct material costs: 3. Overhead costs. The foreman is directly concerned in each one of these cost items. Only by training and educating the foreman in the factors of cost can management be assured of profitable production.

Direct labor costs are wages paid to men working directly on production, i.e., workers who actually are producing, building, machining, or constructing the company's product.

Direct material costs include all costs of materials which eventually become part of the final product.

The most important and most complicated of these three items, that of overhead cost and expense, breaks down into many classifications, most of which concern the foreman.

1. Indirect labor. This includes the wages of maintenance men, sweepers, truckers, inspectors, watchmen, shop clerks, etc.

2. Depreciation. In many industries, depreciation depends on the nature of the equipment and the care given.

3. Cost of light, power, heat and water.

4. Cost of insurance.

5. All salaries.

As a general rule overhead expenses remain fairly constant. But minimum overhead expense per unit of product is obtained only when a plant runs continually at capacity. Shutdowns for any reason are very costly, for a company is bound to lose heavily from its overhead when its plants do not run at capacity.

Training Foremen to Cut Overhead Expenses

How does management train its foremen to cut overhead expenses? Here is the answer to management's real stake in training foremen.

A foreman is trained to recognize, understand and control waste, whether in human form, or supply. He must:

1. Reduce accident expense by eliminating accident cause.

2. Minimize depreciation by better care of equipment, tools and machinery.

3. Economically supervise use of heat, water, light and power.

4. Select capable men for positions of responsibility.

5. Reduce waste of supply.

6. Safeguard the plant against fire, boiler and man hazards.

Foremen must be trained to:

1. Carefully select and place men on the right jobs. Management gains in training foremen particularly when it includes the lower levels of supervision. This makes it possible to replace a supervisor in the event he is elevated to a more responsible job, or leaves the company. In this instance, the company is in a position to replace that supervisor without loss of production because the incoming supervisor has already had sufficient training to fill the gap with practically no loss in efficiency.

2. Plan the work in advance.

3. Economically and efficiently lay out his department for full production.

4. Line up his equipment to promote efficiency.

5. Break men in and instruct them properly.

6. Win the co-operation of the workers.

7. Improve working conditions.

8. Work constantly to achieve less hazardous production methods in his department.
9. Reduce waste and scrap.

Foremen Transmit Policies

Foremen are the life line between top management and employees. It is through the foreman that the company should transmit its policies and views to employees. This same policy should work in reverse. Employees should have an open channel to management in order to transmit their problems. Those in top management should encourage foremen to use the channel opened to them as the need arises. The foreman must be given an opportunity to acquire a thorough understanding of management's viewpoints. This, in turn, provides the foreman with the necessary tools to carry on his work according to the overall plan.

To maintain a high level of efficiency in industry today, foremen must be continually provided with accurate information as to the manner in which the organization is functioning. The problems of transmitting information from bottom to top in many instances have not been clearly indicated. The information which flows through the supervisory structure should be an accurate representation of the problem.

In order that management may receive a factual story free from superfluous details, such information going through the various levels of the supervisors should be sorted, and only that information pertinent to the situation be compiled and presented to the members of management delegated to dispose of the problem. Only by careful sorting can management get a clear picture of the situation. By eliminating the details, management can render its decision in record time.

Training Program

What does management have to gain by training foremen to deal intelligently with representatives of the unions on matters arising in their departments? Regardless of how well an agreement has been drawn up between the parties, many problems arise that cannot be covered adequately by the agreement, or by the policies set up by the company.

In order to fortify the foreman with the necessary information whereby he may deal with the problems efficiently, management must provide a means of obtaining such information. It may mean that management will embark on an extensive training program, or a series of meetings. Regardless of the road management chooses, it spells training in one form or another. However, the most popular means of imparting information to the supervisor is through conference.

Primarily, the "conference" method of instructing supervisors in human engineering is one of indoctrination, making them management conscious, training them in the fundamentals and principles of good supervision, guiding them in the application of these principles to their own problems, and following up to see that they practice what they are taught.

They are being taught new "habits" and how to apply them with good results. The most successful conclusion to any conference is when the principles taught to the supervisors are made a part of the supervisors' own personalities. One supervisor may apply them differently than another. The principles followed must be the same, but the methods of using them will differ from supervisor to supervisor.

The foreman is trained in the importance of maintaining high productivity among his men. He is trained to carefully plan his departmental activities, to lay out his work, to improve methods and to stimulate his men to do their best.

Does Training Pay?

By what standard of measurement is management able to determine whether the program it has inaugurated is paying off in dollars and cents? Many responsible people are asking themselves these same questions. Any person responsible for training the foremen in his plant is deeply concerned and feels that management is entitled to some explanation as to whether the expenditures are justified.

To date, no satisfactory yardstick has been produced to determine the dollars-and-cents value of a training program; however, benefits may be determined by the following:

1. Time required to bring new employees up to average production.
2. Reduction in the number of man hours lost through unsafe practices.
3. Reduction in scrap.
4. Reduction in the number of complaints registered by employees.
5. Number of grievances going beyond the first step.

We all agree that the training program does produce results, but how are we going to prove that these results can be attributed to the training program? The information submitted must exclude new procedures, new methods and any changes in the reorganization of the plant. To discount all of the above factors would be impossible under ordinary circumstances. Perhaps it will be impossible to prove to our complete satisfaction that our reasoning is positive. However, we can be sure that our calculations are partially correct.

What Has Management Gained?

Events of the past few years have proven conclusively that the training of foremen has more than paid for itself. One cannot evaluate the returns in dollars and cents, but can only look at the foreman training program from the standpoint of increased quality, efficiency and morale building which cannot be properly evaluated.

What are the results obtained from training supervisors in the conference room, and what does management gain? The first question naturally would be: Have the supervisors in a plant improved because of the conference method? Those who are directly connected with the conference program feel there is a steady and definite improvement in supervision. First, they point to comments from employees which seemed to suggest such improvements. Second, there are the supervisors' own comments and admissions on the subject. Third, management believes that the training program was beneficial, both from a financial and a morale building standpoint.

Following are indications of the success of a training program for supervisors:

1. Employees' comments of a specific nature. Their reference was to specific changes which they had noted in their supervisors after the supervisors had been attending conferences, in comparison to their views and actions on the same problems before the training program was inaugurated.

2. One way of proving that a supervisors training program is fulfilling its purpose is to get the reaction directly from the supervisors themselves. On the whole, their reactions to the program are favorable. They are able to see their own practices through the eyes of the employees. From their discussions with other supervisors, they learn that their problems in many respects do not differ from those of any other supervisor. From the opinions of other supervisors on problems similar to their own they are able to see how other people handle such difficulties. In addition to offering the supervisor an opportunity to meet and exchange ideas with fellow supervisors, it affords him an opportunity to build friendships which are not only beneficial in promoting new co-operation between departments, but result in many "get-togethers" outside of regular work. In view of the progress made by supervisors thus far, it is evident that management has made many gains.

DISCUSSION

Chairman: F. C. CECH, Cleveland Trade School, Cleveland
Co-Chairman: B. D. CLAFFEY, General Malleable Corp., Waukesha, Wis.

MEMBER: I would like to inquire whether Mr. Garry follows a definite foreman training program at the Caterpillar Company?

MR. GARRY: We have a definite program that we follow in our supervisory conferences. Subjects are planned a year in advance. In some cases we may retain subjects we had the previous year and inject them in our training program for the subsequent year.

All training programs should be made to fit the particular needs of the organization that intends formulating such a program. Pertinent pamphlets are made up by the company and distributed to the supervisors. We have about 750 supervisors who are now attending foreman conferences, and each supervisor gets a pamphlet describing the various conferences. He decides which conferences he wishes to attend. There are various subjects to choose from such as speech, economics, job relations training, personality traits and conference leading. Conference leading is not offered until after the supervisor had many years of regular foreman training conferences. Only when we feel a need for new conference leaders do we inject that particular conference. Better English, practical psychology, reports of cost control and foundry practice are some of the subjects offered at the present time. You may think it odd to offer foundry practices in our conferences. However, there are machine shop supervisors who know nothing about the foundry and its problems. It becomes evident that such a conference is beneficial and at the same time stimulates cooperation between the two divisions.

Conferences on quality control, accident control, welding, accounting, timestudy, heat treatment, labor legislation affecting supervisors are also available to the supervisor. Labor legislation is a very interesting conference. It deals with the various labor laws; it goes into the history of labor as far back as 1790 up to the time of the National Labor Relations Act. It covers the Fair Labor Standards Act and the Public Contracts Act. This conference also includes the six-day week and the eight-hour day for women.

MR. CLAFFEY: Mr. Garry mentioned the selection of foremen. In many shops, particularly those that have incentive plans of payment, it is a problem to decide on what basis a man is to be set up as a foreman. It is quite a job to interest a man in a supervisor's job because he knows that when he is on this incentive basis, as he produces, so he is paid. He knows that with his overtime and his extra premiums he probably has an opportunity over the period of a year to earn more than the foreman does, even though his security of pay may not be as great. How can you handle that particular phase in the organization? You have all straight time at your plant, do you not?

MR. GARRY: Yes, our employees are paid by the hour. We do not have an incentive plan.

MEMBER: Many plants are beginning to think about group bonus, profit-sharing type plans for supervision, and I think that is your solution. Supervisors more and more are going to be brought into foremen's bonus plans of companies, where they will share with the minor and the major executives based on the total work of the company and the total record.

MR. GARRY: Why not pay the supervisor a reasonable wage and forget about the incentive plan? Incentive plans for supervisors in some instances have been a source of trouble. If you distribute it equally, then the supervisor who has a desirable job will probably be satisfied, but will the supervisor who has one that is undesirable? He gets the same pay as the one who has a desirable job.

MR. FALK: I do not think any forward-looking company would ever start a supervisor on a job where he did not get over and above what the man could make as a production worker on a premium basis. At least we do not.

MR. GARRY: During the war many companies had difficulty getting wage increases for supervisors. We had the problem and I still feel that some supervisors have not been sold on the idea that it was a wartime measure.

MR. CLAFFEY: At the present time and in the immediate past the foreman has been driven on one side by an organized group of workmen and on the other side by management looking for production. As a result, we must pay more attention and give more consideration to the position of the foreman. His position has changed entirely with the advent of organized labor and the recognition by the organization of collective bargaining in our shops.

There is one other point in Mr. Garry's talk that is very pertinent dealing with the organization meetings with the foreman. In large plants, probably this case is not as true as it is in the smaller plants, where we have a small organization of foremen. The foreman in few cases and the other supervisors do not have that opportunity to talk with their top management, the result being that many things that they learn about company policy and program come to them through the channel of the workmen whom they supervise.

If you do not think that that happens, just talk to your foremen or your supervisors not up in the very top brackets and you will soon learn how they feel about it. I think when Mr. Garry made the statement to the effect that we should discuss these things with our supervisors, it was one of the most important things in his talk. If you have not been following the practice of holding regular meetings with your organizational men and discussing with them your plans, policies and program for the future, problems confronting production due to shortage of supplies such as pig iron, coke, etc., it would be well worth while to do so.

If you are undecided as to what value such a meeting might have, if you have not done it in the past, try one or two, and I am sure that you will soon realize that we have been neglecting a very important factor in dealing with our organization. As Mr. Garry said, the money which we invest in our companies is put into the hands of these men who represent us. If you are not going to equip them with all the information we have, we cannot expect them to do a good job in bringing a return on the investment which they handle.

MR. GARRY: In our Foundry Division, in addition to having foremen conferences, we have supervisory meetings once a week. In these supervisory meetings they discuss various shop problems and, in the event they do not have shop problems, they discuss any situation or any question that may be pertinent. Every supervisor at one time or another has a complaint he would like to air. Perhaps, his complaint has grown to monstrous proportions not because it is a serious one, but because of the lack of an opportunity to get it out of his system. If given an opportunity to discuss their problems with fellow supervisors all supervisors gain in the exchange of ideas.

SPECIAL REFRACTORIES
FOR METAL MELTING

By William H. Henson
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ABSTRACT

Brief classifications of refractories and example of the various classes are given. The more usual types of failures, their symptoms and some possible causes are described. The important properties of special refractories are outlined, and typical applications are discussed, a few in some detail. Highly technical analysis of the foundryman's refractory problems are not included, but a discussion of broad general interest is afforded.

THE TERM REFRACTORIES has little or no meaning to people not familiar with furnaces. Such people may have some vague idea about fire brick but few probably ever saw one. Foundrymen, on the other hand, live with refractories every day and know them as a necessity to production. The operator probably gives them little thought, so long as production moves along smoothly. However, when trouble develops, he, and in turn his suppliers, become acutely conscious of the fact that refractories can present some difficult and tricky problems. Quick action is required to find the cause of the trouble and correct it. The discussions that follow are intended to assist the man in the foundry in understanding and dealing with any refractory problems he may encounter.

From the industrial point of view, refractory materials are those which meet a primary requirement of absence of softening by fusion at furnace temperatures. In addition they must invariably have those properties which enable them to resist destruction under one or more of the following conditions: corrosive slags, fluxes or furnace atmospheres, abrasion, heavy loading, and rapid temperature changes.

Classification of Refractories

Refractories may be classified in several ways and no one basis will serve all purposes. Simple classifications mentioned here for the purpose of illustration are (1) by chemical nature and (2) by source of raw material.

1. The classification by chemical nature may be shown as follows:

A. Acid refractories
B. Neutral refractories
C. Basic refractories

Acid Refractories are the most common and include fireclay, silica, zircon, and mullite refractories. The name "mullite" includes all refractories whose primary raw material is calcined kyanite, sillimanite or andalusite. The electric furnace product of this class is described as "synthetic mullite." Acid refractories are so named because of their high silica content, the principal acidic oxide.

Fireclay Refractories

The fireclay group includes low heat duty, intermediate heat duty, high heat duty, super duty and high alumina fireclay refractories. High alumina fireclay refractories are those containing over 47.5 per cent aluminum oxide and are classified as 50 per cent, 60 per cent, 70 per cent, and 80 per cent alumina. This distinguishes them from fused alumina brick which will normally contain from 87 to over 99 per cent aluminum oxide. Because of their chemical nature all of this group (except the 99 per cent alumina) react readily with basic slags and fluxes.

Disregarding the physical properties of the various bricks, reaction with basic slags is approximately in proportion to the refractoriness and the alumina content of the brick. Generally speaking, in the case of fireclay brick, the higher the alumina content, the higher the fusion point.

Silica bricks are next in importance to fireclay and make a valuable contribution to the foundry industry. Their outstanding properties are high fusion point and high load bearing capacity which make them suitable for furnace roofs. Being comparatively porous they do not readily withstand the action of highly fluid slags. Consequently care and judgment must be used where there is probability of slag action. Also due to their physical characteristics they are not suitable for periodic furnaces where thermal shock may be encountered.

Neutral Refractories

The most common neutral refractories are graphite, chrome ore, and alumina. They are considered neutral because they do not readily react chemically to form compounds with either silica or bases. Strictly speaking, silicon carbide may fall within this group, although due to oxidation and the type of bonds used, it behaves as an acid refractory in most instances.

* Chief Sales Engineer, Refractories Division, Norton Company.

TABLE I
PROPERTIES OF REFRACTORIES

	Type of Brick	Composition	P.C.E. or Fusion Point	True Specific Gravity	Approx. Weight in lbs. 9" Brick	Linear Coefficient of Expansion per °C	Thermal Conductivity BTU/Hr./-Sq.Ft./In./°F.*	Mean Specific Heat (cgs)	Deformation Under Load 25#/sq. in.	Constancy of Volume	Remarks
ACID	Silica	SiO_2 95–96%	31–33 Cone	2.3–2.4	6	See (A) Below	13.0 390–1832°F	0.265 70–1832°F	No deformation at 2732°F, 50psi	Low Shrinkage. Rev.† exp. to Melting Point	Strongly resistant to acid slags. Readily attacked by basic materials and fluorides.
ACID	High Heat Duty Fire Brick	Al_2O_3 35–42% SiO_2 52–60%	31–33 Cone	2.60–2.70	7½–8	53 x 10 $^{-7}$ 20 – 1200°C	400–2400°F 9.5	0.26 70°–1830°F	4.0+% @ 2460°F	2.0% Shrink. @ 2550°F	Rapidly attacked by basic slags, particularly iron slags. Moderately resistant to acid slags.
ACID	Super Duty Fire Brick	Al_2O_3 43–44% SiO_2 51–53%	33–34 Cone	2.65–2.75	7½–8	53 x 10 $^{-7}$ 20 – 1200°C	Approx. same as High Heat Duty	Approx. same as High Heat Duty	2.5 to 4.0% @ 2460°F	1% Max. Shrink. at 2732°F	
ACID	High Alumina Fire Brick	Al_2O_3 50–80%	34–39 Cone	2.80–3.40	8–9	53 – 65 x 10 $^{-7}$ 20 – 1425°C	Slightly more than High Heat Duty	Approx. same as High Heat Duty	Approx. same as Super Duty	0–3% Shrink. @ 2912°F	More resistant to alkali slags than fire clay brick; readily attacked by iron slags.
ACID	Kaolin	Al_2O_3 44–45% SiO_2 51–53%	31–34 Cone	2.60	7½–8	53 x 10 $^{-7}$ 20 – 1425°C	13.5 @2400°F	0.254 480–1832°F	1% @ 2800°F	0.5% Shrinkage @ 3000°F	Slightly more resistant than fire brick to slags, particularly basic slags.
ACID	Mullite	Al_2O_3 62% SiO_2 38%	38 Cone	3.03	8½	45 x 10 $^{-7}$ 20 – 1320°C	8.5 200–2600°F	0.175 70°–1475°F	0 @ 2650°F., 50 psi; 2.0% @ 3000°F, 50 psi	No appreciable change to 3000°F	Low solubility in glasses and some slags. Attacked by high iron slags.
ACID	Zircon	ZrO_2 67.1% SiO_2 32.9%	approx. 4400°F	4.6	12	42 x 10 $^{-7}$ 20 – 1550°C	13.5 390°–1832°F	0.132 100°F	Fails @ 2820°–2910°F	No appreciable change to 2820°F	Not readily attacked by acid slags. Readily attacked by basic slags and fluorides.
ACID	Silicon Carbide	SiC 89–91%	dissoc. @4082°F	3.13–3.22	9.5	45 x 10 $^{-7}$ 20 – 1100°C	66.0 @2000°F	0.186 70–1832°F	No deformation to 2732°F, 50 psi	No Shrinkage to 2732°F	Readily attacked by strongly basic slags, particularly those high in iron or calcium oxides.
NEUTRAL	Graphite	C 100%	above 5432°F	2.25	8	20 x 10 $^{-7}$ 20 – 1000°C	220. @ 2000°F	0.29 70–1832°F	None	No Shrinkage	Strongly resistant to all slags except those containing oxides readily reduced.
NEUTRAL	Chrome	Cr_2O_3 30–45% Al_2O_3 15–33% SiO_2 11–17% FeO 3–6%	3550–4000°F	3.8–4.1	11	80 x 10 $^{-7}$ 20 – 1000°C	12.1 @2460°F	0.22 @ 1832°F	No deformation @ 2600°F	1.3% Shrinkage at 2820°F	Neutral properties. Not readily attacked by acid or basic slags.
NEUTRAL	Fused Alumina	Al_2O_3 90–99.1%	38–41 Cone	3.80–4.00	11	70 x 10 $^{-7}$ 20 – 1000°C	18.0–20.0 @ 2000°F	0.174–0.304 32°–1832°F	1–2% @ 2732°F, 50 psi	No Shrinkage at 2732°F	Very resistant to acid slags, moderately resistant to basic slags; tendency to absorb.
BASIC	Forsterite	MgO 57.3% SiO_2 42.7%	40 Cone	3.3–3.4	9	110 x 10 $^{-7}$ 20 – 1500°C	10.3 @ 2400°F	0.22 70–200°F	No deformation to 2732°F, 50 psi	Negligible Shrinkage to 3000°F	Not readily attacked by basic slags; readily attacked by acid slags.
BASIC	Magnesia-Alumina Spinel	MgO 28.2% Al_2O_3 71.8%	3875°F	3.6	10	80 x 10 $^{-7}$ 20 – 800°C	14.5 @ 2400°F	0.25 70–1832°F			Moderate resistance to basic slags; low resistance to acid slags. Magnesia spinel sometimes shows slag absorption.
BASIC	Magnesite (Dead Burned)	MgO 83–93% Fe_2O_3 2–7%	3992°F	3.4–3.6	9½	147 x 10 $^{-7}$ 20 – 1425°C	13. 2400°F	0.278 70–1832°F	Poor load bearing property above 2450°F	No Shrinkage below 2900°F	Not readily attacked by basic slags; readily attacked by acid slags.
BASIC	Magnesia (Electrically fused)	MgO 94–96% SiO_2 2–3% CaO 1–2%	3992–4532°F	3.60	10	150 x 10 $^{-7}$ 20 – 1475°C	20.0 2000°F	0.292 32–2375°F	Poor load bearing property above 2732°F	Less than 1% below 3250°F	

* To change BTU units to C.G.S. units divide by 2903

(A) 430 x 10 $^{-7}$, 20 - 300°C, 30 x 10 $^{-7}$, 300 - 1100°C.

† Reversible



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Of all refractories, graphite is probably the least reactive. Liquid silicates do not wet it and thus it will not react with them. Its use, however, is confined to metallurgical applications in which the process of oxidation can be controlled.

Basic Refractories

Refractories of a basic chemical nature are of considerable importance for furnace linings where basic slags are encountered, as in the open hearth steel industry and in non-ferrous metallurgy. Where basic slags are used it is generally desirable to use basic refractories in contact with them. Magnesite, chrome-magnesite, forsterite refractories, dead burned magnesite, burned dolomite and basic clinker in granular form are the chief basic materials. Due to their high coefficient of expansion and susceptibility to thermal shock considerable care must be exercised in the use of the above refractories. They are in general not suitable for periodic or intermittent operations.

Electrically fused magnesia products and magnesia-alumina spinel likewise are in this group. Table 1 gives comparative data on the physical properties of most acid, basic and neutral refractories.

Classification by source of raw material, previously referred to, may be briefly shown as follows:

A. Naturally Occurring Minerals

1. Fireclay
2. Silica
3. Chrome
4. Magnesite
5. Forsterite (from Olivine)
6. Kyanite, Sillimanite and Andalusite
7. Zircon

The most important materials of this group have been previously discussed under chemical classification.

B. Synthetic Fused Materials

1. Silicon Carbide
2. Fused Alumina
3. Fused Magnesia
4. Fused Mullite
5. Magnesia-alumina spinel

The differences that distinguish naturally occurring raw materials from raw materials made in the electric furnaces are explained in terms of crystal development and strength of the respective granular substances (Fig. 1).

Electric furnace materials have a completely developed crystal structure whereas in calcined naturally occurring minerals, crystal development may be limited. Calcination often has as its main purpose the removal of water of crystallization and reduction in shrinkage. In the electric furnace, minerals in their strongest and densest crystalline form are produced, and some purification may be accomplished. Because of their density, these materials show little or no shrinkage after manufacture into bonded shapes.

Failure Due to Slagging

In general most refractory failures can be attributed to slagging, abrasion, fusion and spalling.

Slag action is the result of chemical recation between the slag or fluxes and the refractory. This can be greatly influenced by the composition and viscosity of the slag, the density of the refractory, temperature and the chemical nature of the refractory, i.e., acidic or basic.

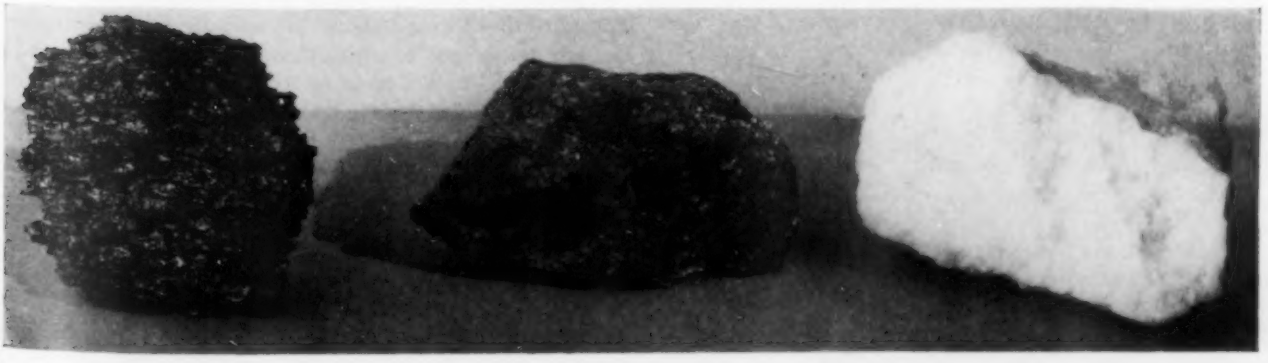
When slags are composed of highly reactive materials such as soda, potash, lime and iron oxide, slag action is accelerated. To get the longest life from refractories it should be the aim of every furnace operator to keep as well balanced a slag as possible. This of course is not always possible as the operator is often unable to predict what type of slag will be formed in a particular metallurgical operation.

Since all refractories have some degree of porosity, they are subject to penetration by slags. Fluid slags penetrate more readily than viscous slags. This action causes erosion, with the result that new surfaces of the refractory are continuously being exposed to reaction. In general, where temperature and chemical reaction are not severe, this action can be reduced by increasing the density of the refractory. There is, however, a limit to which this can be done economically.

A progressive decrease in rate of corrosion may occur if reaction balance is established by the cooling effect of increased heat loss through the thinner wall. This will decrease the rate of erosion by the slag and a relatively small reduction in temperature (as little as 50 F) will often materially reduce the action of the slag upon the lining. This has led to some interesting studies on the effect of insulation on furnace life.

Slagging of refractories is often aggravated by a reducing atmosphere in metal melting furnaces. Ferrous

Fig. 1—Products of electric furnace in lump form. Left to right: Silicon carbide, fused alumina of 95% purity, and fused alumina of 99.5% purity.



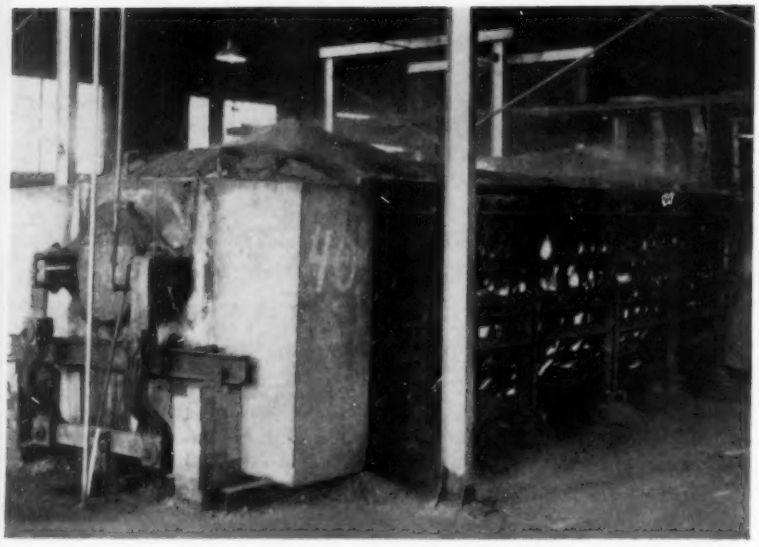


Fig. 2 — Resistance furnace producing silicon carbide.

oxide in the refractory, formed by the reduction of ferric oxide, accelerates the destruction of the refractory by slagging. Use of refractories of lower iron content will minimize this hazard.

Flame impingement is another common cause of excessive slagging. When the flame is permitted to impinge directly on the refractory, where slag is present, the elevated temperature will accelerate chemical reaction. Care in firing any furnace will contribute to longer refractory life.

Failure Due to Abrasion

Abrasion is the wearing away of refractory surfaces by the scouring action of moving solids. Tests have indicated that high strength refractories in general possess the greatest abrasion resistance. The abrasion resistance of the refractory in service decreases with higher operating furnace temperatures which soften the refractory.

The factors influencing abrasion are speed, pressure, angle of impact, and nature of the abrading fuel bed and charge as it moves against the furnace wall. The velocity and entrained solids in the flue gases are also important. Improper or careless use of cleaning tools and improper or careless charging of heavy pieces into the furnace must be avoided. Thickness and type of cement used in the joints must be watched.

Failure Due to Fusion

Fusion or softening is not a common type of failure but is one which can cause serious troubles. It is often the result of improper selection of refractory. It frequently comes with high localized temperature on piers, sidewalls and roofs due to flame impingement. Loading beyond the safe limits for a refractory at a given temperature may cause failure by plastic flow. This generally occurs in arches and locations where high stresses prevail. In structural design load test data for the specific refractory should be considered, and an ample safety factor should be allowed.

Softening can also be caused by penetration of materials such as fluid slags which, by reaction, reduce the fusion point of the refractory. This is sometimes caused by reducing atmospheres, and can be avoided by selecting refractories of lower iron content or of higher density.

Spalling

Spalling is a word that applies to stone-like or low-tensile strength mineral substances meaning "to break up into chips, chunks, or fragments." This term adequately describes a type of failure that may be encountered in using refractories. It results when these brittle materials rupture instead of buckling, bending, stretching, or otherwise yielding under sudden or severe stress. The stresses that cause spalling in refractories develop through one or both of the following conditions:

1. Unequal or differential thermal expansion or contraction of a refractory.
2. Unequal mechanical loading, or localized overloading.

In the case of spalling due to unequal thermal expansion and the resultant thermal stresses a brittle refractory of low-tensile and higher-compressive strength breaks up rather than deforms under the (irresistible) unequal expansion or contraction following sudden localized heating or cooling. If one face of a body is suddenly heated faster than another, the heated part expands rapidly and tries to bend or stretch from the part that expands slowly or only a little. The same occurs during contraction on sudden unequal cooling. When this differential expansion or contraction is extreme, the resulting stresses will cause the refractory to fail just as though it had been fractured with a hammer and chisel.

If the piece broken loose has been carrying a critical mechanical load, its absence may cause overloading of adjacent parts and thereby cause more or less progressive mechanical failure. Unfortunately, when a piece spalls off, it presents a new face of the refractory which is subjected to the same stresses developed by severe local heating or cooling, for example, flame impingement—or—sudden introduction of a cold charge, etc.

Thermal Expansion and Conductivity

Consideration of two physical properties of solids assists in making better selection of refractories to combat and minimize thermal spalling or heat shock. These physical properties are thermal expansion and thermal conductivity.

By using refractories having a low coefficient of thermal expansion the difference in expansion and resultant stresses when subject to unequal heating will be minimized. Fused silica for example has an extremely low coefficient of expansion; consequently, it can be quenched in water from white heat without spalling or fracturing.

By using refractories of high thermal conductivity local heating may be equalized quickly and the stresses of differential expansion will be minimized.

The refractory in a furnace may undergo a change in the coefficient of thermal expansion or heat conductivity so as to cause spalling. These alterations in physical properties may be caused by such unfortunate conditions as slag penetrations, vitrification and shrinkage

due to overheating or reaction with fluxes. Improper insulation can contribute to these changes. When slag penetrates a porous refractory or when fluxes or overheating cause vitrification (development of glassy texture) in the exposed portion of a refractory, the physical properties of the thus modified mass may be different from those of the original material which remains in the unexposed part of the refractory. Differentials in expansion, conductivity, and mechanical loading are all conducive to spalling.

Slag Penetration

When spalling occurs due to slag penetration new refractory faces are presented for further penetration by slag. Thus a vicious progression of penetration and spalling can rapidly destroy a refractory structure. Barring and cleaning of slagged surfaces should be done carefully.

Spalling due to localized overloading is one wherein a brittle refractory is pinched or subjected to bending stresses through improper furnace maintenance, construction or design. Sometimes the rupture of the refractory is caused by inadequate expansion joints in areas having greatest expansion. Arches, skews, jambs, and keys often exhibit this type of spalling, frequently due to improper alignment in construction and the difficulty of loading these members uniformly.

Special Refractories

The term Special Refractories has been selected to designate those refractories whose basic raw materials have been synthesized or fused in electric furnaces. The term Super Refractories sometimes applied to this group of refractories, by implication, imposes an unfair burden on them. Super suggests that they are the ultimate in refractories for all applications. Actually their use is limited to those instances where they can demonstrate an economic advantage. They cannot be recommended as a "cure all" for the foundryman's refractory problems.

However, it has been clearly shown that these materials extend the range of operations in many difficult fields and have wide possibilities yet to be exploited. Although the use of these refractories has been limited, there are ample indications that further study and trial should prove beneficial to the foundry industry.

Special Refractories refers to that group whose chief constitutent is one of the following electric furnace products: fused alumina, silicon carbide, fused magnesia, and synthetic mullite. These are the best known and more common ones. Others upon which some work has been done are fused stabilized zirconia, magnesia-alumina spinel, fused beryllium oxide and fused thorium oxide. Because of their present high cost and industry's limited knowledge of their properties, it is doubtful if this latter group will be of immediate interest in the foundry field.

Products of the Electric Furnace

Silicon carbide is produced in a resistance type electric furnace from an intimate mixture of silica sand, coke and sawdust (Fig. 2). The mixture is placed around a graphite core through which the electric current passes, heating the charge to the reaction temperature. Large quantities of gas are envolved and the sawdust gives porosity to the mass providing an escape for the gasses. When a furnace run is completed, a core of silicon carbide remains, surrounded by a mass of unconverted material.

Fused alumina is produced in a direct arc type furnace and is usually a periodic operation (Fig. 3). Bauxite or alumina concentrates are fused to a molten mass and allowed to crystallize in the furnace. Two grades of fused alumina are produced; one from bauxite containing approximately 96 per cent alumina with small percentages of iron, silica and titania, and a purer grade containing 99.5 per cent alumina, with the balance consisting of soda, silica and iron.

Fused magnesia may be prepared from calcined magnesite or sea-water magnesia. This is also prepared in the direct-arc type furnace by the same method employed for alumina.

Synthetic mullite is usually produced in the same type of direct arc furnace, from naturally occurring minerals such as bauxite, diaspore clay and silica mixed to give the proper composition.

Silicon Carbide Refractories

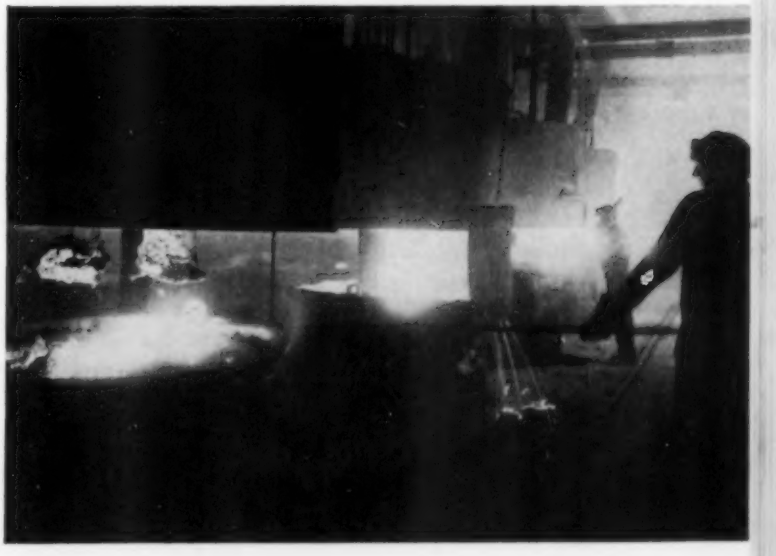
Of the special refractory materials, silicon carbide is probably the most widely used in the foundry field in the form of bonded-and-fired bricks or shapes and refractory cements.

Silicon carbide bricks and shapes are used for lining:

1. Crucible melting furnaces, stationary, tilting, crucible and pit type. They are also used for covers for these furnaces.
2. Copper alloy melting and scrap refining furnaces, reverberatory, cupola and open-flame types.
3. Aluminum melting furnaces, reverberatory, crucible melting or metal pot types.
4. Combustion zone in air furnaces.

Silicon carbide refractories also find use in the foundry field in crucibles for non-ferrous melting, fabricated muffles for large malleable annealing furnaces, and slag hole blocks for gray iron and malleable operations.

Fig. 3—Direct arc furnace producing fused alumina or fused magnesia.



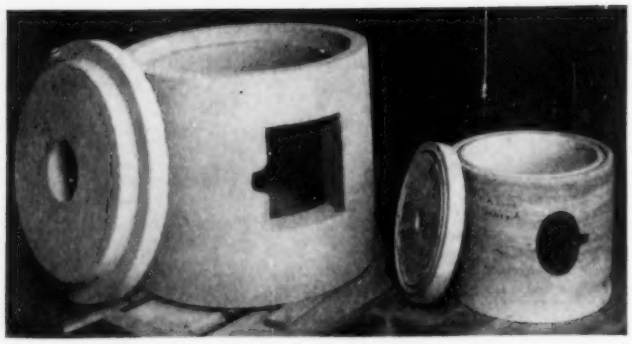


Fig. 4—Crock and cover linings for two sizes of indirect arc furnace.

Silicon carbide tubes for recuperators in the metal industry are well known. Considerable work has also been done on the use of internally-fired silicon-carbide tubes as sources of radiant heat for annealing and heat treating furnaces. These are intended for use in that temperature range which is too high for heat resistant alloy tubes. The results have been encouraging and at least one company is offering furnaces incorporating this design and others are considering this application.

Silicon carbide slag hole blocks are widely used, especially in back-slagging cupolas. Comparatively speaking, where clay blocks give one hour or more of service, silicon carbide blocks will consistently serve from one 8-hr to three 10-hr shifts before replacement is necessary. Conditions of operation affect the life of silicon carbide and highly basic slags tend to shorten the life of the block. Since molten metal flowing through slag hole blocks will rapidly erode them, metal contact should be avoided as much as possible.

Silicon carbide door jambs and lintels are used in direct arc type furnaces, in many instances supplementing the usual lining and reducing wear and spalling in the door area.

Silicon Carbide Cements are used for:

1. Rammed linings and patching in open flame furnaces melting non-ferrous metals.
2. Rammed linings and patching in all types of crucible melting furnaces.
3. Patching cement in non-ferrous scrap refining furnaces at the slag line.
4. Patching cement for malleable air furnaces at the slag line just above the metal.

They have a wide range of application and probably constitute the largest single usage of silicon carbide in the foundry trade.

Silicon Carbide Grain

Silicon carbide grain, formerly known as fire sand and lower in silicon carbide content than that used in bonded shapes or bricks and cements, was sold in large quantities to the foundries who created their own refractory cements by the addition of a plastic clay for bond. Some of these mixtures often showed outstanding qualities and seriously confounded the refractories engineer who tried to replace them with one of his own prepared cements. Silicon carbide grain is still available for this use but is now sold under other designations. It is of higher quality than the fire sand and commands a higher price. During the war, necessity required the withdrawal of fire sand from the market in support of the industry's effort to meet the ever increasing demand for more silicon carbide.

Many crucibles of the so-called clay graphite type contain appreciable quantities of silicon carbide grain as a primary ingredient and show decided service benefits.

Fused Alumina Refractories

Fused alumina refractories, composed of fused alumina grain, bonded into bricks or shapes with a suitable type and quantity of ceramic bond have found little use in foundry practice. However, when used for special applications, they have given good service.

Large fabricated muffles for anealing castings to protect the work from the products of combustion and flame impingement have given excellent service.

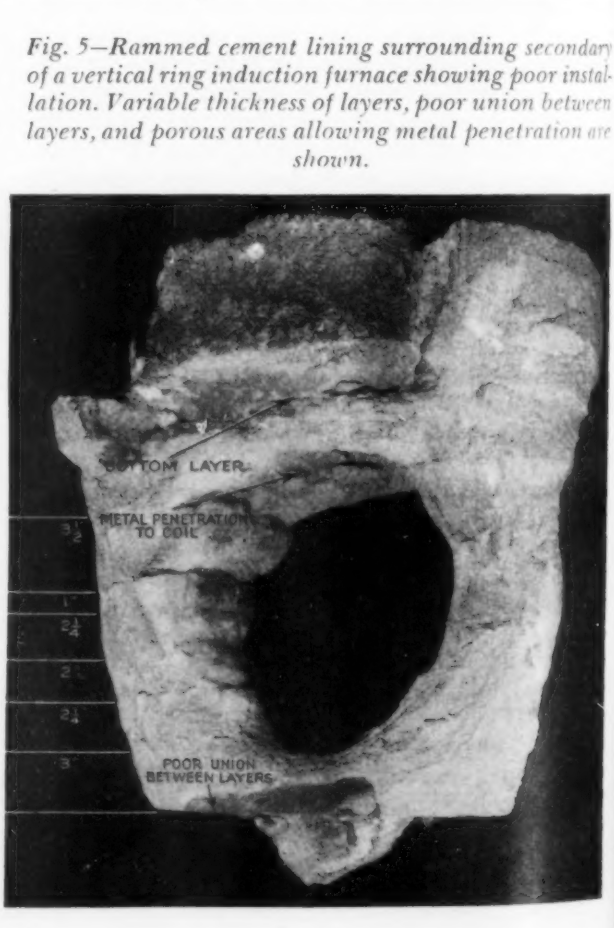
Solid rings as well as segmental linings have been successfully installed in gas and oil-fired crucible melting furnaces. Segmental covers of this material have been tried but usually fail due to thermal shock.

Burner tunnels and burner blocks for melting furnaces have been supplied in fused alumina.

Pouring spouts and tubes for precision casting of heat resistant alloys have also been successfully used.

In one case where high conductivity copper was being melted in an indirect arc furnace, trouble from silicon pick up in the melt was encountered while using an aluminum silicate lining, necessitating the scrapping of a number of melts. By installing a fused alumina brick lining, silicon pick up was eliminated and a considerable saving was shown, without considering a 20

Fig. 5—Rammed cement lining surrounding secondary of a vertical ring induction furnace showing poor installation. Variable thickness of layers, poor union between layers, and porous areas allowing metal penetration are shown.



per cent increase in lining life. This was true in spite of the fact that the fused alumina lining cost twice as much as the one formerly used. In this case special fused alumina door jambs, lintels, and port blocks were also installed.

Fused alumina cements are a mixture of fused alumina plus a suitable clay bond, selected to mature satisfactorily within the temperature range of use. In most cements of this type, it is important to keep the percentage of bonding agent as low as practical and this seldom exceeds 15 per cent by weight. Most applications of these cements are in the non-ferrous field.

Several fused alumina cements have been used successfully in the Ajax Wyatt or vertical ring induction furnace for melting yellow brass and a wide range of copper alloys such as nickel silver, gilding metal, leaded brasses and bronzes and many others.

It is also used as rammed linings and patching cements in the indirect arc type furnaces melting pure nickel and nickel alloys such as the (aluminum-nickel-cobalt) magnet metals and some heat resistant alloys.

In a test of a rammed fused alumina cement roof for a small direct arc furnace, melting 1000 lb of alloy steel per heat, encouraging results were obtained. With basic melting practice, the best previous roof had given seven heats, while the fused alumina cement roof gave an average of 45 heats. When the furnace was switched to acid practice, a life of 250 heats was obtained. So far, this is the best roof this foundry has found, indicating that further work should be done in this field.

Successful life has been reported when melting beryllium copper and manganese steels in high frequency induction furnaces lined with fused alumina cements.

To a limited extent these cements have also been used as rammed-in linings in various types of gas-fired crucible melting furnaces, especially where high temperatures are consistently maintained.

Burner tunnels and burner blocks in metal melting furnaces are often rammed in place on the job using these cements. They may be installed for both gas or oil firing, being sufficiently refractory to withstand flame erosion satisfactorily.

Fused Alumina Grain

The principal use of fused alumina grain in the foundry is for mold facings in the Durand process as described by J. H. Hall in patent No. 2144532. This grain is excess fines from the milling of fused alumina for abrasive purposes and is generally classified as "settling tank fines." Some of the larger foundries use several carloads a year. As described in the patent, the mold composition is 100 parts alumina, 13 parts hydraulic setting or Portland cement and 7 parts water. This is insufficient water to completely hydrate the cement and Durand describes the mixture as being "sub-hydrated." The mixture of alumina fines and cement is more refractory than the silica sand and Portland cement mixture comprising the balance of the mold. It reduces metal penetration of the mold at those points where this is a common source of trouble.

Some work has been done on the use of fused alumina fines as a core wash, and in one or two instances this application has been satisfactory.



Fig. 6—Section of lining from lower left-hand side showing enlargement of slot from rectangular cross section of approximately 3 in. x 1 in. to oval section 4 in. x 2½ in.

Fused Magnesia Refractories

Crucibles of clay bonded fused magnesia are available for high frequency induction melting in capacities up to 1000 lb. They may be used for melting various ferrous and non-ferrous alloys. Crucibles are seldom used in furnaces of over 500-lb capacity, since rammed-in linings have proven more practical in most cases.

Small special linings of fused magnesia containing very small percentages of bond have been widely used in small indirect arc melting furnaces for precision casting heat resistant alloys of the high cobalt-nickel type (Fig. 4).

Larger indirect arc furnace linings of the so-called crock-and-cover type are available in fused magnesia. These range in size from 13 in. diameter by 13 in. long up to 20 in. diameter by 32 in. long and weigh from 50 to 1100 lb.

Many of the fused magnesia cements contain fused alumina as well as fused magnesia, together with some suitable bonding agent, generally a small percentage of plastic clay. The percentage of fused alumina may vary considerably but seldom runs over 40 per cent (Figs. 5 and 6).

Cements of this type are used for rammed linings in the Ajax Wyatt or vertical ring induction furnace melting cupro-nickel, various bronzes, phosphor-copper and in general those copper alloys of low-zinc content.

In the high frequency electric furnace, these cements are used for melting stainless steels, nickel-chromium alloys, alloy tool steel, etc. For small melts in laboratory furnaces, where contamination from a graphite crucible is of concern, a fine grained cement of this type is often used to line the crucible.

In the small indirect arc type furnaces these cements may be used as rammed-in linings when melting heat resistant alloys for precision casting.

In any discussion of refractories for foundry use, attention should be given to zircon and mullite refractories. Both are of considerable importance in the foundry field and their use has proven helpful in many troublesome melting problems.

Miscellaneous Special Refractories

Zircon bricks and shapes have been widely used in linings in aluminum melting furnaces. Zircon crucibles have proven useful, primarily in the melting of platinum and other precious metals.

Zircon sand has found considerable use as a core wash. J. H. Hall has covered its use with hydraulic setting cement as a mold facing in the Durand process covered by patent No. 2169384.

Mullite refractory linings are widely used in indirect arc type furnaces for the melting of practically all types of non-ferrous alloys, stainless steels and heat resistant alloys.

For melting a wide variety of copper alloys in the vertical ring induction furnace mullite refractory cements have gained considerable usage in recent years. They are also used in many foundries for patching and lining crucible melting furnaces and open flame furnaces, for building up pouring spouts and for other miscellaneous uses.

J. H. Hall has also covered the use of Mullite, Sillimanite, Kyanite, and Andalusite as a mold facing in the Durand process, patent No. 2169385.

By discussion of classifications and descriptions of refractory materials, their successful applications, and some of the factors entering into failures, an approach has been made to the evaluation of special refractories in the foundry.

Special refractories are just what the name implies. They are a special tool available to foundry operators and other furnace users for specific applications.

Due to the complexity of refractory failures in the foundry, collection of empirical data from field tests is still one of the best means to use in approaching a problem. An understanding of how and why failures occur, along with experience, will serve to analyze troubles and conduct tests. Few failures have a single cause. In most cases the cause will be a combination of factors woven into a complex pattern. Although failures have had considerable attention, the fact that much has been learned and that most progress has been made through successful application of refractories should not be overlooked.

Improvement in the life of any refractory installation may be achieved by careful selection of the product best suited to the job, proper installation, adequate maintenance provided at the time needed, protection from thermal shock, attention to the firing equipment and care to avoid damage in charging or cleaning the furnace.

The best refractory for any specific job will not yield satisfactory service if it is carelessly installed or abused during operation.

DISCUSSION

Chairman: R. H. STONE, Vesuvius Crucible Co., Pittsburgh
Co-Chairman: A. S. KLOPF, Western Foundry Co., Chicago

C. A. BRASHARES[1]: I believe Mr. Henson's paper on these special refractories is very timely. Some few years ago, these higher-priced refractories might have been of little interest to the practical foundryman. However, with the increased cost of labor for maintenance and the heavy costs of lost production, foundrymen are becoming more and more willing to consider higher-priced refractories where improved service life is a reasonable possibility.

The term "special refractories" frequently is considered as applying to types of refractories produced in limited quantities. Those in the refractories industry, however, who will recall the development of the 70 per cent alumina brick and the chemically-bonded chrome-magnesite brick know that it has not been many years since these two products were considered "special refractories." As the output of a given product increases, there is less and less inclination to refer to this product as a "special refractory."

CHAIRMAN STONE: Refractories have a distinct bearing on the problem of interesting younger men in the foundry industry because the part of the foundry considered objectionable is mostly in the hot zone, in the hot places. If these special refractories are brought in they should reduce the amount of repair necessary, which is one of the most onerous things in foundry work. Insulation would also have the same effect.

It is the intention of the Refractories Committee of A.F.A. to make the refractory field more understandable to the foundryman. If foundrymen have time to study the field of refractories and understand what the different kinds of refractories are and why they are applicable in certain places and not in others, it will help considerably. This paper was intended for that purpose along the lines of refractories. Table 1 fills a need because you may not know what a refractory is but if somebody mentions it you can turn to a table like this and find out about it. It lists all the properties and some of the more common applications.

MEMBER: Please elaborate on silicon pickup by aluminum in fireclay refractories.

MR. HENSON: The case referred to happened to be a special one which they were working on. A small amount of silicon pickup by the molten copper would throw it out in electrical tests.

MEMBER: Is that in aluminum alloys?

MR. HENSON: That is in high copper alloys. Apparently we were getting a certain amount of reduction of silicon from the lining. There was enough reduction of silicon that the pickup was appreciable. It was not a fireclay lining but an aluminum silicate of the mullite type. It happened to be a special job in which high conductivity copper was being melted and the silicon pickup was sufficient to throw it out on electrical tests and by changing to a fused alumina lining (92 per cent alumina) they were able to eliminate this silicon pickup.

CHAIRMAN STONE: Was it an indirect arc furnace?

MR. HENSON: Yes, it was indirect arc furnace.

J. MCBROOM[2]: I am interested in that little electric furnace roof on the 1,000-lb. electric furnace. There are a lot of us with that problem. Were any special precautions used in installing the material? Was it air-setting or heat-setting mix in the roof? Are there any special instructions that you would like to pass along for installing such a thing? I would like to find out what material that was.

MR. HENSON: It was a fused alumina cement. No special precautions were used in installing it. It was a heat-setting cement. Some difficulty was experienced from the lining peeling off, as will happen with practically all heat-setting cements under that type of service. But that difficulty has been overcome to some extent by reducing the thickness of the roof. The lining was simply dried out and put in place and the furnace brought up to heat.

W. R. JAESCHKE[3]: What possibilities are there in the application of a zircon refractory in an iron melting furnace?

R. KNAUFT[4]: We have been manufacturing zircon refractories since 1930. Primarily we were looking for a market for what we thought was an interesting special refractory. Our first zircon products were small crucibles used for the melting and alloying of platinum. We entered into the aluminum field during the war. The Navy approached the makers of the Detroit electric furnace for a refractory lining which would have a softening point considerably above that of mullite or the special refractories which were available and which would meet the

[1] Harbison-Walker Refractories Co., Pittsburgh

[2] Stainless Foundry & Engr. Co., Milwaukee
[3] Whiting Corp., Harvey, Ill.
[4] C. Taylor Sons Co., Cincinnati

limits that were imposed on the furnace in operation so they ran some tests on zircon. Basically any furnace where iron oxide is present at a high temperature, is no place for zircon. Yet surprisingly enough we have gotten some good results. In fact, in a short time one company will sell nozzles for steel ladles that will contain a zircon inner sleeve.

In the indirect-arc furnace the zircon refractory is not displacing sillimanite or mullite. Zircon may double the life or number of heats per lining. However, when you take into account the higher cost of zircon over mullite, there usually is no appreciable saving in refractory cost per heat. I do not see where zircon has any place in your cupola or an air furnace.

MR. JAESCHKE: Is there any refractory that has a high point that will not run away from this universal flux iron oxide?

MR. KNAUFT: What a few years ago were super-refractories or special refractories are now common, ordinary refractories in the foundry industry. Your solution will be a basic refractory.

MR. BRASHARES: Magnesia or more commonly termed magnesite is a basic refractory and is exceptionally resistant to the chemical attack of iron oxide. However, we sometimes overlook the fact that a slag running high in alumina is very reactive with magnesite brick as are slags running high in silica. At high temperatures, iron oxide is actually dissolved by magnesia, with little visual evidence of the solution having taken place.

J. E. REHDER[5]: A lot of air-furnace slags run between 20 and 24 per cent iron oxide. They are usually fairly low in alumina. The only place alumina can get in is from the brick. We are not talking about straight FeO. I do not think alumina is important but I am wondering how important the silica content of the slag is?

MR. BRASHARES: In this connection, it is interesting to note that magnesia brick in their manufacture are fired on silica brick. In the firing of magnesia brick, they are set on a brick running approximately 95 per cent silica. Even at high-kiln-firing temperatures, there is no noticeable reaction between the silica and the magnesia. However, if a piece falls off of the fireclay brick crown of the kiln, it may frequently show a decided reaction with the magnesia. The fireclay brick from the kiln crown has a silica content of approximately 50 per cent and an alumina content of about 40 per cent.

MR. REHDER: Would not the answer be laying the walls up so the materials were above them?

MR. BRASHARES: Silica and alumina slags, or the combination of the two as is usually the case, in the same slag, would be chemically unsuited to magnesite brick.

MR. REHDER: Where would alumina come into a totally basic-lined furnace?

MR. BRASHARES: There would not be any substantial amounts from the refractories in a totally basic-lined furnace. I was thinking from the standpoint of slags which would be introduced or developed in the furnace.

MR. JAESCHKE: The chemical reaction between the acid lining and the so-called neutral fire clay lining would not be too good in a furnace at that temperature. I would think you would have to go to a totally basic lining.

MR. REHDER: In 100 per cent basic furnace, would there be any trouble with roof drip? What would you think of a basic roof?

MR. KNAUFT: You would probably have to go to a basic roof. I should think the silica and alumina you would get from the refractories in the roof of a duplexing furnace would be negligible.

MR. BRASHARES: One consideration is the possible change in practice with the basic bottom and side walls showing satisfactory service. This change very probably would lead to materially higher temperatures. We have seen this in many cases where strengthening of certain weak points in the refractory structure resulted in much more severe service conditions on other parts which were previously considered of satisfactory resistance. The higher temperatures would naturally lead to more severe conditions for a silica roof and an increased tendency for dripping.

MR. KNAUFT: Mullite type brick have been used successfully in the metal-lined and the air furnace, in the holding furnace particularly in conjunction with the cupola. Certain of the large steel companies tried magnesite in a big 1,000-ton mixer and used a band of brick. Then the selection of refractory is essentially a compromise. The mullite is not outstanding for iron oxide but mullite has a place in that project.

MEMBER: Table I shows the alumina content of the mullite brick to be about 62 per cent. Is that a true mullite brick?

MR. KNAUFT: There are ways of developing the additional mullite within your bond but basically that is correct, as far as the acceptance of the product is concerned.

MEMBER: If that is satisfactory in the sidewall, then it should serve well in the bond, too, should it not?

MR. KNAUFT: I do not think you need it. It is pretty expensive.

CHAIRMAN STONE: When you spoke of zirconia silicate for refractories, you meant zirconium silicate? That is the natural mineral "zircon" containing 60 to 80 per cent $ZrSiO_4$.

MR. KNAUFT: Yes, it has some interesting properties.

CHAIRMAN STONE: When they speak of zircite, they mean the natural mineral oxide containing about 60 per cent ZrO_2.

MEMBER: Mr. Knauft mentioned that a certain manufacturer will make a nozzle with a liner or sleeve in the nozzle for steel ladles. What is the purpose of this liner in a nozzle?

MR. KNAUFT: This is used for reducing slag inclusions in high priced forging steels. It does not belong in an ordinary foundry. The nozzle is expensive and will be used in large ladles where an ordinary refractory will not stand up satisfactorily.

MEMBER: You have in mind ladles for steel mills.

MR. KNAUFT: Yes, what they are trying to do is to cut down inclusions.

L. A. BEHRENDT[6]: Will this liner extend into the seat of the nozzle?

MR. KNAUFT: The stopper will set on the zircon which runs all the way through.

MR. BEHRENDT: It runs all the way through the blow holes.

MR. KNAUFT: It is supported with the fire clay material. About a half inch of zircon is used.

MR. MCBROOM: Is there such a thing as a good basic mix that could be used for roof linings? I have tried lining a roof once or twice with magnesite brick and it did not hold together at all at high temperatures. I tried ramming it with two or three different synthetic magnesite mixes and that was entirely unsuccessful. Do any of the refractory men know of a basic mix that would possibly hold up in a roof? I am thinking of a small basic electric furnace.

MR. KNAUFT: As far as I know, the first rammed roof on an electric furnace for melting steel, which gave outstanding results was installed about 18 years ago and I should think that Mr. Henson's idea of proper grain size and a bonded fused magnesia ramming mix or mullite ramming mix would answer your problem.

MR. MCBROOM: Mullite is acid, or at least on the acid side of neutral.

MR. KNAUFT: We have many mullite or sillimanite roofs on basic furnaces.

MR. MCBROOM: If you are trying to run close slag control to control certain slag reactions, when the roof goes to pieces you have difficulties.

MR. KNAUFT: I thought you were speaking of life.

MR. MCBROOM: Life would, of course, enter into it but I cannot find a basic roof that will hold for more than one or two heats. I think I have had one last up to four or five but that is not very economical operation.

MEMBER: I am not a foundryman but we are taking on refractory installation material. You who have been talking about mullite and magnesia, have you tried multichrome for any of your jobs? A manufacturer is coming out with a refractory that is used for the very purpose you have mentioned.

A. H. THOMSON[7]: We have tried magnesia in electric furnace roofs with very poor results. But sillimanite roofs will certainly give you many hours of good life, many weeks perhaps before you get contamination in the slag. You will probably get economical use from it.

MR. MCBROOM: The particular furnace I have in mind is abnormally hard on roofs because of abnormal voltage that is available and the best life we have had has been with the

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[7] Canadian Refractories, Ltd., Montreal, Que.

mullite type, which we are using now. But we do melt them out rapidly so that as they do go to pieces they do bother our slag operations. But normally if you had the proper voltages available on the electric furnace the contamination of your slag from a mullite roof would probably not be serious.

MR. HENSON (author's closure): Mr. Brashares' point, regarding "special refractories," is well taken at this time. The term "special" as applied here to refractories, whose chief source of raw material is the electric furnace, is not intended to set them apart as some mysterious product which will eventually prove to be the answer to all the foundryman's refractory problems. They are "special" only in the sense that the source of raw material is different, their cost comparatively high, their use limited at present and their application not extensively developed in the foundry field. They are a potential tool not widely used by foundrymen, though their usage is rapidly increasing. At present their role is supplementary to the better known and more commonly used materials. With increased use and knowledge the term "special" may well be dropped.

The interest shown in zircon (zirconium silicate) and some of the remarks in the discussion would indicate that it is a material of considerable possibilities in the metal melting field, beyond those uses mentioned.

The possibilities offered by rammed monolithic electric furnace roofs has been only casually explored and would seem to offer a fertile field for experimenting by the foundrymen. Here consideration should be given to the *more refractory* fireclay plastic materials as well as mullite, fused magnesia or fused alumina refractory cements.

In general, lengthened life seems to be the chief source of concern to the foundrymen in discussing refractories. As closer slag control and contamination of both slag and metal becomes more and more important in the melting of various metals these "special refractories" offer a potential source of help in these problems as well as the possibility of longer lining life. Certainly there are many "spots" in the foundry industry where investigation and experimentation in their use would afford an ample return.

TWO WAYS TO MAKE A PROFIT

Wally E. George
Account Manager
Booz, Allen and Hamilton
Chicago

SOME FOLKS MAKE MONEY in business by buying as cheaply as possible and selling at prices as high as the traffic will bear. Junk yard dealers blossom into millionaires by this nondescript approach. It might be a paradox to call it unorthodox.

Some foundries seek to make profit in like fashion. For every one that succeeds by this method, there are at least a score of monuments in the forms of closed foundries and bankruptcies that one can point out. Wise men do not risk their capital on hit-or-miss deals at 20-to-1 stakes unless they are desperately seeking to get rich quick.

Common sense decrees that there must be a better way, one that involves a normal risk at one-to-one odds. Successful foundrymen have found it. Their steady run of profits year after year attests to their having discovered the winning formula.

Determine Costs before You Sell. The better way to profits can be stated so simply that there seems to be a catch in it: Know your selling price in advance. This must be based on known costs and those costs *must* leave a margin of profit. That is all there is to it!

This is easier said than done, but making profit in the tough and competitive era ahead will be no easy task. He who would succeed should employ all the mechanical, manual, and mental skills and ingenuity known to his industry. He who neglects to use all the shop and office tools available becomes an odds-on favorite to fail.

To be sure, some will make profit without any cost system whatsoever. They will operate without the expense, or benefits, as you may look at it, of cost accounting, estimating, engineering or laboratory. The rule of thumb, when coupled with brawn, energy, and skill, has made money before and will make money again. No one would deny such an operation its righteous measure of glory but its return would be still greater if planning and cost control were mingled with those time-honored ingredients.

Costs can be *planned* in advance. They *can* be met if work is done according to plan. Profits on profitable operations can only accrue when volume absorbs all, or enough of, overhead. So volume also must be planned and the required tonnage must be sold.

The profit formula may be reduced to seven elements, each of which is essential to continuing success. These are given as the components in Fig. 1.

Plan for Control. Patterns must be established for a given kind of system. It is necessary to gear control plans to an integrated program so that estimates mesh with the cost accounting and the accounting ties in with budgets for burden absorption. Volume of sales, in turn, must exceed the break-even point. Controls must measure and guide the performance of every key man in your organization and their contributions to a winning team must be acknowledged with extra compensation.

When the whole organization works to a common goal, when the team moves forward along charted pathways, toward known horizons, with the forces of each activity following the forecasted plans of operation, success comes as no accident.

What Kind of a Cost System? Accountants and engineers often disagree with each other, and among themselves too, as to the best form for a system of cost accounting. There should be no disagreement, however, on the principles and the objectives. Foundries, whether large or small, require a system that accomplishes the five points listed in Fig. 2.

One point on the form or objectives of cost accounting should be particularly emphasized. Unless the plan parallels estimating and various controls for expense budgets and profit comparisons, there will be needless duplication of effort. The obvious solution is to design these reporting and control functions in closely correlated fashion to obtain the greatest good at the least cost.

The form and the complexity of a cost accounting plan may not fit any standard mold. The plan should be tailored to the needs, size, classes of work and to the understanding of key executives in each foundry. The latter stipulation is not a casual afterthought. A plan not understood is not used and a plan not used is worthless.

A Case Study in Costs. A little over a year ago a leading foundry came to this realization, to quote the words of its vice president and general manager: "Our estimating system for jobs brought to us by our sales department, we think, is quite outmoded. Likewise, our present cost system is in need of rather complete revamping."

This foundry's original line of products had

Figure 1

ESSENTIAL COMPONENTS OF THE PROFIT FORMULA

1. Have the know-how and the necessary equipment.
2. Supply and apply the requisite energy and skill.
3. Estimate the shop cost by foreseeing how you will do the job.
4. Use a cost system to tell you if you are meeting the estimated cost.
5. Develop a budget plan to project the total cost of any stated level of volume.
6. Employ a sales force that sells enough work to absorb the overhead, at a price that affords a profit.
7. Reward all who serve you in direct ratio to their coordinated accomplishments as a team.

Figure 2

OBJECTIVES OF A FOUNDRY COST ACCOUNTING PLAN

1. To record the cost of manufacturing in a manner adequate for the preparation of the financial statements.
2. To establish a list of cost accounts which renders (1) an accurate segregation of direct costs to product lines and pattern numbers and, (2) classifications of indirect costs arranged for ready distribution as overheads and burdens.
3. To provide a sound basis for estimating normal product costs for sales quotations.
4. To afford a practical means of profit control by providing daily and monthly cost comparisons between the estimates and the actual costs on any or all pattern numbers.
5. To serve as basis for maintaining a variable budget plan which projects and controls costs at all levels of operation.

Figure 3

STANDARD CHART OF ACCOUNTS
(Condensed)

I.	DIRECT LABOR	
	Account No.	Name and Explanation
	100	All direct labor.
II.	INDIRECT LABOR	
	200	All indirect labor not otherwise coded.
	201–3	Charging labor; metal control; tap, line, and slag.
	204	Supervisory and clerical.
	205–9	Miscellaneous other indirect
	210	Overtime and shift differentials.
III.	OPERATING SUPPLIES	
	300	Lining material.
	301–11	Various molding, core, and cleaning items.
	312–18	General department supplies.
	319	Miscellaneous stores.
IV.	OPERATING EXPENSES	
	400–3	Power; light; water; heat.
	404–11	Various repairs accounts.
	412–13	Compensation Insurance and Social Security.
	414–30	Miscellaneous other expenses.
	431–3	Group, hospital, and fire insurance.
	434	Depreciation—general plant.
	435	Taxes—general plant.

changed. Gradually the business of making cast iron pipe had swung into a broad line of chemical castings, thence into castings for the automotive, agricultural, and electrical fields. The cost system that once had provided a satisfactory basis for costing pipe and fittings was totally inadequate to the estimating and costing of the postwar lines.

A short engineering study revealed the weaknesses of the foundry's old plan. Cleaning room costs and much of the core costs were spread on a tonnage basis; the costs for alloys and special operations were difficult to allocate. Specific profits were more of a conjecture than a known fact.

The late O.P.A. had frozen an estimating formula that followed a restricted pattern and produced irrational results. By oversimplified additions in the estimating system for tonnage costs, for burden absorption, and for gross profit, the direct labor basis of the estimate had become a very small part of the selling price. Three high production jobs were tested to develop this point and the ratio of the selling prices to the direct labor portion of the estimates were found to be, respectively, 9 to 1, 13 to 1, and 35 to 1.

These figures merely verified the general manager's contentions. A new cost accounting plan was indicated, to be tailored to the foundry's need. With it a better estimating plan, designed to put selling prices where they belonged as soon as suspension of wartime regulations would permit, was required.

The new accounting plan was drawn up to meet the five basic objectives. It was tested and revised over a period of two months before its acceptance in final form. In parallel with it, a new estimating plan was outlined and forms designed for estimate preparation. An engineering report at the conclusion of the installation work recorded all the plans and procedures.

Explanation of the Accounting Plan. In a chapter of this report entitled, "Basic Outline of Cost Accounting Plan," the explanations read as follows:

"Cost accounting is the system of putting costs where they belong so that a true picture of unit costs may be built up for management. By the very manner in which costs are reported, segregated, and distributed, a cost accounting plan must be tailored to fit the situation at hand if management is to get the proper and desired portrayal of its unit costs.

"An understanding is essential, therefore, of the component parts that go to make up a unit cost before management can determine the correct alignment and application of its cost accounts. Direct costs must be reported accurately and charged directly to the unit of manufacture. All other costs must be distributed under the accounting plan so that each unit is properly charged with its just shares of the several classes of overheads and burdens.

"A cost accounting plan, in short, becomes a system of collecting costs into accounts and applying those costs to the unit of manufacture by clearly defined methods and formulas."

The outline of the company's new plan of cost accounting included the following topics:

1. Definitions of departmental costs, with clearly drawn distinctions between the productive, the service, and the general departments.
2. A numerical coding for identification and desig-

DIRECT LABOR AND ALLOY COST

PART NAME PART NO

CARD NO	NEW P&F		CORE		MOULDING		CLEANING		ALLOYS							
YEAR	EST.		EST.		EST.		EST.		EST.		EST.		EST.		EST.	
DATE	AMT	UNIT	AMT	UNIT	AMT	UNIT	AMT	UNIT	AMT	UNIT	AMT	UNIT	AMT	UNIT	AMT	UNIT
FWD																

Fig. 4—Cost card form designed to show direct charges.

nation of the various cost accounts and classifications.

3. Delineation of direct labor classifications.

4. Explanation of the compilation and distribution of melted metals costs, along with alloys.

5. The story of the treatment of scrap losses in the cost of manufacture.

Standard Operating Procedures. A condensed standard chart of accounts (Fig. 3) indicates the pattern of accounting along with the coding numbers set up. The numerical coding system employed prefix digits to designate the department or cost center wherein the cost was incurred. The code number suffixes identify and classify the type of expense.

The Procedures chapter of the report detailed the steps to be followed in the distributions of direct labor costs, departmental labor, stores charges, cost of melt, and departmental burdens. Outlined also were the preparation of monthly cost-of-production reports, product costs, monthly operating statements, and the treatment of work in process.

Compilation of Pattern or Symbol Costs. The direct costs were to be segregated or recorded by individual pattern numbers. To serve this purpose a cost card was drawn up, as may be seen in Fig. 4. The individual card is identified by the pattern number and provides basic information as to pieces run (total and good), direct labor, and alloy costs. Printed on both sides, this cost card has lines for recording 30 days of operations. For steady production jobs it becomes a monthly cost card. For short runs it is used for a job cost card.

Labor and Material Cost

The card has spaces for all regular direct labor accounts and for the alloys, plus three extra columns for special direct charges of any sort. Item costs for the day are posted and per-piece costs are computed daily and extended for the half-month and the month. At the top of each column is a space for the estimate figure so that the actual and the estimated item costs may be compared at a glance.

This card provides a concise and permanent record of all direct costs on every pattern number. It is readily available for use in completing the whole cost of manufacture for any job, for cost control work, and for re-pricing. The total cost, in comparison with the estimate, may be regularly computed on such specific jobs as the general manager may designate.

Plan for Estimating. With the new cost system installed and working, the third objective could then be achieved, that of providing sound estimates of costs for sales quotations. To make the fullest use of the accounting system, there must be a proper integration of accounting, estimating, and pricing. It is essential, therefore, that the estimating of a job and the subsequent accounting of costs be carried out in close parallel to each other.

The best estimate is one that reflects, in advance of quotation and sale, the best possible cost that can and will be attained when the order goes into the foundry. Direct costs on labor, iron, and alloys must be projected accurately, and without padding, to start the estimate on a proper basis.

The application of departmental overheads, tonnage costs, and plant burden must be on a normal-volume basis. The normal-volume values may not reflect the level of current overheads and burdens, for the latter costs naturally stand as functions of the immediate peak-or-valley condition of the foundry's operations.

Figure 5 shows the Production Estimate Summary sheet. On this sheet the direct labor values are entered first, productive department overheads are applied percentage-wise, per ton costs are added at normal volume levels, and iron and alloy costs are calculated on a yield basis. All plant burdens are included, at normal rates per ton, and finally the estimate for scrap losses is included. This sheet goes from the manufacturing to the sales department where the values for selling and administrative expenses and for profit are added.

This type of estimate summary sheet can be used to compile monthly or job cost comparisons if no other form is provided for that purpose. The five open columns afford room for the original estimate,

Fig. 5—Form drawn for production estimate summary.

Name of Casting _____ Pattern Number _____

PRODUCTION ESTIMATE SUMMARY

Customer _____ Date _____

Where & How Made _____

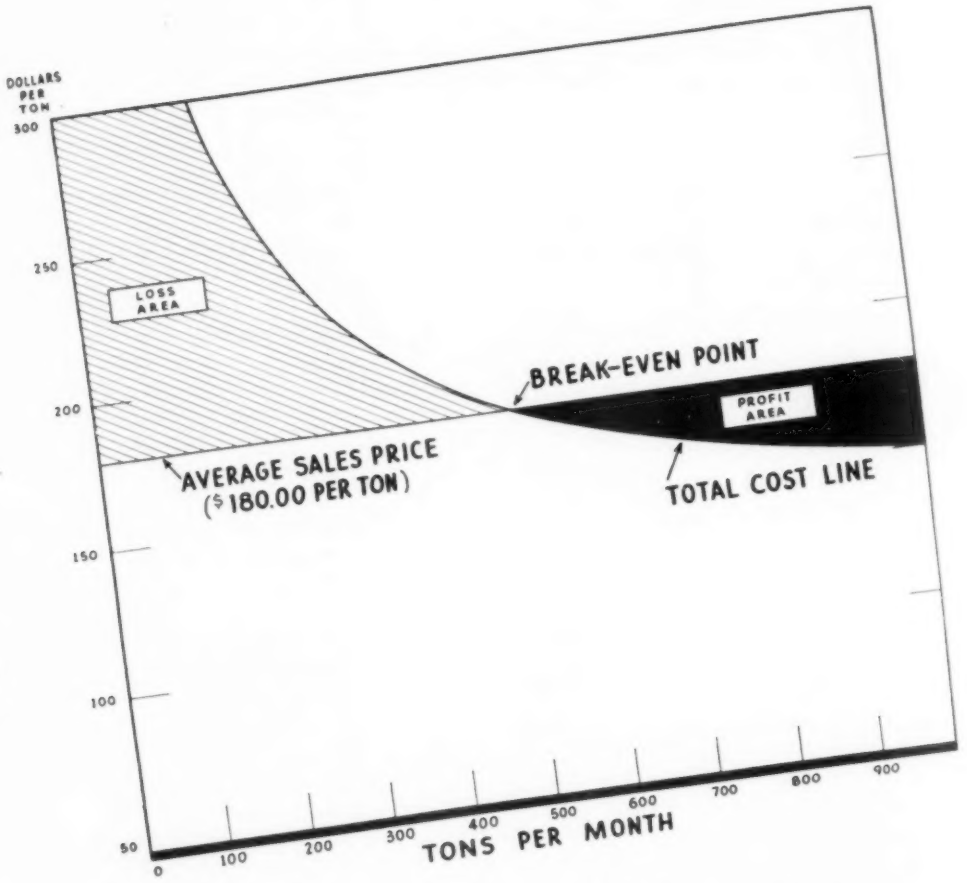
Casting Weight _____ lb Total Quantity _____ Pc. per Day _____ Type of Metal _____

		Direct Labor	Department Overhead	Materials	Plant Burden	Total Cost
Pattern Estimate	Total					
Equipment Estimate	Total					
Machining Estimate	Per Pc.					

	Items of Cost	Unit Value	Dates of Original and Revised Estimates													
1	Molding Direct															
2	Molding Overhead	%														
3	Core Direct															
4	Core Overhead	%														
5	Tumbling & Blasting	pT														
6	Cleaning Direct															
7	Cleaning Overhead	%														
8	Special Finishing Direct															
9	Finishing Overhead	%														
10	Iron Cost @ % Yield	pT														
11	Alloy Cost @ % Yield	pT														
12	Heat Treating	pT														
13	Plant Burden *	pT														
14																
15																
16	Plant Cost	Per Pc. Cast														
17	Plant Cost	Per Lb														
18	Combined Scrap Losses	%														
19	Total Plant Cost	Per Pc. Good														
20	Total Plant Cost	Per Lb														

Signatures _____ Date Fwd. To Sales _____

* Includes Shipping, Pattern & Flask Repairs, and General Foundry Burden.



Break-Even Chart
Showing Cost and Selling Price per Ton
Figure 6

for monthly or job cost comparisons, and for estimate revisions when they are required.

Budgetary Control Plan. Essential to the day-to-day and month-to-month control of costs and profits is a system of variable budgets. These must be so developed that they can be adjusted to compensate for changing classes of work and for the fluctuations in volume. Such budgets tell management in advance what costs should be for varying conditions of operations and enable the manufacturing head to measure equitably the cost control performance of his entire organization.

Variable budgets require an accurate unit of volume by which to measure the activity or work load of the different departments and plants. A satisfactory unit is the time-studied or standard hour of direct labor.

For the foundry without benefit of time-studied standards, either the estimated dollar of direct labor or the ton of castings may be the unit. However, it must be remembered that a ton of flywheels is far different from a ton of cylinder blocks. Therefore, when a ton is the basic unit, budgets may be adjusted by the use of a class-of-work factor.

A practical combination for any foundry to use for the control of time and expense is the comparison of total actual direct labor with the sum of the estimated or standard values. These should be linked with budgeted cost ratios to establish good performance expectancies for departmental indirect labor, supplies, and expense.

These projected ratios become, at normal volume, a part of the estimating formula, as has been previously mentioned. For control purposes, however, the periodic comparisons must be made to the actual volume level instead of the normal.

All items of plant-wide costs and burdens are as susceptible to budgeting as are the costs of the principal producing departments. Circumstances dictate that some costs are fixed and others, such as salaries and research, are essentially controlled by top management. But they may be determined in advance and accurately budgeted to any volume.

The total dollars of sales may be broken down or budgeted into items of plant cost, selling and administrative expense, and profit or loss. Profit or loss is the right term, because the variable budget may as readily project the expected costs below the break-even point for volume as above it.

In fact, detailed expense budgets should be established to cover the entire area between 30 or 40 per cent of capacity and 100 per cent or full capacity. If it develops that a profit can be made in the range between 40 and 50 per cent, it indicates that foundry costs are well under control.

Examples of the break-even or profit charts may be exhibited in two ways. Figure 6 is on a cost-per-ton basis and shows the total cost curve dropping from a peak cost value for low volume to cross the average selling price line at a break-even point of 50 per cent and on to a satisfactory profit in the higher ranges of production activity.

Figure 7 utilizes the profit-graph method of displaying projected profits. It shows the total dollars of sales finally catching up, as volume increases, with the

total dollars of cost at the 50 per cent mark. The upper section in black depicts the profit area.

Use Budgets for Supervisory Bonuses. The canny Scot, Robert Burns, must have had budgets in mind when he wrote, "The best laid schemes o' mice and men gang aft a-gley."

Budgets serve better as an incentive base than as a whip. Of themselves, they are not self-restricting. They serve as a guide but not as a panacea. But when coupled with a pay-off plan that rewards the supervisors for meritorious control performances, by sharing the greater-than-budgeted profits, budgets have a far better chance of success and far less possibility of going "aft a-gley."

Bonus funds should be provided by setting aside from 25 to 50 per cent of the extra profits realized by the collective attainment of cost reductions beyond the budgeted cost levels. Budgets to be used thus must be carefully set and must first provide a satisfactory and substantial profit at normal volume. Further, they must be projected down through a break-even point at a volume level where breaking even would be a distinct accomplishment.

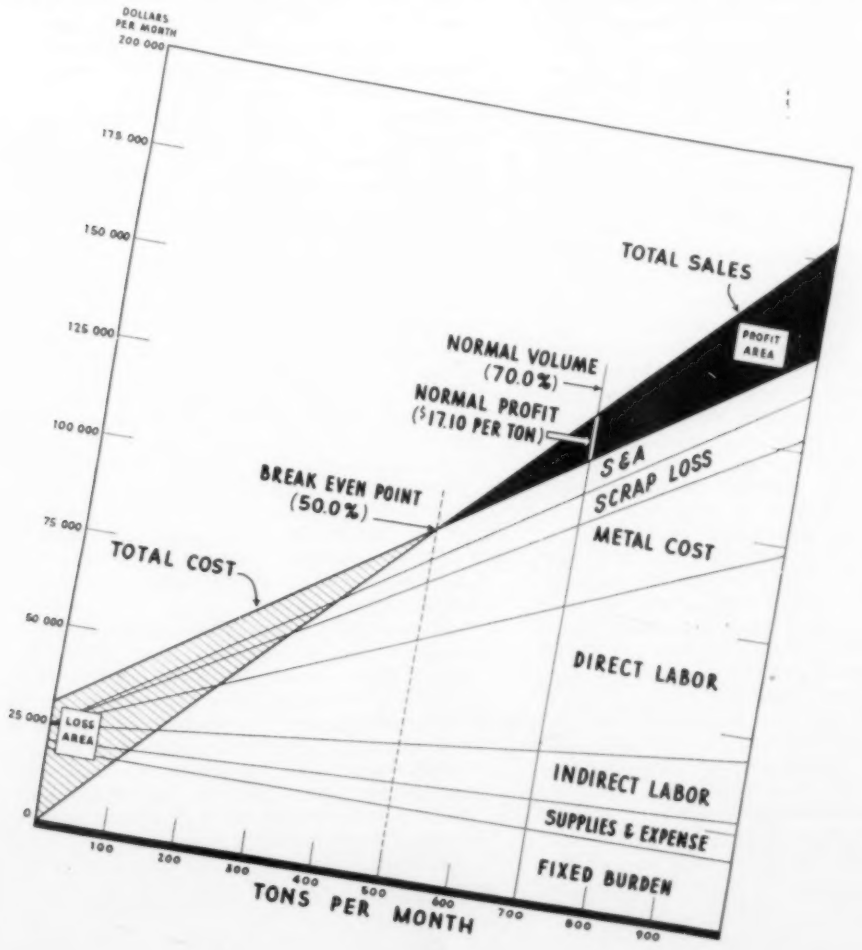
If the contemplated bonus is only for the plant supervisors and the office heads who serve them, the plant cost budgets may be separated from the overall or profit budget. Profits in this way need not be discussed with the shop. Instead, the supervisors may know the expected cost for any level and class-of-work combination and they collect their due share whenever the actual costs are lower than the budgets.

Bonuses may be applied departmentally or plant-wide or in a combination plan which embraces both. Budgeting must always be done by separate accounts and all expense accountability must coincide with control responsibility. Be wary, though, of a straight pay off plan for each separate department. Teamwork is a magic ingredient in the profit formula. If bonus plans are carelessly handled, the melting, the core, and the molding departments may make great showings while the cleaning room becomes burdened with castings impossible to finish satisfactorily.

Control of Defective Castings. In the matter of defective castings, the bonus plan must penalize and reward for the showing in the scrap record. Scrap losses may be budgeted as readily as they can be segregated as to causes and to department inefficiencies. Daily scrap reports, segregation analyses, and comparisons to scrap bogies, are important steps in licking the scrap losses. Every reduction in scrap is pure gain.

The scrap costs in a plant may well be so great that a full-time man of assistant superintendent stature can be profitably assigned to the problem. His day-to-day efforts conceivably can take 30 to 60 per cent off the cost of defectives if he is the right sort and gets the right backing. Before rejecting this idea, a check to see what scrap losses really cost in total for 1946 should be made.

The problems of making good castings at the right cost and selling them for a profit will always be with the castings industry. Cost control is never done, but with all the simple and tested tools of control at work, management may rest assured that there is a far greater chance of making profit.



Profit-Graph
Showing Monthly Costs, Sales and Profits
Figure 7

DISCUSSION

Chairman: R. L. LEE, Grede Foundries, Inc., Milwaukee.
Co-Chairman: G. TISDALE, Zenith Foundry Co., Milwaukee.

MEMBER: In Fig. 5 there are some extra columns labeled "Revised Estimates." Does at least the first column after the original estimate represent the actual cost as a check against the estimate, or are those columns all just estimates and revised estimates?

MR. GEORGE: Figure 5 has various uses. You can use it for monthly comparison for the next four months, or you can draw up a similar sheet that will give you 12 months, so that every month you can look at every item across your sheet. And alongside of that estimate or standard, you can have every actual cost, and can see how you come out. If you want to use that sheet for estimating only, there are columns for five estimates. Your second estimate, instead of being guesswork, can be predicted on known costs.

MEMBER: What part is estimated on a tonnage basis?

MR. GEORGE: That again depends on your business and what you put into your burden. One large company I know puts depreciation back to the department. Another large company, equally successful, puts it into this plant burden. I have not attempted to define just what we put into that plant burden, but, you have to use care in spreading burden against any class of work.

Let me give you an illustration. We had different classes of work. We had our old line of cylinder heads and cylinder blocks, where we did not need all the controls in metallurgy that we needed in high alloy steels. It was not fair to load that old line business with the same burden we loaded the new steel business. We did not use the same overhead. We used the same estimating sheet, but in that particular plant we had four different overheads for four different classes of work. But we predicted that at the start of the year, that we expected to get so many tons of each kind. When we estimated on steel castings or the like, they had to carry a lot more of the overhead than the old line because in the old line we were competing with foundries that did not have the controls we had put in ostensibly for our other lines.

A. E. HAGEBOECK:[5] Foundrymen are more cost minded today than they ever were. I have been in this cost work for 25 years, being chairman of this American Foundrymen's Association committee many years ago. It seems that during the last 18 months the foundryman finally has realized that he must know his costs. He is interested in costs today while ten years ago he was indifferent, saying: "If we knew what our castings cost, we could not sell castings because they cost too much."

MR. GEORGE: Is not that why a lot of foundries went bankrupt then?

MR. HAGEBOECK: It is a matter of record that no foundry that had good cost records, cost control, and used this information intelligently has ever gone into bankruptcy. On the other hand, we have many jobbing gray iron foundries, who do know their costs, who do have cost control, and who have very successful operations. This type of foundry improves their facilities so as to meet the exacting requirements of modern-day specifications and, incidentally, are improving their working conditions to prove the A.F.A. slogan, "The foundry is a good place to work."

MR. GEORGE: The thought that impressed me was that you get your cost accounting above the table, out in the open and use it. The cost accountant that figures his costs down to the penny is deeply concerned because he is ten cents or ten dollars out of balance and then locks the books in the safe is not rendering any service to the foundry whatsoever. He may be rendering a service to the Income Tax Division of the Treasury Department; but if you are going to keep cost figures, keep them in the open and tell the men what their costs are. Show them the costs, because the minute you throw some light on costs that are bad, they are going to improve.

MEMBER: You are absolutely right in connection with the necessity for control of spoilage or scrap. However, the application of that loss on individual castings sometimes can be quite a problem.

In connection with the manufacture of cores, how do you determine what the scrap loss is on the cores, particularly when they manufacture a core and then there may be greater or less time interval according to the scheduling of the using of that core at the time of the pouring of the metal? Sometimes that disappears. How do you get the loss?

MR. GEORGE: If you were really keeping purchasing costs, you probably would put it under core material. You put that cost of core material against every job, so whether you scrapped it or not it was already charged in.

Another way is to add five per cent—if five per cent is about the amount of cores you scrap over the months or over the years—to deliberately boost all your expected core cost by five per cent to make sure that each estimate carries the total core cost it should carry. Because in the long run, you are going to be scrapping, you might say, five per cent of your cores. You may find, if you check back, that you are scrapping seven per cent or three per cent.

MEMBER: Five per cent is very low.

MR. GEORGE: I am thinking of one high production plant where five was enough. But if it has to be ten, make it ten.

CHAIRMAN LEE: Make it 20.

MR. GEORGE: But put it where it belongs from your own study, and add it to every job. Otherwise, you better go to costing the core material used in each job separately, and then it is automatically put there.

MEMBER: In our case, every dollar of direct labor expended on any customer's account is charged to that customer. All the time we spend on making that core is charged to him. At the end of the month, sometimes we ship those castings out, he gets credit. Our cost is actual cost. We add around 20 per cent on our estimates to make sure they cover everything.

MR. GEORGE: Do you actually let them run 20 per cent, or do you take steps to reduce it below 20 per cent? That is the real purpose of cost control, so you know you have a higher cost and do something about it. That is what you want a cost system for, because if there is a 20 per cent throwaway, you try to find some way to save part of that 20 per cent, if you can.

CO-CHAIRMAN TISDALE: On estimating burdens, what do you consider normal?

MR. GEORGE: There you get into a normal activity. In order to stay in business, that has to be somewhere above the break-even point. As these Figs. were drawn, they included selling and so forth, breaking even at 50. If it can be less than 50, you ought to be in a highly profitable position and glad of it. Normal is not too well understood a word, but, nevertheless, it represents a volume of business at which you want to price your work. You want to have a reserve, so that when you sell at full capacity, you are over-absorbing the burden or the overhead of your company. When you sell at normal, you are absorbing it all and your profit is exactly what you expect it to be, providing you meet the direct costs you expect to have. But when you sell at less than that, you realize that you are under-absorbing. You are not carrying all your burden into cost.

I was in a foundry once that in one year had had the largest volume they ever had. The burden was $17 a ton. The next year they saw themselves getting into low volume, and they raised the burden for estimating to the actual cost of $24 a ton, and volume kept going lower and lower because they were pricing themselves out of the market. They ended up selling 33 per cent of the previous year. If they had carried the same burden or the proper burden on all goods, instead of selling 33 per cent of capacity they might have sold at 45 or 55 per cent. In other words, they might have broken even. Instead of that, they sustained quite a loss for that given year, and it was largely the fault of the accounting plan, for determining costs and selling prices.

J. E. SATTERLEE:[1] What are normal overhead rates for core room and cleaning room?

MR. GEORGE: I think everybody in the room could answer it and give a different answer. For instance, I would not know whether you have a core blower. The minute you put in a core blower, you boost direct maintenance.

CHAIRMAN LEE: The important point is that you boosted the percentage of indirect labor in relation to the direct labor.

MEMBER: You do not know how much burden you have on the core department. You cannot answer it.

MR. GEORGE: If you got answers from different ones here, you

[5] Frank Foundries Corp., Moline, Ill.

[1] El Dorado Foundry, Inc., El Dorado, Kansas.

might get answers anywhere from 50 per cent to 500 per cent, and each one would be right for his particular case.

CHAIRMAN LEE: Frankly, you might just as well ask what would be the proper price per pound to charge for castings.

JACK BEAN:[2] How would you go about determining, for estimate purposes, the direct labor cost of repair welding?

MR. GEORGE: There again you better go back to your own experience and see what per cent of your work you are welding. Granted that the worse your castings are, the higher your number of welds is, that you would like to salvage. If you had a good scrap record, you are accomplishing it by welding or by making good castings in the first place. But you must go back to your own experience and see the total welding cost for a given class of work. If it is, say, $2,000 and you have 1,000 tons, you can come up with a $2.00 cost per ton for welding in that class of work, and that is about your current average. Whatever that figure is, do not use $2 per ton if that happens to be it, from now on. It might have to be $3 or $4 next year, or it might have to be $1; but keep it within the realm of your experience and watch it and you can determine your own figures that way.

CHAIRMAN LEE: In one of our plants, we have a welding salvage account, and whenever repair welding is done on a casting, we charge that account with all those expenses and credit it with the selling price of the salvaged castings, and you can easily tell at any time whether you are making a profit. If you want to reduce it to individual castings, you can do that. Or if you have it on an overall basis, if it shows a credit, all right. If it is not, then you had better start making castings that do not need welding.

MEMBER: Could you tell me what your experience has been in percentage of scrap encountered in accounting systems from cast iron and steel melted in electric furnaces?

MR. GEORGE: That question would be like answering your overhead. Frankly, I am no foundryman. I gravitated into foundry work when I was 32. I had several years of it, and I am out of it now. In that time, I saw scrap records vary from 2 to 17 per cent. I can think of those two figures. That does not tell you anything when you have such a large variation. I suppose steel foundry castings vary widely. It all goes back to what you are making. If you are making wheels or something chunky, you may have a lower scrap loss, and if you are making intricate core work or something liable to cause scrap, I am not enough of a foundryman to give you an answer on that. But each in his own experience should compare with a competitor on a similar situation and not be satisfied with anything but the lowest you can get it down to. I think you can reduce your scrap loss in any plant by having daily scrap meetings, daily scrap reports, assigned causes, and so on. If you expect to run a given block with a three per cent molding loss, watch the scrap count every day, whether you are running three per cent at the end of the day or not. The more you watch it, the better you will find it. To do a good watching job, you need a full time honest man whom the foreman and the metallurgist respect, to get at the seat of the trouble, not to place the blame but to eliminate the cause.

MR. HAGEBOECK: Scrap in 27 foundries in one of our groups ran from 0.67 to 14.6 per cent, averaging 8.6 per cent.

MEMBER: Is that by weight or by number of pieces?

MR. HAGEBOECK: That is by weight. The total good castings plus the pounds of scrap castings added together is 100 per cent. That is divided into the pounds of scrap.

M. R. VIPOND:[3] What would you consider a normal amount for administrative and selling expense in job foundries?

MR. GEORGE: I would not consider anything *normal*. I have known some companies that tried to farm out their selling at a five per cent commission basis. They were not successful. Maybe they have tried it at 10 per cent. I knew one large foundry that operated at a surprisingly small selling expense because the work came to them. They did not have to go after it. To get a normal, you have to define the kind of work you are talking about, size of business, the location, who your customers are, whether you are jobbing or production, whether you have a reputation or if you are trying to build up a reputation. I think every one of these factors would vary it.

R. FICKENWORTH:[4] We make castings from four ounces to four tons. The cleaning room is entirely on the hourly basis, and when it comes to figuring out the cleaning expenses on, say, a four-ton casting as compared to a four-ounce casting, how many groups would you suggest or should you break that up into? It is not fair to have one cleaning cost per pound. Do you have any suggestions as to just how to break the cleaning room costs down where you do not have them on piece work?

CO-CHAIRMAN TISDALE: You keep your cleaning cost a direct charge basis or part of your burden.

CHAIRMAN LEE: From the tenor of your conversation, I guess it would be at least thirty to forty. I am honest about that, because you will have an extremely wide variation between your four-ounce casting and the heavy one. If you investigate your shop, you will find that at least 50 to 60 per cent of your work will run in probably 30 to 40 classifications. If you establish pound costs on that basis, it would not be perfect, but it would be better.

MR. FICKENWORTH: One to five pounds, five to ten pounds, and so on?

CHAIRMAN LEE: No, I would start it with four ounces.

MR. FICKENWORTH: Anything under a pound?

CHAIRMAN LEE: Start at four ounces to one pound, one pound to two pounds, and then begin to make larger weight intervals. Make it half pound, pound, two pounds, five pounds, ten pounds, about 20 or 25, then 50 to 100, 100 to 250, 250 to 750, and make the intervals wider, because your variations as your weight increases will be less than with the light castings.

MR. FICKENWORTH: Then consider the direct labor, as the grinding, tumbling, and sand blasting, and then the rest, all indirect labor?

CHAIRMAN LEE: If you are paying day work, all the way through, I do not see how you can distinguish, unless you keep time on the different jobs.

MR. FICKENWORTH: This is jobbing with all types of jobs. Do you think it would be better on piece-work basis?

CHAIRMAN LEE: Certainly. If you get on piece work, then you apply the majority of your cleaning costs on a direct basis, and then establish an overhead burden for that. But you are entirely on a day-work basis. The first thing I would advise you to do would be to get on a direct piece-work basis for as many of the operations in the cleaning room as you can. Then apply your direct labor on a direct basis and the rest of your labor and expenses and so on as a percentage of the direct labor.

MR. FICKENWORTH: Have you had any experience in classifying your cleaning costs into squeezer, rollover, and floor work and figuring a cleaning room cost per pound on each group?

CHAIRMAN LEE: You have a wide variation in the work, and I do not think a distinction on the basis of molding equipment would help you very much. I do not see how it could. It must be on the basis of plain, intricate, or medium castings, and then go on the basis of your different weights.

MR. HAGEBOECK: Chairman Lee has given you the real answer to cleaning room costs. The best method is to obtain direct costs for grinding, shipping, sandblasting, any work that can be charged direct. All other items of cost in the cleaning department are to be calculated as a percentage of the direct cleaning labor.

Most foundries, however, find it impractical to attempt to gather all of these direct cleaning costs, and, as a result, we offer three alternate methods.

First determine the average cost per pound for total cleaning room costs and apply this on a pound basis. Second, determine the total cleaning room cost on a percentage basis against the molding direct labor. The third method was first suggested by Ralph Lee, which, in our judgment, is by far the best procedure. Under this method, the total cleaning room cost is determined on a percentage basis against the combined mold direct and core direct labor, and this percentage figure is used against the mold direct and core direct on any given job. We all agree that where possible direct cleaning labor should be determined plus a cleaning room overhead. Where this is not possible, then we recommend the cleaning room total cost against the combined mold and core labor.

[2] Michigan Steel Casting Co., Detroit.
[3] Aluminum Company of America, Cleveland.
[4] C & G Foundry and Pattern Works, Inc., Indianapolis, Ind.

SEGREGATION

IN SMALL STEEL CASTINGS

By H. F. Bishop and K. E. Fritz*

ABSTRACT

Pronounced segregation of carbon, and to a minor degree the other alloying elements, has been found in small steel castings adjacent to the riser contacts. This segregation is indicative of potential shrinkage and occurs when restricted riser contacts are used. Examples of segregated zones are shown in which the carbon content is nearly twice as great as the carbon content of the base metal.

It is concluded that in castings where such segregation occurs, the riser contact consists of solid low carbon dendrites surrounded by liquid high carbon metal during the latter stages of solidification. To supply the last feed metal required by the casting, the interdendritic material is drawn into the casting to form the segregated zone.

SEGREGATION IN CAST STEEL is usually associated with massive sections such as are found in large ingots. It has been found however that severe segregation of carbon, and to a lesser extent of the other alloying elements can, under certain conditions, occur in castings weighing but a few pounds. This type of segregation obviously arises from different conditions than the segregation in ingots.

Segregation in steel castings can be the cause of hard spots encountered in the machining operation. It can be the cause of localized quench cracks or weld cracks which occur unexpectedly, and it can be the cause of cracking during flame cutting operations in carbon and low alloy steels which normally would not crack.

Molding and Casting Procedures

The castings described in this report were poured into air dried molds. A high frequency induction furnace was used for melting and the charges consisted of foundry returns. Additions of ferrosilicon and ferromanganese were made to the furnace and, when necessary, high carbon pig iron was added to raise the carbon content of the heat. Final deoxidation was accomplished by adding 0.10 per cent of aluminum to the ladle. The castings were poured at temperatures within the range of 2900 F to 3000 F.

*Published with permission of the U. S. Navy Department without endorsement of statements or opinions of the writers. Mr. Bishop is on the staff of the U. S. Naval Research Laboratory of the Office of Naval Research, Washington, D. C., and Mr. Fritz was formerly on the same staff and now metallurgist, Bucyrus-Erie Co., South Milwaukee, Wis.

Experimental Work

Segregated zones in small steel castings and a clue to their formation were found accidentally in experiments with graphite rods in blind risers, the use of which is described by Vosberg and Larson.[1] Briefly, Vosberg and Larsen claimed that if a graphite rod is inserted in a blind riser, the rod will partially dissolve when it comes in contact with molten metal to form a high-carbon low-melting-point steel in the riser. Since the fluid life of the riser metal is thus lengthened, smaller risers can be used to obtain higher yields in castings.

The objection to the use of graphite rods is that, if too large a rod is used, there is a possibility that the high carbon metal formed in the riser will feed into the casting to form hard spots.

In tests made to investigate the claims of Vosberg and Larsen, one and one-half inch thick plates were cast in a vertical position and were fed by blind risers attached to the bottom of the plates as shown in Fig. 1. Various sizes of rods were placed in the risers, and the castings were then sectioned and etched with a 10 per cent ammonium persulphate solution to disclose the distribution of the high carbon metal. Invariably, even when small rods were used, a high carbon zone was found in the casting at the position where the head was attached.

Since the occurrence of segregation at the riser necks when ¼-in. diameter rods were used was entirely unexpected, several test castings were made which were identical with the one described above except that conventional sand cores were used in the risers instead of graphite rods. When these castings were sectioned and etched, they too had segregated zones, even though there was no outside source of carbon available to dissolve in the metal.

Figure 2 is a photograph of etched sections of two cast plates made as shown in Fig. 1. The section on the left in the photograph was fed by a riser containing a sand core and the one on the right by a riser containing a graphite rod ¼-in. in diameter. Segregation is equally severe in both cases, and this phenomenon, which at first seemed to discredit graphite rods as an



Fig. 1—Plate casting with blind riser attached

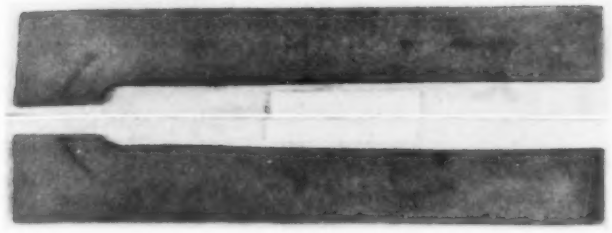


Fig. 2—Etched section showing segregation in vertically cast plates fed by (above) blind riser with sand core, (below) blind riser with graphite rod

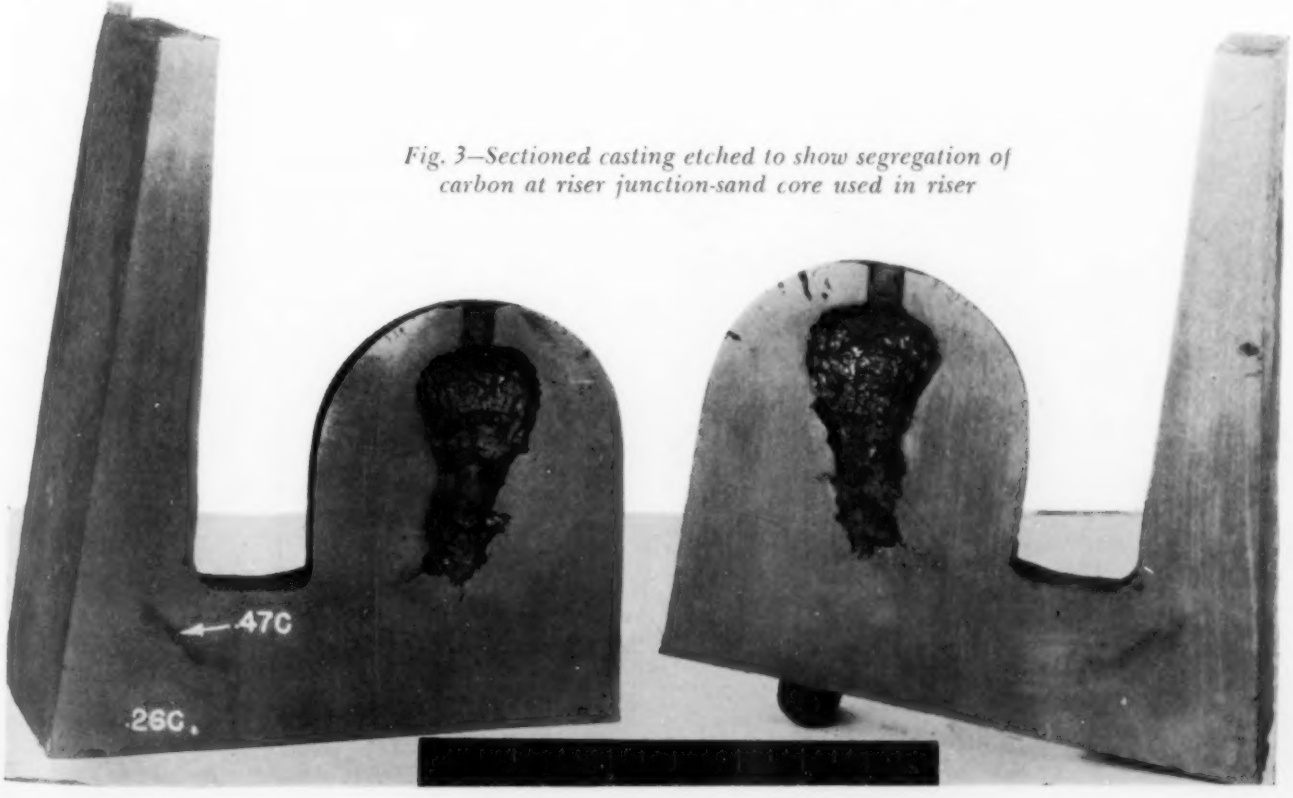
aid to more efficient feeding, was found to occur with or without a graphite rod in the riser.

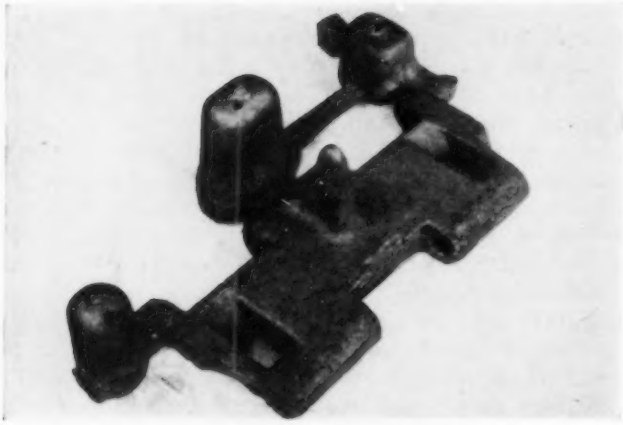
In Fig. 3 is shown another example of segregation in a plate casting which was fed by a blind riser kept open to the atmosphere by means of a sand core. This photograph shows the casting and riser as sawed through their centers with the segregated area at the bottom of the casting disclosed by an ammonium persulphate etch. Drillings taken from the segregated area contained 0.47 per cent carbon whereas the base metal contained 0.26 per cent carbon.

During the war a company engaged in the manufacture of tank treads was encountering hard spots in the treads during machining operations and was also finding cracks where the risers had been burned from the casting. Several of these castings were sent to the Naval Research Laboratory for inspection. It was found that these treads, which were made of a steel containing 0.35 per cent carbon, 1.50 per cent manganese, and 0.35 per cent molybdenum, had highly segregated areas in the sections adjacent to the risers. Top and bottom views of one of the castings with the risers attached are shown in Fig. 4.

These castings were fed by blind risers containing the Williams sand core which permitted access of atmospheric pressure to the riser. The lugs to which the risers were attached were cast solid, while holes were cored in the lugs on the opposite side of the casting. Since the most difficulty was encountered in machining the hole through the central mating lug, a thin section through this area was removed for examination. This section was found to be radiographically sound. When this specimen was etched with ammonium persulphate a thin segregated zone was found leading from the riser contact and extending across the lug as shown in Fig. 5.

Fig. 3—Sectioned casting etched to show segregation of carbon at riser junction-sand core used in riser





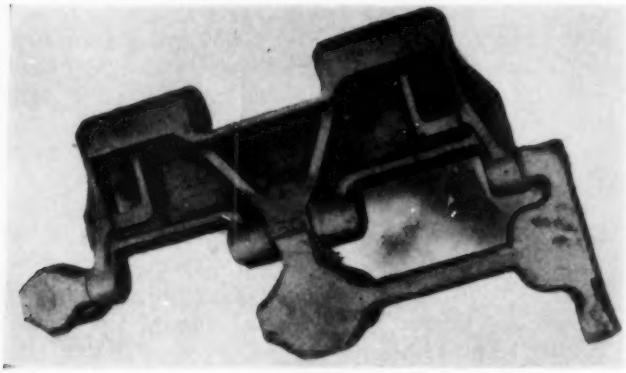
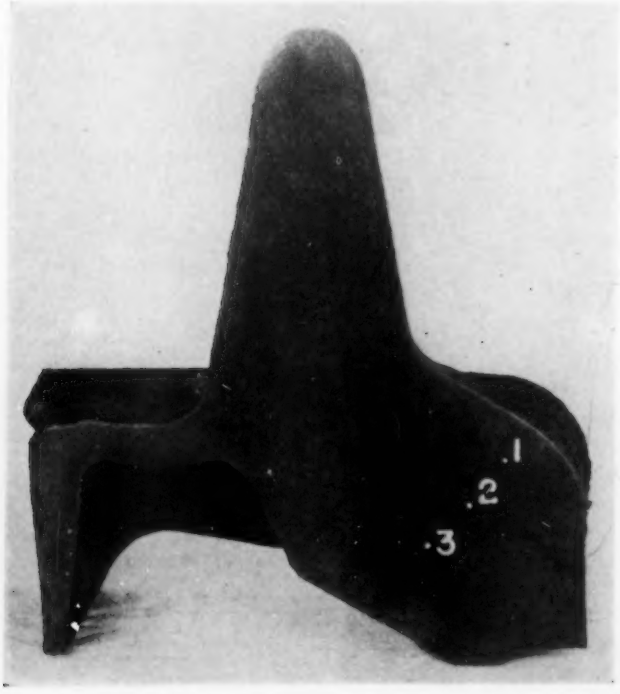


Fig. 4 (above)—Tank tread casting with blind risers attached

Fig. 6 (below, right)—Hardness survey across segregated zone in tank tread casting

Fig. 5 (below)—Segregation in section through center of tank tread



From a hardness survey made across the segregated zone with a Vickers hardness tester, it was observed that the base metal in the as cast condition had a hardness of 220 Vickers and the segregated zone had a maximum hardness of 300 Vickers. The hardness curve across the segregated area is shown in Fig. 6.

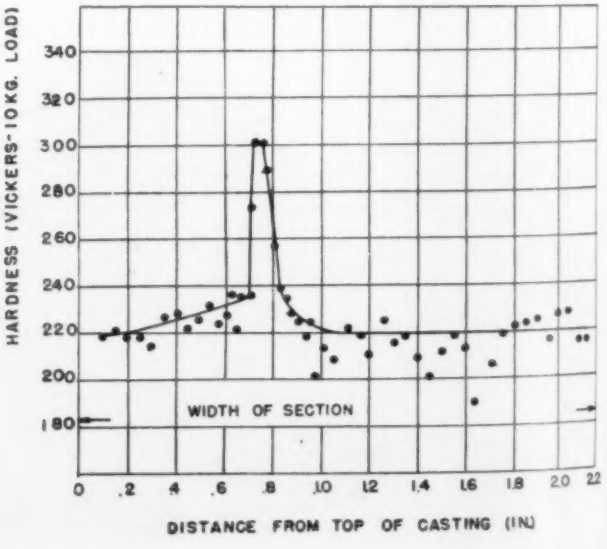
In determining the variation in carbon content between the segregated area and the base metal, a specimen having a thickness of 3/16 in. was water-quenched from 1650 F and surveyed for hardness. Since the maximum quench-hardness that can be obtained in steel depends only upon the carbon content, it was possible to determine the carbon content of the specimen by referring to the curves of Clark and Kowal, described in *Metals Technology*, Jan. 1944, which show carbon content plotted against quench-hardness. It was found that the carbon content of the segregated zone was 0.57 per cent while the carbon content of the metal adjacent to this zone was 0.37 per cent. Drillings taken from the casting some distance from the segregated zone contained 0.33 per cent carbon. The segregated zone thus contained at least 0.20 per cent more carbon than did the remainder of the casting.

From spectrographic analyses made at the positions indicated on Fig. 5, it was determined that there was a segregation of other alloying elements as well as carbon. The results of these analyses are as follows:

	Position 1 per cent	Position 2 (Segregate), per cent	Position 3, per cent
Manganese	1.55	1.83	1.40
Molybdenum	0.32	0.43	0.30
Chromium	0.081	0.097	0.077
Nickel	0.08	0.09	0.08
Silicon	0.70	0.81	0.70

The segregation of sulphur in this section is shown by the sulphur print of Fig. 7.

Since the hole which must be machined in the mating lug passes through the segregated zone, it is not surprising that hard spots were encountered in machining, and since the segregated zone extends to the riser connection, the occurrence of cracking in burning the riser from the casting might be expected.



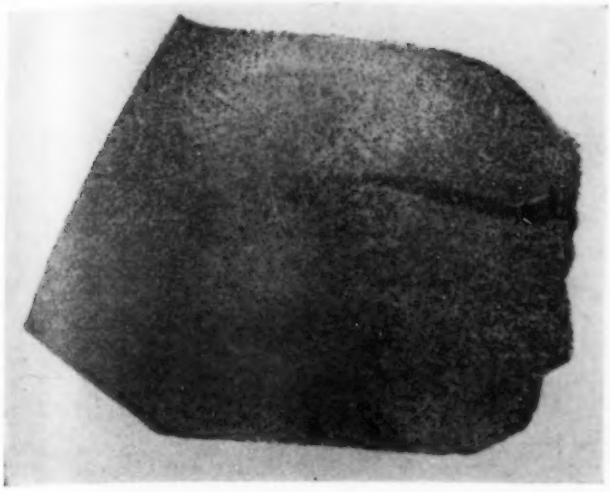


Fig. 7—Sulphur print of segregated zone in tank tread casting

Another example of segregation of this type was found in small bracket castings which were risered as shown in Fig. 8. Two sizes of riser contacts were used on these castings. The riser contacts were etched with a 10 per cent ammonium persulphate solution which disclosed the segregated areas seen in Figure 9. Both of the castings were sound, but it is apparent that with the oversized contact the degree of segregation is reduced. Figure 10 is a photograph of an etched section obtained a half-inch behind the surface of the flange-face of the casting shown on the right of Fig. 9. It can be seen that the segregate is still present at this point. The compositions, obtained by spectrographic analyses, of the top segregated area of Fig. 10 and the base metal are as follows:

	Segregate per cent	Base Metal per cent
Manganese	0.75	0.70
Silicon	0.60	0.60
Chromium	0.28	0.25
Molybdenum	0.35	0.30
Nickel	0.31	0.29



Fig. 8—Bracket casting and riser

Photomicrographs taken from the base metal through the segregated zone of this specimen after it was annealed at 1700 F for two hours are reproduced in Fig. 11 at a magnification of about 50 diameters. Figures 12 and 13 are photomicrographs at 500 diameters of the base metal and segregated area respectively. Since the amount of pearlite in steel is dependent upon the carbon content, these photomicrographs indicate the degree of carbon segregation present.

It is to be noted that this segregation is apparent even after the specimen had been annealed. It has been observed in other castings also that an anneal or a normalize and temper treatment has little if any tendency to disperse the segregated elements.

From the preceding illustrations it is evident that segregation occurs near riser contacts which have a cross sectional area smaller than that of the riser, and that the magnitude of segregation is increased as the riser connection is reduced.

If a small riser contact is conducive to the formation of segregation, knock-off or Washburn riser necks should present ideal conditions for segregation of this type to occur. Figure 14 is a photograph of etched sections of two cylindrical castings 4 in. in diameter and 6 in. high and the Washburn risers which fed them. The riser necks on the castings were 1½ in. and 2 in. in diameter and were formed by cores having a thickness of ½ in.

Spectographic Analysis of Segregate

The top ½ in. of each casting was removed for analyses and hardness surveys, and these segments are not shown in the photograph. A slight shrinkage cavity was found in the casting to which the smaller neck was attached while the other casting was sound; severe segregation appears in both cases. Results of spectrographic analyses taken from the solid casting just below the neck are as follows:

	Segregate per cent	Base Metal per cent
Manganese	0.80	0.75
Silicon	0.40	0.45
Chromium	1.12	1.01
Molybdenum	0.62	.055
Nickel	0.32	0.30

The carbon content obtained on drilling was 0.25 per cent in the base metal and 0.44 per cent in the segregated area. The plot of a hardness survey taken across a section of the casting just below the neck is shown in Fig. 15.

In the two castings of Fig. 14, the cores were made of oil sand, and a carbon-free pipe-eliminator was used on the risers. There was a possibility that the pipe eliminator might have contained carbonaceous material which was dissolved in the metal and was carried into the casting, and there was also the remote possibility that the oil binders from the Washburn core, being carbonaceous, might have caused an increase in carbon content of the metal. To determine if carbon was obtained from these sources, a series of cylindrical castings 4 in. high and 4 in. in diameter was poured and the castings were fed with Washburn risers having neck diameters ranging from 1 to 2½ in.



Fig. 9—Riser contacts of bracket castings etched to show areas of carbon segregation

The cores were bonded with cement and were made to contain only sand, cement, and water with no carbonaceous material present. No pipe eliminator of any kind was used on these castings. A photograph of this series of castings and risers is shown in Fig. 16. The risers were sawed from the casting and both sawed surfaces were ground and then etched with ammonium persulphate solution.

From this photograph it would appear that the castings with the 1¾-in. and 2-in. diameter necks have more segregation than those with the smaller necks. This was not confirmed however when the castings were sectioned as shown in Fig. 17, for segregation of a more severe nature was found within the castings fed through smaller necks. The degree of segregation appears to vary inversely as the diameter of the neck. Only slight segregation occurs in the casting to which the 2½-in. diameter neck was attached and no segregation at all occurs in the casting fed by the riser in which the contact completely covers the casting.

A ¼-in. section from the top of the casting fed with the riser having the 1¾-in. contact, as shown at the lower right, in Fig. 17, was removed for carbon determination by means of the quench-hardness method. The carbon content of the segregated zone was found to be 0.47 per cent while the carbon content of the base metal was 0.25 per cent, a difference of 0.22 per cent of carbon. The complete composition of the heat was 0.25 per cent carbon, 0.60 per cent manganese, 0.43 per cent silicon, 0.26 per cent chromium, 0.20 per cent molybdenum, 0.11 per cent nickel and a balance of iron.

Figure 18 shows the carbon contents obtained on drillings at various positions in the casting and riser having the smallest connection. Although severe segregation is shown in this manner the maximum amount of segregation may not be disclosed because of the manner in which the sample was taken. Each value of

Fig. 10—Segregation in bracket casting shown on right of Fig. 11 a half inch from surface of flange

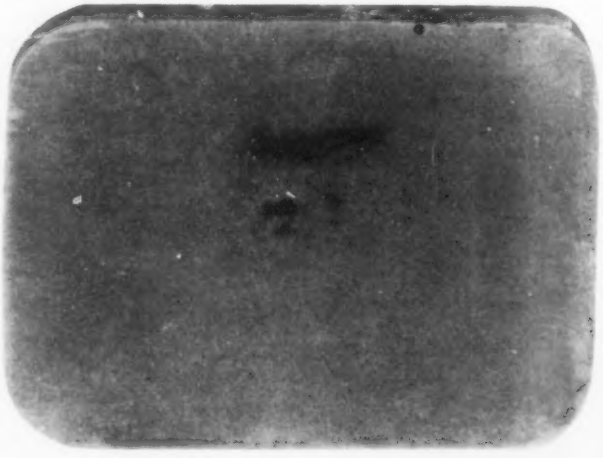
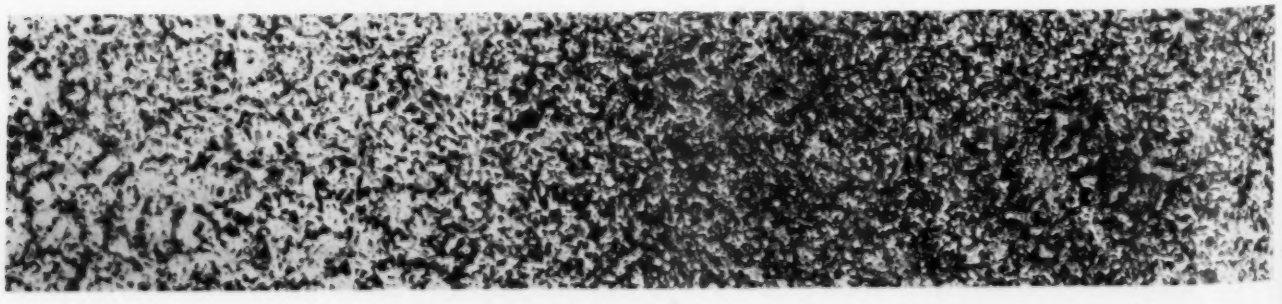


Fig. 11—Photomicrograph through segregated zone in Fig. 10 showing structure after annealing at 1700° F. for 2 hr. Approx. 50X.



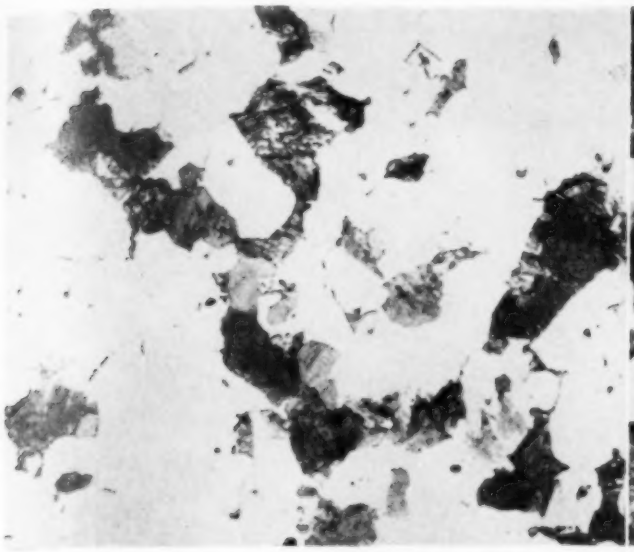


Fig. 12—Photomicrograph of base metal in annealed bracket casting. Mag. 500X.

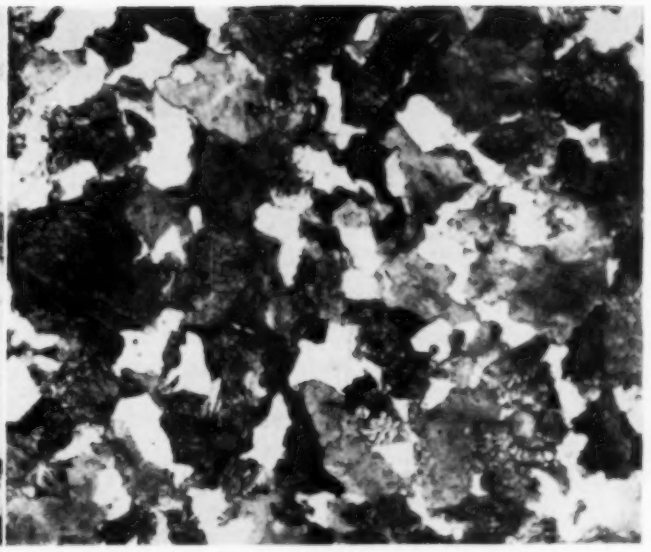


Fig. 13—Photomicrograph of segregated metal in annealed bracket casting. Mag. 500X.

carbon content shown on this figure is the average of the small drilled volume which, when it is taken from a segregated area, undoubtedly includes some of the lower carbon base metal.

The series of castings discussed above indicated that this type of segregation is entirely a function of the casting design and the mechanism of solidification, and that carbon is not absorbed by the metal from outside sources. Nevertheless, in all subsequent experiments, no pipe eliminator was used on the risers.

The chromium, nickel and molybdenum which were present either as intentional alloying elements or as residuals in all the castings described thus far had little if any influence on the occurrence of segregation. A series of test cylinders like those shown in Fig. 16 was cast from a heat made of Armco iron melting stock which contained no more than 0.01 per cent each of chromium, nickel and molybdenum. The patterns and degrees of segregation were found to be very similar to those of Fig. 16.

Segregation has not been found in castings fed by conventional open risers in which the connection to the casting has a diameter approximately equal to that of the riser itself. When the riser to casting connection is restricted as is sometimes done, conditions become favorable for segregation.

Figure 19 is a photograph of a sectioned and etched casting and riser having a slightly neck-down connection. Slight segregation is disclosed at the neck by the ammonium persulphate etch. Analyses of drillings

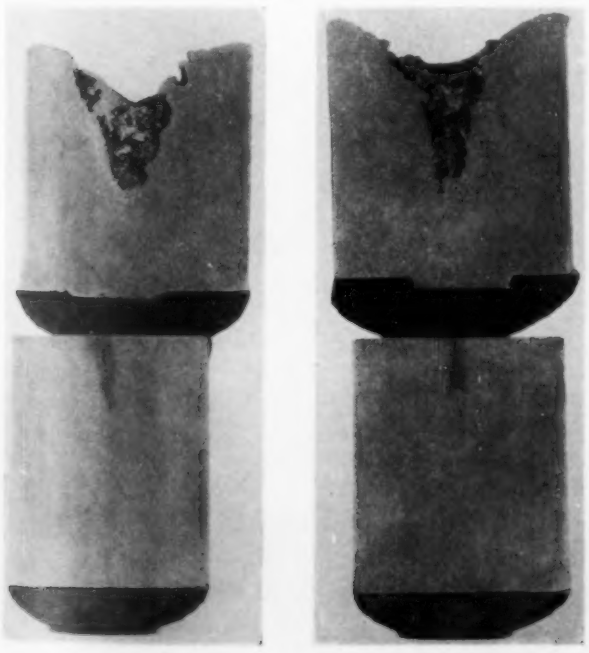
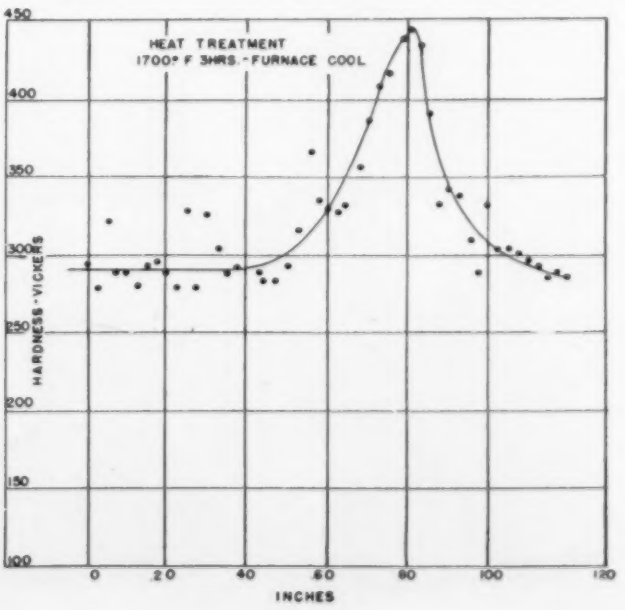


Fig. 14 (left)—Segregation (dark areas) beneath knock-off risers in two castings, 1½-in. neck (left) and 2-in. neck (right). Fig. 15 (right)—Hardness survey across segregated zone of casting with 2-in. neck (Fig. 14).



from the segregate and the base metal show the carbon to be 0.31 and 0.26 per cent respectively. Fig. 19 represents a borderline case. If the riser junction was restricted still more, conditions similar to those found in knock-off risers would exist and more segregated metal would form.

The presence of a restricted section in a casting, however, is not in itself the cause of segregation. There must also be a flow of feed metal through this section. This is illustrated by means of the following test castings. The test casting shown in Fig. 20 consists essentially of two 3x3x4-in. rectangular blocks which are joined together at their bottoms by means of a one-inch square section. Feeding was accomplished by one large riser with Washburn necks attached to each block.

If segregation is caused only by a restriction in the casting, it should be found in the one-inch section joining the two blocks as well as beneath the riser necks. Sectioning and etching of these castings disclosed segregation beneath the risers, but no segregation was found in the connecting section. Several of these castings were made, some with a single gate leading into only one block, as shown in Fig. 20, and some with gates leading into both blocks, and the same results were obtained. Figure 21 shows a typical segregation pattern existing within these castings.

When however the casting was made with the riser over only one block, as shown in Fig. 22 segregation was found extending from the bottom restricted section into the unrisered block. Figure 23 is a photograph of a typical section of a test casting risered in this manner. The unrisered block of Fig. 23 was chilled on the top half of three of its sides in an attempt to make it sound, but the solidification rate in the one inch channel was not sufficiently retarded and the channel was not sufficiently preheated by the metal which flowed through it to permit complete feeding. The areas which etch darkly in this casting are the segregated areas. The position and pattern of the segregate indicate movement of metal toward the unrisered section of the casting.

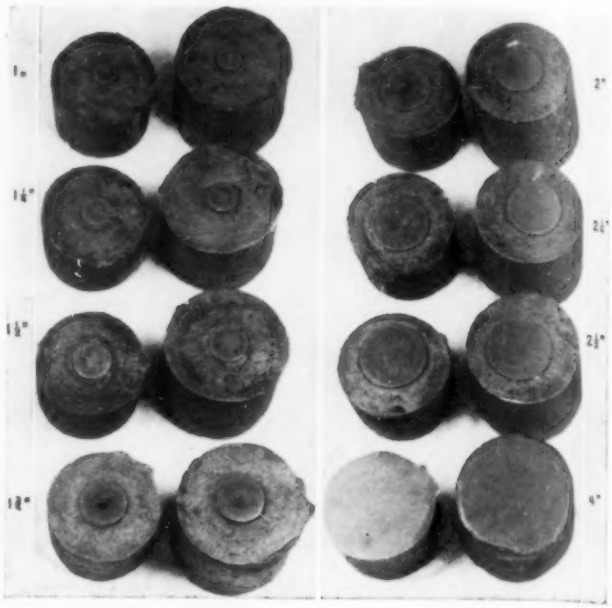


Fig. 16—Series of 8 castings 4-in. diam., 4-in. high, and their risers which have various neck diameters, showing segregation found in the riser necks

Figure 24 shows the carbon content at various points in a section of the casting of Fig. 23. Pronounced segregation is apparent under the riser, and the segregate leading into the unrisered block is shown to contain from 0.08 to 0.16 per cent more carbon than the base metal which has 0.28 per cent carbon. In the casting of Fig. 21, however, which had risers over each block, spot analyses showed segregation under the risers only.

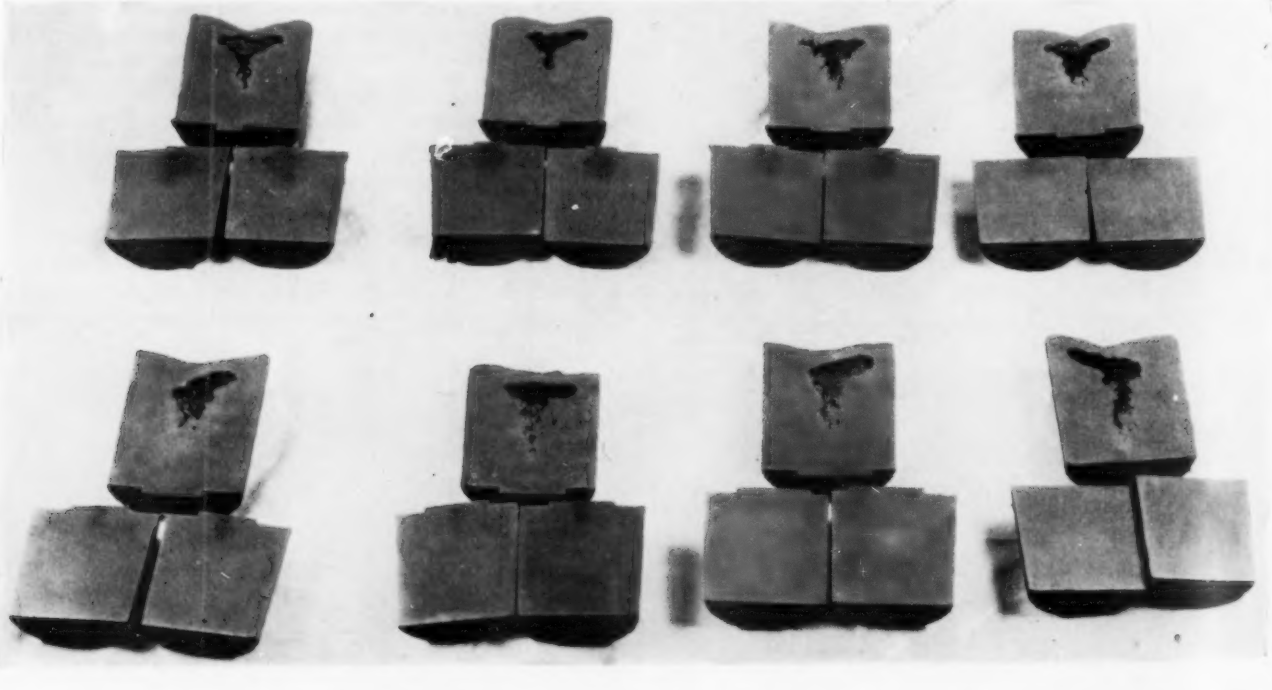


Fig. 17—Segregation in cylinder castings of Fig. 16 after further sectioning

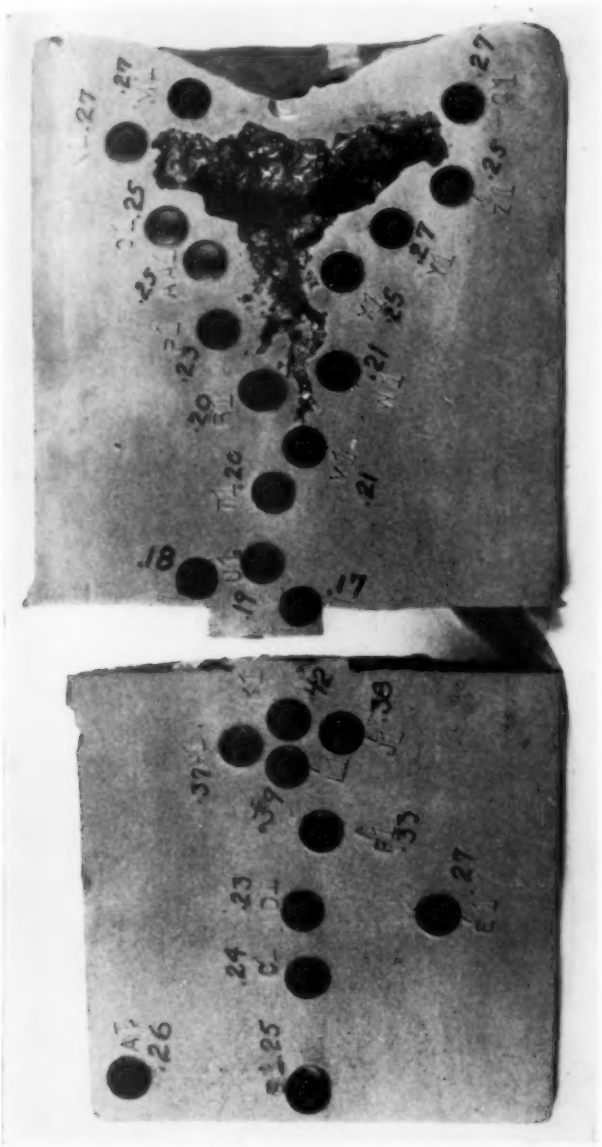


Fig. 18—Carbon content at various locations in cylinder casting and its knock-off riser

To confirm that the location and pattern of the segregate shown in Fig. 23 is caused by the movement of feed metal, and not by the method of gating which caused the metal filling the mold to flow in the same direction as the feed metal, a casting of this type was made with the gate leading into the unrisered portion. The etched sections of the resultant casting, Fig. 25, show segregate patterns which are the same as those of Fig. 23. The segregate pattern is in the same position and has the same appearance as in the castings which were gated under the riser, indicating flow of metal away from the riser, even though the metal filling the mold flowed in the opposite direction.

Figure 26 shows an etched half of a larger casting and of the large side riser which fed it. Extensive segregation is shown along the centerline of the casting. Much the same conditions exist in this casting as in the double block castings. The casting of Fig. 26 was separated at its mid-length by a Washburn core making it necessary for feed metal to flow upward through this restricted section to make the top half of the block sound. Since the feed metal in this casting flows in a direction opposite to its flow in a conventional knock-off riser, segregation is found above the opening.

It is to be emphasized that this segregation is not a result of the massiveness of the section in which it is found. The casting of Fig. 26 has a diameter of 4 in. and the riser has a diameter of 6 in. but no segregate is indicated in the riser by the etchant.

Theory of Segregate Formation

Ingot manufacturers have always been vitally concerned with segregation, and much information has been published on ingot segregation. A generally accepted theory of the cause of segregation in killed ingots[2,3] is that during the latter stages of solidification temperature gradients across the ingot become very slight and free floating dendrites form within the liquid metal. The dendrites have a greater specific gravity than the liquid and settle to the bottom of the ingot, and, since the dendrites are relatively pure iron, the top

Fig. 19—Block casting fed by open riser with reduced neck showing slight segregation



metal becomes enriched in carbon and the lower portion of the ingot where the dendrites come to rest is lower in carbon. Thus, there is invariably found in killed ingots a zone of positive segregation at the top and a zone of negative segregation at the bottom.

The same mechanism of solidification which causes segregation in killed ingots is undoubtedly active in small castings to a minor extent but it can by no means account for the extreme segregation which forms in castings with restricted riser contacts. From data compiled by Larsen,[2] the average segregation ratio of carbon, that is the ratio between the lowest and highest concentration of carbon, in 15 ingots weighing between ¾ of a ton and 64 tons is 1.45. In the casting of Fig. 3 which weighs about 50 lb. including the riser, the carbon segregation ratio is 1.8. In addition the segregation occurs at the bottom of this casting which is, in ingots, the location of the zone of negative segregation. The casting shown in Fig. 26 in which the segregate exists throughout the length of the casting indicates further that the segregation found in the castings under discussion arises from conditions other than settling dendrites.

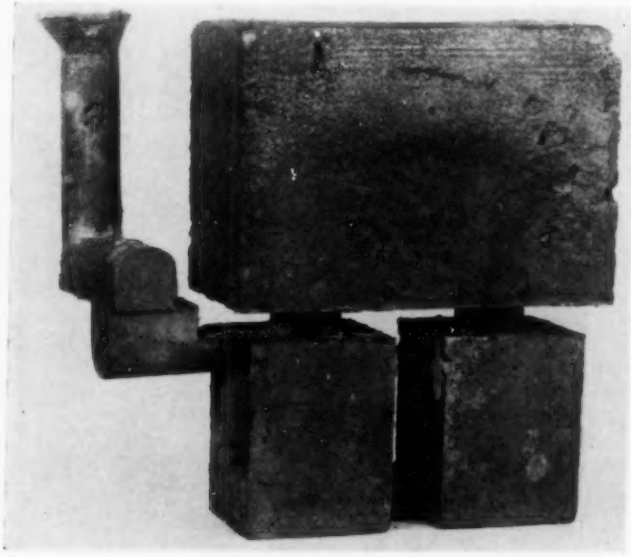
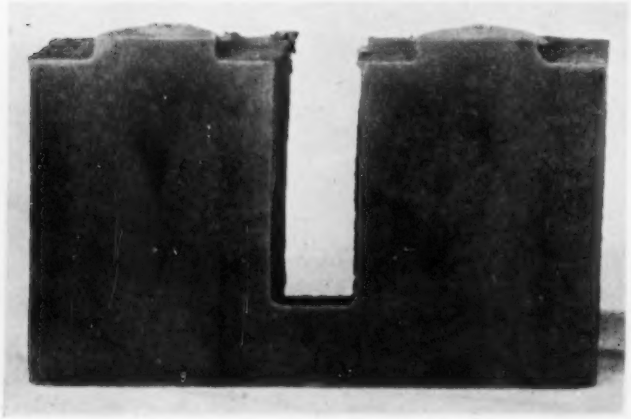


Fig. 20—Test casting used in studying segregation

Fig. 21—Section of test casting poured through double gate, one gate into each side of casting



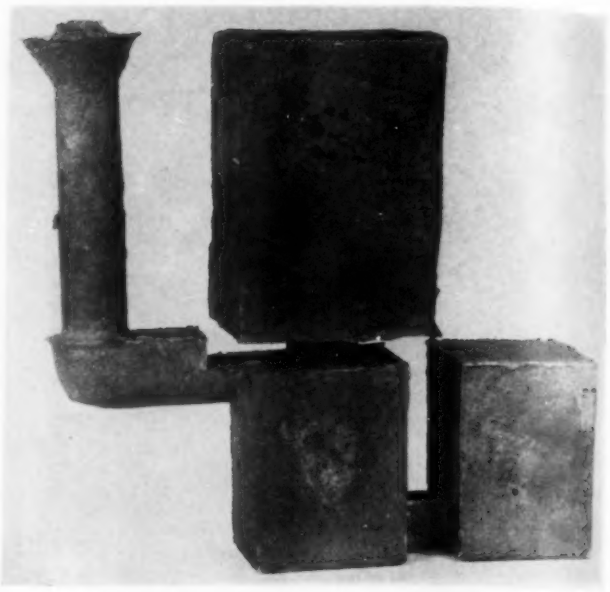
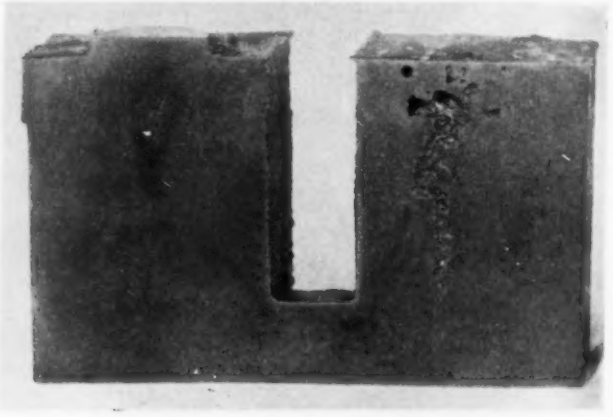


Fig. 22—Test casting risered to produce segregation in small bottom section

Fig. 23—Sectioned casting of Fig. 22. Note line of segregation leading into unrisered half of casting.



In the casting of Fig. 18 it is noted that the carbon content of the lower part of the riser and of the restricted contact contain less carbon than the base metal near the casting surface. However, the riser shown in Fig. 27 which covered the entire casting shows a more uniform carbon content throughout. Extensive analyses throughout several other risers taken from castings in which segregation occurred also indicate negative segregation. This condition was found in both the top "neck down" risers and the side risers.

At the right of Fig. 28 is a photograph of a section of the riser which fed one of the double block castings. The carbon contents at various positions on this riser section show a zone of negative segregation above each riser contact. At the left of Fig. 28 is a section of a simple block casting equal in size to this riser and analyses at a number of positions on this section show a uniform carbon content. It is apparent that the zones of negative segregation in the risers and riser contacts are associated with the zones of positive segregation in the castings.

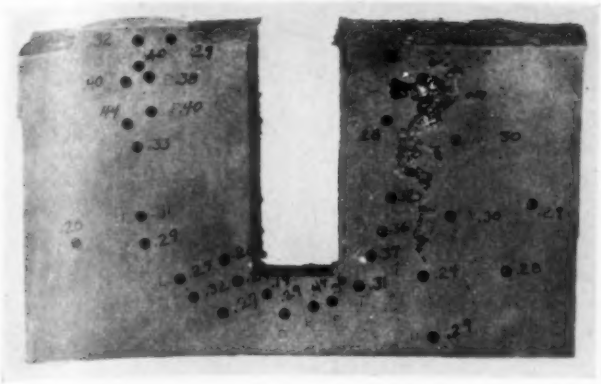


Fig. 24—Carbon content at various positions of casting shown in Fig. 23

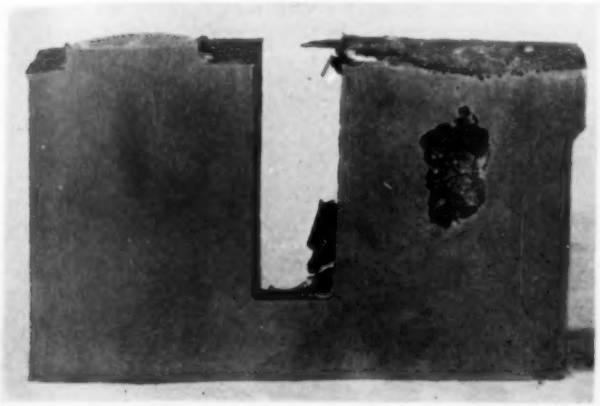
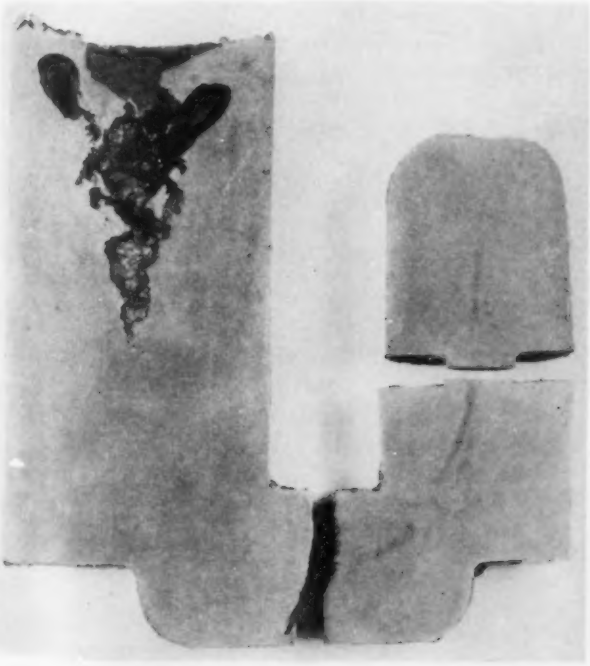


Fig. 25—Section of half-risered casting gated into unrisered side-location of segregate is the same as when gate leads to risered side

Fig. 26—Sectioned half of casting and riser etched with ammonium persulphate



It is believed that this occurrence can be explained in the following manner. According to the constitution diagrams, alloys freeze selectively under equilibrium conditions. The higher melting point constituent freezes first, leaving the remaining liquid deficient in this constituent. In practice the solidification rate of alloys may be sufficiently slow so that segregation is present after the alloy has solidified as is typified by the cored and dendritic structures of many alloys.

In steel, the low carbon dendrites solidify first and as solidification progresses the remaining liquid becomes enriched in carbon. At the surface of a casting, cooling may be so rapid that there is insufficient time for selective solidification to occur to a very great extent, and the metal will be comparatively homogenous. At the interior of a casting, the temperature gradients tend to level off causing the rate of solidification to be retarded, and it is probable that within the solidification range there exists a solidified dendritic lattice within which there is high carbon liquid metal.

A knock-off riser contact, because of its low rate of heat loss, will solidify similar to the central zone of a

Fig. 27—Carbon contents in casting made with conventional riser

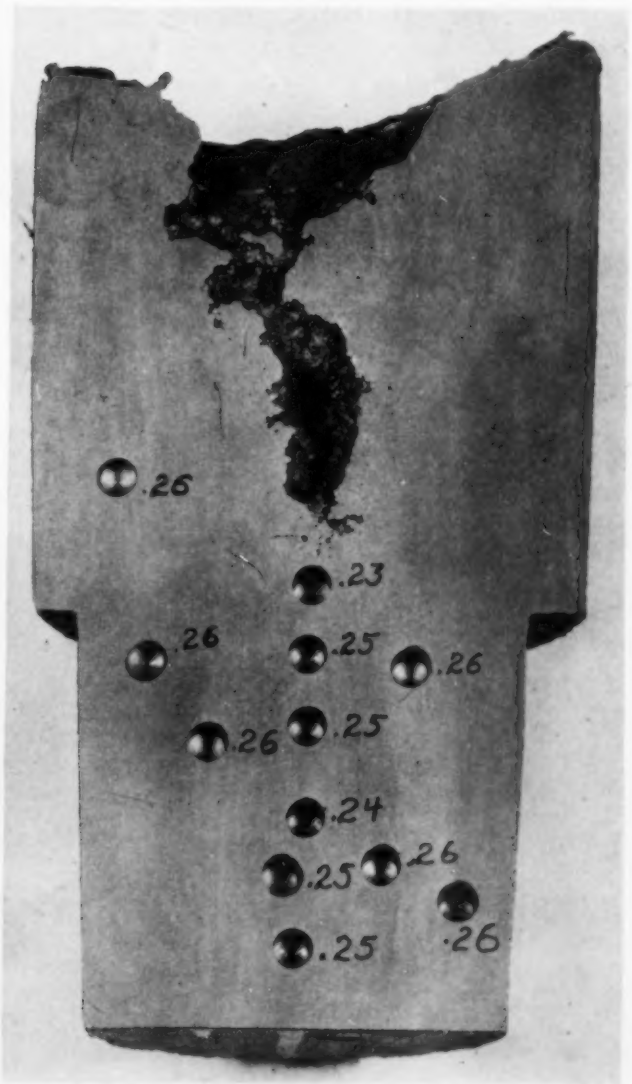




Fig. 28—Carbon contents of right-sectioned "neck-down" riser which fed a double block test casting, and left-sectioned casting of the same size as the riser which had to do no feeding

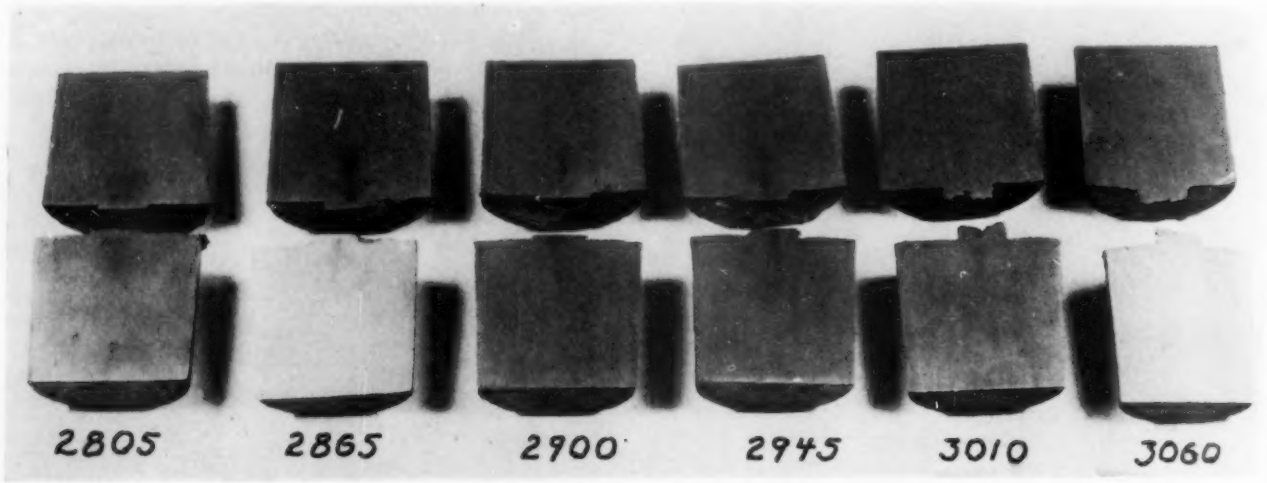
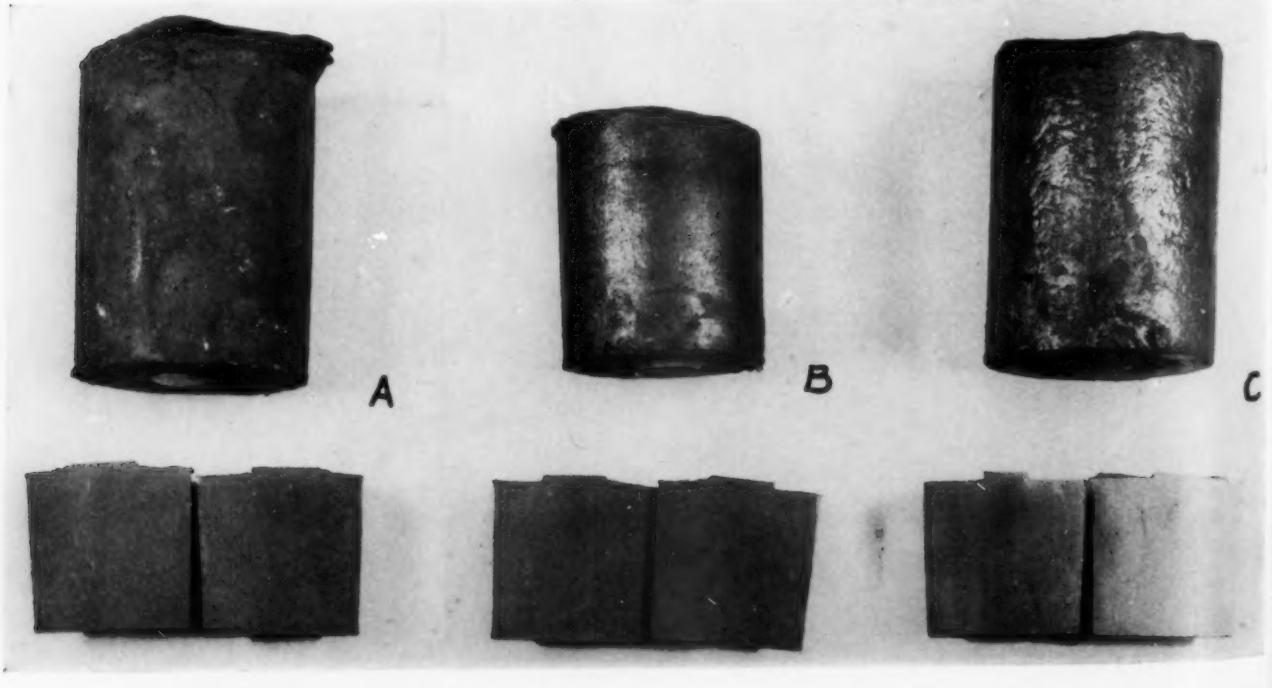


Fig. 29 (above)—Series of test castings poured at various temperatures as indicated. Fig. 30 (below)—Sectioned and etched castings fed by:

A. 6-in. diam., 8-in. high riser with a 1¾-in. contact.
B. 5-in. diam., 6-in. high riser with a 1¾-in. contact.
C. 6-in. diam., 8-in. high riser with a 1¼-in. contact.



much larger section. It is believed that this particular type of segregation occurs in castings because, during the freezing process, the riser contact is in a more advanced state of solidification than the casting. The dendritic lattice with its entrapped liquid extends across the contact while there is still a demand for feed metal within the casting. When further solidification occurs within the casting, a vacuum forms which causes the high carbon interdendritic metal in the contact to flow into the casting.

In the riser, the existing liquid has also become slightly impoverished in iron and enriched in carbon. This liquid flows into the dendritic structure of the riser contact and also into the casting if additional metal is needed there. The lower part of the riser and the riser contact contain less carbon than the base metal because the liquid metal flowing through these areas during the last stages of feeding is continually losing some of its low carbon constituent by the process of selective solidification. Thus the neck and lower extremity of the riser, when solidification is complete, consist of an abnormally high percentage of the low carbon constituent.

Segregated zones of the type described in this report may thus be considered as an intermediate condition between sound, comparatively homogenous metal and shrinkage cavities. Segregated areas are warnings of potential shrinkage and in many cases segregation and shrinkage exist together.

There is a definite indication that castings which are poured hot have less tendency to segregate than those poured at lower temperatures. Figure 29 is a photograph of a series of sectioned and etched castings poured from the same heat at temperatures ranging from 2805 F to 3060 F. There is a noticeable decrease in the magnitude of the dark segregated areas in the castings poured at the higher temperatures.

Work in progress indicates that increasing the size of the risers may also reduce the severity of segregation in castings where restricted contacts are used. The casting at the center of Fig. 30 was fed by a riser 5 in. in diameter and 6 in. high through a neck which was 1¾ in. in diameter. This riser is a little larger than is usually considered necessary for a 4-in. diameter 4-in. high casting, but slight segregation is disclosed by etching. A similar casting fed through the same size neck but with a riser 6 in. in diameter and 8 in. high showed no segregation by etching. The casting on the right of Fig. 30, although it was fed by an equally large riser shows segregation because the riser contact was only 1¼ in. in diameter.

Both series of castings of Figs. 29 and 30 were gated into the castings below the cores and a possible explanation for the decrease in segregation may be that the core was more thoroughly preheated by the metal flowing through it during the filling of the mold. When the core becomes highly preheated either by very hot metal flowing through it or by a large volume of cooler metal flowing through it, the metal in the neck may remain molten for a greater length of time. Since the neck remains open, the casting can obtain the last bit of feed metal it requires from the riser rather than from between a dendritic network.

A systematic study was made on this 4-in. diameter, 4-in. high test casting to determine how carbon segregation under restricted riser contacts varied quantitatively with core thickness, neck diameter and neck design. Figure 31 shows the manner in which the test castings were made. Four castings were made in a flask and were fed by risers 5 in. in diameter and 5½ in. high.

The risers were completely enclosed by the sand mold, and had sand cores inserted in them to permit atmospheric pressure to act on the riser metal. The risers were enclosed in sand primarily to insure that they all contained the same quantity of metal so that any variation in results would not be ascribed to different amounts of metal flowing through the contacts. The castings were poured with a Navy Class B steel at temperatures between 2900° F. and 2950° F. Cores of four different thicknesses, ½, ¾, 1 and 1½ in., were used at the riser contacts.

Two types of neck design were employed in the tests—cylindrical and tapered, with the outer surface of the tapered contacts forming an angle to 60 degrees with the base of the riser. All of the castings were annealed at 1650° F. for 4 hr. before being examined for segregation. Figure 32 shows one series of sectioned castings which were made with cylindrical contacts.

The cores which formed the contacts had thicknesses of 1 in. and the contact diameters varied from 1 to 3 in. as indicated by the figures between the two sections of each casting. The castings were first etched

Fig. 31—Test castings as removed from mold.



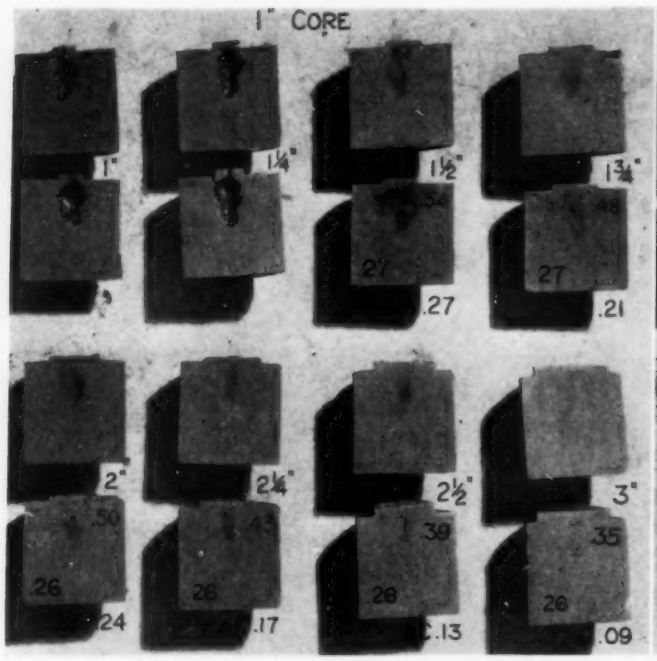


Fig. 32—Series of sectioned and etched castings with cylindrical contact—1-in. core.

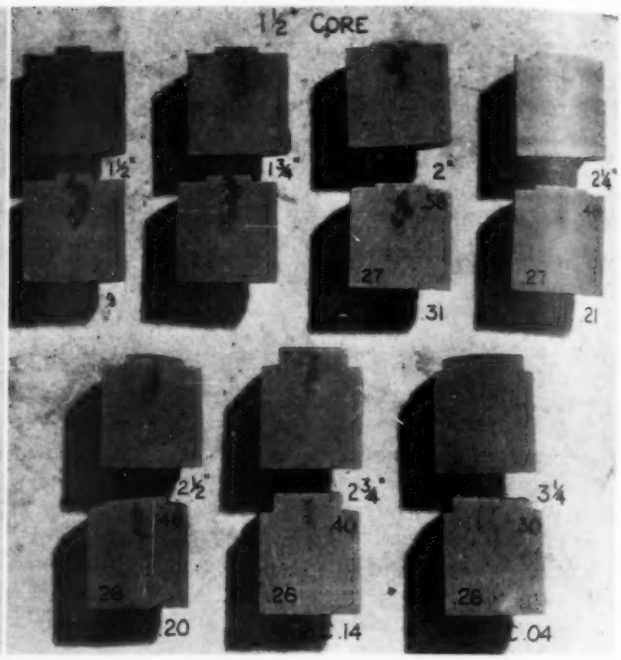
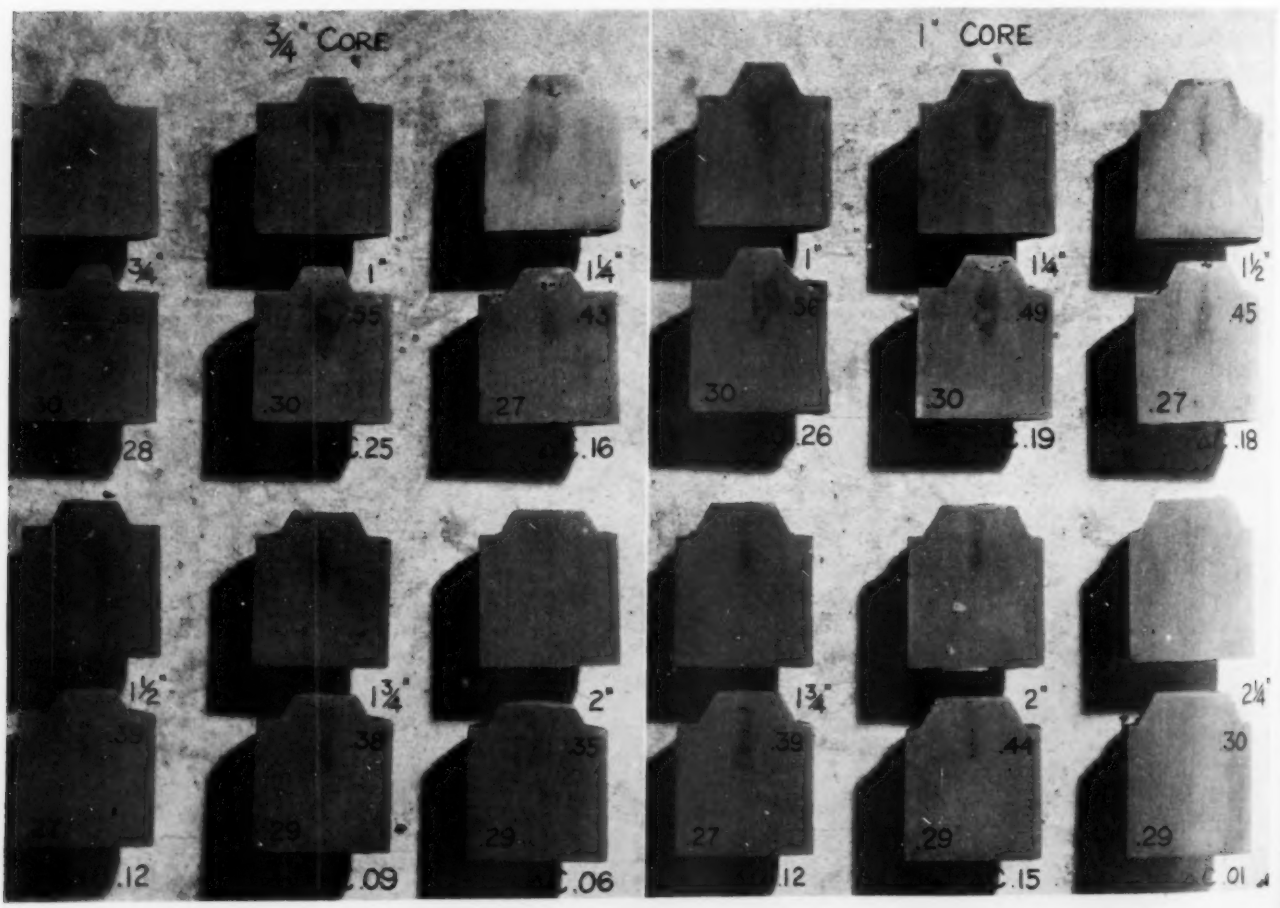


Fig. 33—Series of sectioned and etched castings with cylindrical contact—1½-in. core.

Fig. 34—Series of sectioned and etched castings with tapered contact—¾-in. core.

Fig. 35—Series of sectioned and etched castings with tapered contact—1-in. core.



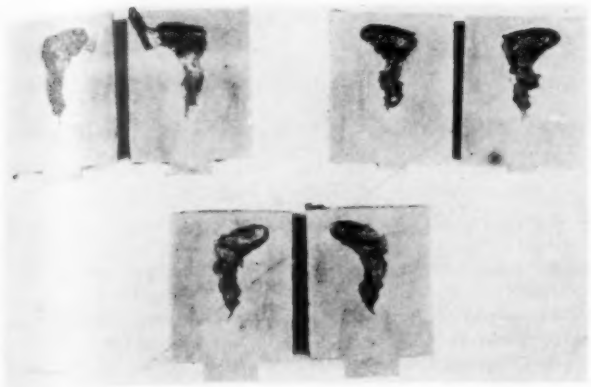


Fig. 36—Typical risers removed from test castings as sectioned and etched.

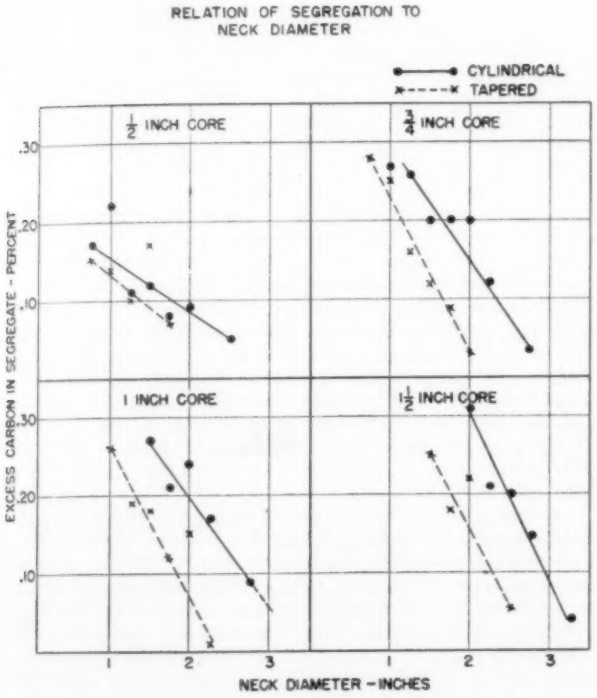


Fig. 37—Carbon segregation as related to contact diameter. Relation of segregation to neck diameter.

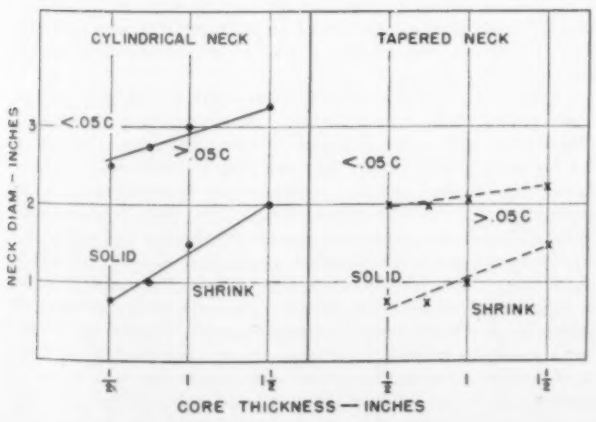


Fig. 38—Contact diameters required to keep carbon variation within 0.05 per cent carbon.

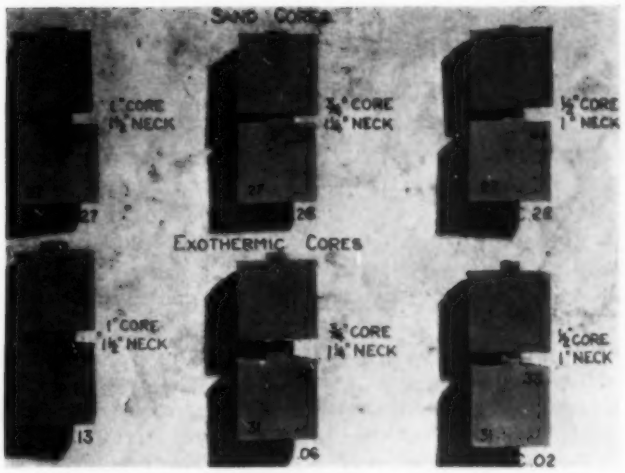


Fig. 39—Comparison of castings made with sand and with exothermic cores.

as shown in Fig. 32 to delineate the segregated areas and then punch marks were made at the points where the segregate was most pronounced. The castings thus marked were sand-blasted and drillings taken for carbon analyses. The carbon contents of the base metal and segregated areas are indicated in Fig. 32 and the differences between these two analyses are shown below the castings.

Figure 33 is a similar series made with 1½-in. thick cores between the castings and risers. Figures 34 and 35 are two of the series made with tapered contacts. The neck diameters indicated in Fig. 34 and 35 refer to the smallest diameter of the neck. Three of the risers from these test castings are shown in Fig. 36 after they had been sectioned and etched with ammonium persulphate. No segregation was found in any of the risers.

The data obtained from these tests are shown graphically in Fig. 37 and the rapid decrease in the amount of segregation with increasing neck diameters is evident. Segregation also becomes less as the core thickness or neck length decreases. For example, with a constant neck diameter of 2 in., it can be observed from Fig. 37 that the amount of carbon variation in the castings with cylindrical necks varies from 0.30 per cent with a 1½-in. thick core to 0.10 per cent with a ½-in. thick core.

The curves in Fig. 38 show the neck diameters required in cores of different thickness to make this particular 4-in. diameter, 4-in. high casting sound and also the neck diameters required to keep carbon segregation in this casting to within 0.05 per cent of the carbon content of the base metal. For example, with a ½-in. thick core which forms a cylindrical contact, the casting will be sound when the neck diameter is only ¾ in., but to keep the variation of carbon between the base metal and segregate as low as 0.05 per cent carbon, a 2½-in. diameter contact must be used.

When a 1½-in. thick core is used, the casting will be sound with a 2-in. diameter contact, but a 3¼-in. diameter contact is necessary to keep segregation to this arbitrary minimum. Unfortunately, when the

neck diameters are increased to the dimensions indicated by the top curves of Fig. 38 the risers cease to be "knock off" risers and must be removed by burning.

However, as pointed out previously, when the core is thoroughly preheated by metal flowing through it, less segregation results. In other words the core forming the riser contact may be considered as a mild chill which disrupts the temperature gradients from the casting to the riser, and by preheating the core its chilling effect is reduced.

A core made of exothermic materials should cause less segregation than a sand core since an exothermic core would supply heat and absorb less from the molten metal in and around the contact. This was found to be true as shown by the test castings of Fig. 39. At the top of Fig. 39 are three sectioned and etched test castings made with sand cores to form the contact and at the bottom are three castings identical in dimensions but with cores made of a commercial exothermic material instead of sand.

The carbon variation in the casting made with the 1-in. thick core and the 1½-in. diameter neck was reduced from 0.27 to 0.13 per cent by replacement of sand with exothermic material; with a ¾-in. thick core and a 1¼-in. neck the variation was reduced from 0.26 to 0.06 per cent carbon; and with a ½-in. thick core and a 1-in. diameter neck, carbon segregation was practically eliminated.

Summary and Conclusions

1. Segregation of carbon and also the other alloying elements occurs in steel castings near the riser contacts when the contact is restricted and directional solidification is not quite obtained. Washburn or "knock-off" riser connections form ideal conditions for the occurrence of segregated zones.

2. It is concluded that, during the latter stages of solidification, a lattice work of low carbon dendrites surrounded with high carbon liquid exists in the riser contact while the casting still requires feed metal. To satisfy the feed demand of the casting, the high carbon interdendritic metal flows into the casting to form the segregated zone. If still more feed metal is required in the casting, it may be obtained from the riser through the dendritic network of the contact.

3. Commercial annealing treatments do not disperse the segregation.

4. In a steel having a base analysis of 0.25 per cent carbon, the segregated zone may contain 0.45 per cent carbon. There is also a minor segregation of the other alloying and residual elements. The segregated zone may contain between five and ten per cent more of these elements than is present in the base metal.

5. Segregation becomes more severe as the riser neck is reduced. This type of segregation is not found under large riser connections which cover the area to be fed.

6. The severity of segregation in castings is reduced by pouring with hotter metal and by increasing contact diameter and decreasing riser contact length.

7. Replacement of sand cores by exothermic cores at riser contacts has a favorable effect in reduction of carbon segregation.

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DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, Indiana Harbor, Ind.

Co-Chairman: G. VENNERHOLM, Ford Motor Co., Dearborn, Mich.

J. B. CAINE[1]: The authors are to be congratulated on their experimental vigilance. The writer was fortunate in having an opportunity to discuss this segregation with Mr. Bishop several months ago and came away from Anacostia not only alarmed but puzzled. We at Sawbrook had been using necked-down risers and cores extensively for over a year, and if this segregation existed, it could lead to serious difficulty. At the same time, he was puzzled in that the trouble had not arisen before, for many of the castings made with necked-down risers using small openings were being machined at a hardness of 350 to 400 Brinell, about the limit of machinability, and at a hardness level where even a slight increase in carbon renders the casting unmachinable.

Past experience had shown that the same type of segregation due to high carbon pipe eliminator did cause much trouble. In an endeavor to find out why no trouble had arisen, about 50 types of production castings made with necked-down risers were sectioned and possible segregation studied by etching with nitric acid as well as ammonium persulphate. Hardness surveys were also made. It was found that segregation existed in only two of the 50 castings studied, and in one casting, the segregation was associated with secondary shrinkage in the casting away from the riser. The neck had frozen off, not the neck of the riser in this case, but it was on a small gear and the hub of the gear had frozen off. The segregation was down in the hub associated right at the bottom of a very slight shrinkage area.

It was first thought that the etching procedure used at Sawbrook was not showing the segregated area, so cylinders similar to those shown by Mr. Bishop were made and the segregated area was found every time, even in little 2-in. diameter by 2-in. high cylinders. The question then came up: Why did not all the production castings show this type of segregation? The answer *may* be in the design of the casting itself. The experimental castings used at Anacostia I presume were all cylinders or rectangles whose height is about equal to the diameter or section. This is about as chunky a casting as it is possible to make. Several other castings, the plate and the bracket, show a small connection between the riser and the casting, either a riser that is quite high in relation to the diameter or whose riser section closely approaches the casting section. The section and surface area of the risers that have been studied, or at least shown in this report, are close to the surface area and section of the casting.

The authors' theory of the formation of the segregate implies almost split-second timing and therefore any change in this timing will cause either a definite shrinkage cavity in the casting at the junction of the neck and the casting if a necked-down core is used, or the minimization or elimination of the segregate area. The authors also show that increasing the dimensions of the riser decreases or eliminates this segregation.

Fortunately, the majority of production castings are rangier than those used in this study. By "rangier" I mean they have a higher surface area in relation to their volume. Riser efficiency itself dictates riser sections larger in relation to the casting section to be fed than those risers used in this report. It may therefore be possible to riser efficiently the majority of the castings that we produce using necked-down cores, with little or no carbon segregation at the junction of the neck and the casting.

[1] Sawbrook Steel Castings Co., Lockland, Ohio

Now, if this *theory* is correct, the carbon segregation as described by the authors need only be considered with chunky castings, castings with low surface area to volume ratios, and we need not worry about carbon segregation in the majority of the castings we make.

MEMBER: In Fig. 18, how can you account for the low carbon (0.20–0.21 per cent) directly under the pipe in the riser? That does not seem right.

MR. BISHOP: Selective solidification occurs there. There are low carbon dendrites and high carbon interdendritic liquid metal and there is also a feed demand at that time. This riser was on the narrow diameter side. I think ordinarily in any casting there is a dendritic lattice with interdendritic liquid and if there is no reason for that metal to move, it will stay there and become entrapped, but in this particular instance the interdendritic metal was drawn down a little toward the casting.

MEMBER: It would seem to me if it is drawn down, you ought not have a shrink in that position. Is that not the last part to solidify in the riser?

MR. BISHOP: There might have been interdentritic shrinkage there. It would be so small that it would probably take a microscope to see it.

MEMBER: The point is that when you drill for chemical analysis chips you drill over quite an area. You are not dealing with a small place; you have quite an area that you are integrating there.

MEMBER: That is a good answer to it.

C. E. SIMS[2]: Is it not a case where the shrinkage cavity occurs higher up, and when the high carbon interdendritic liquid metal is drained down through the neck, it is replaced by unfrozen metal, from further up in the riser, which is of normal composition?

It might be possible that the high carbon area may be a little below the neck in some cases and not immediately at the surface. If the casting were machined just below the neck, you might not run into a hard spot; the hard spot may actually be buried.

MR. BISHOP: Yes, that is the case quite frequently. We have noticed when we sawed the risers from the castings and etched the necks, in some cases only a little segregation was shown, but when we sectioned the casting, we uncovered quite a large area beneath the surface.

R. W. THOMAS[3]: Have you found any lowering of the carbon away from the gate?

MR. BISHOP: We found that by flowing large quantities of metal through the neck, segregation was reduced. If we had a large casting and could gate on top of the core so all the metal ran through the neck, it would tend to minimize segregation. This core acts as a chill, and if we can flow a lot of metal over it and preheat it, then it tends to become ineffective as a chill. It is actually the chilling effect which causes the segregation.

MR. SIMS: Have you noticed any difference in this segregation depending on whether the metal flows from the riser to the casting or from the casting up through the riser? That is, would the direction of filling the mold have a noticeable effect on the segregation?

MR. BISHOP: We gated all our castings either at the very bottom of the casting or directly below the core.

DR. C. H. LORIG[2]: Would some of the carbon segregation be decreased if the castings were homogenized for quite some time or heat treated in such a way as to permit diffusion of the carbon from areas which are concentrated in carbon?

MR. BISHOP: We have heated the 4-in. cylinders at 1850° F. for about 6 hr. and sectioned and etched them and we still noticed the segregation pattern. We did not take drillings. I do not know if the intensity of the segregation was decreased or not, but it was not eliminated. Possibly much higher temperatures would cause homogenization.

E. C. TROY[4]: We found segregation sections as light as ½ in. under side blind risers, not washburned. Such risers were not gated but filled through the casting. We changed that situation and put the metal through the riser and still had the condition, though it was reduced. We heat treated the castings to eliminate the segregation but succeeded in eliminating only a very small portion of it. The outer edges of the segregated zone seemed to diffuse into the normal metal, the more concentrated carbon, segregation working toward the outer edges, but we were unable, within the limits of the ordinary commercial heat treatment, to do a great deal of good in reducing the segregation. The difficulty was eliminated by increasing the riser and riser contact size.

R. C. WAYNE[5]: Perhaps a reason for Mr. Caine's failure to find segregation might be that the castings were gated through the riser, and the necked-down riser core thereby causing the core to maintain the metal temperature longer, with the result that a better feeding action was obtained leading to a less selective freezing.

MR. CAINE: Some of the 50 castings that we checked were gated into the riser, the metal flowing through the opening into the casting. Others were relatively large 2,000-lb. castings, and were bottom-gated. This was not a scientific investigation; it was simply spot-checking every casting we could get, gated in a number of ways. One of the two castings showing segregation was gated into the riser, the metal flowing through the opening into the casting. That particular casting showed the secondary shrinkage in the casting. The other one was gated into the casting immediately below the opening. In other words, it was gated at the parting line below the riser—a standard gating practice. The other 45 or 50 castings that we checked were gated in both ways away and into the riser.

MR. WAYNE: It would seem that the parting line gate may hold out some hope from the standpoint of heating the neck area, and eliminating the segregation. If the casting is gated immediately adjacent to the neck area, this would heat that area to a higher temperature and lead to better feeding action through the neck and less chance for segregation.

MR. BISHOP: Some of the castings shown in this report were gated right under the core, and we still got segregation. But actually our riser was bigger than the casting and we should get more preheating by flowing the metal up into the riser than we should by flowing it down into the casting, because less metal would flow through the opening if we gated on top of the core.

MR. THOMAS: We thought of gating under the core and above the core. In other words, our casting fills up right under the core and as soon as it does, all the hot metal goes into the base of the riser.

CHAIRMAN RASSENFOSS: Exactly how are you getting above the core and assuring yourself that once the bottom of the mold has been filled, you go to the top gate?

MR. THOMAS: The distance between the two gates is only the thickness of the core.

J. A. DUMA[6]: We are gating in exactly the same manner a series of tests which we are making using Washburn cores. We use two gates; a small one at the very bottom to effect evacuation of all gases from the mold, and a second larger gate atop the core to give us the hottest metal in the riser. I see where Mr. Bishop's work indicates the direction towards which we should go in solving this segregation. He says that the more preheat the core has, the lower the tendency toward segregation. In other words, thin cores are the answer. We use much thinner cores than Mr. Bishop has used and have experienced no trouble either from core breakage or in obtaining a sound casting. I wish Mr. Bishop will explain why he used such thick cores for such small castings.

MR. BISHOP: I do not think that is unusual. There is no reason why you should not get a casting sound with these particular diameter contacts. However, in the ¼-in. thick cores you mentioned, with our particular sand, we tend to get swelling and much penetration, so that is the reason we did not go to core thicknesses of less than ½ in. However, we did make a few with a ¼-in. thick core and, since the thinner the core, the less segregation there is, there was little segregation beneath the ¼-in. thick cores. In no case did we find a carbon variation of more than six points with a ¼-in. thick core. The only difficulty was in the bowing and the penetration that resulted.

MEMBER: We get a definite rise in carbon content under the head, but I never could get a good feed on a thick core. We never go beyond ½ in. We never gate into the bottom of the

[2] Battelle Memorial Institute, Columbus, Ohio
[3] General Electric Co., Schenectady, N. Y.
[4] Dodge Steel Co., Philadelphia
[5] Steel Founders' Society, Cleveland
[6] Norfolk Naval Shipyard, Portsmouth, Va.

casting. We always put the metal through the riser hole. In that way, we heat the core to a temperature equal to that of the head and you have your hot metal on top, which supplies gravity. We also used the atmosphere core with it, and we never encountered a rise in carbon, except on a flat base under this riser. We experienced it on as deep as a 2-in. casting, but it was flat, possibly 6 in. across, and in that way it seemed to be the chilling effect down under that raised the carbon.

S. W. BRINSON[6]: Some time ago when the question of Washburn cores came up, I raised the question as to the chilling effects of the core causing this segregation, and the different points of gating. I maintain if your core is at room temperature and the steel is at a much higher temperature, regardless of how you gate it, you have a certain zone under the core, through the neck and above the core in which at one time you reach heat equilibrium. The point is, what happened from then on?

From the point of directional solidification, the best way would be to gate in the riser above the core. The coolest metal will go down through the hole and the hottest metal will be up in the riser. However, I believe if you pour below the core while you are filling your mold, your hot gases are going up through your core and preheating your core and then if your riser is larger than your casting, you have a lot of metal flowing from the bottom up through the neck into the riser. We maintain you can have a casting that, if it is poured slowly enough from the top—and this is theoretical, of course—you can have it so the last drop of metal that you put in that casting will be the last flowing in the casting and you can have a perfect casting without a riser, if it is possible to pour that casting slowly, as it solidifies. So it is a question of whether you gate it above or below your core or on the bottom. I think the bottom gating is the worst of all. I do not think there is any question about that, because I have no idea you can bottom gate it and push hot metal from your frame gate up through that hole into the top of the riser. I think it is foolish to expect it. But I do believe if you gate below the core, then after you fill your mold the hot metal will go under the core and up through the hole. You will also get the benefit of the hot gas while you are filling your mold, and with a good-sized casting (Mr. Caine said a 2,000-lb. casting) that core will be hot by the time that hole is filled.

C. E. WENNINGER[7]: I believe Mr. Caine is right in stating there is possibly a critical factor of time in relation to the solidification of the casting in the risers. Ninety per cent of the metal will solidify in the first half of the time and the last 10 per cent of the metal will require an equal amount of time.

E. L. LAGRELIUS[8]: I would like to know whether you found any difference in metal temperature and the rate of pouring on the amount of segregation.

MR. BISHOP: By pouring hot, we have less segregation.

CHAIRMAN RASSENFOSS: Our studies are incomplete but we can give you a brief history of what we have done. We took a cylinder which was approximately 5 in. in diameter and instead of being 5½ in. long, it was 7 in. long. We put a 6-in. diameter riser, 6 in. high, on that, gated into the bottom and proceeded to make some segregation studies. We poured several castings from electric furnace steel at our Indiana Harbor plant. Those were bottom poured from a one-ton ladle. We found a lot of segregation in them, so we decided we were going to find out the mechanism and the variables which affected it. So we brought the pattern to the laboratory and poured the same casting with the use of our induction furnace. We did not find the segregation in any casting poured in the laboratory. So we took the pattern back to the shop and made single castings from consecutive heats. Some of the castings contained shrinkage and some did not; some had carbon segregation and some did not. There was no correlation between any of those factors and we do not have full information as to those heats, that is, as to the pouring temperature and as to the pouring time of each of the castings, but apparently we have a critical casting and the results are not predictable except perhaps on a percentage or a statistical basis. But these heats were apparently poured quite hot as the dendrites run completely through the 5½-in. diameter section. We really do not know the cause yet.

R. E. KERR[9]: We questioned one of the refractory core manufacturers about this carbon segregation under the Washburn cores and his statements corroborate Mr. Sims' theory. He mentioned that some of the ingot men were finding exactly the same thing; namely, high carbon in the ingot proper with lower carbon in the hot top head.

The ingot men had supposedly investigated and formulated the following theory: (a) Toward the end of solidification the high carbon metal in the riser is fed down into the ingot by more or less a sucking action. (b) The remaining normal carbon content metal in the hot top cavity is further decarburized by atmospheric oxidation; this was supposedly quite noticeable on the ingot heads.

I also wanted to ask Mr. Caine a question on his theory on the chunkiness of the casting in relation to the riser. I did not check the size of the riser on the track link of Fig. 4, but I was curious as to whether his theory held on that application. Segregation was found under one of the center risers of Fig. 4 and I was wondering whether the chunkiness of the track link approached that of the riser?

MR. CAINE: I do not know the actual dimensions. It appeared as if that riser is relatively high in relation to its section or diameter. Perhaps by increasing the diameter and decreasing the height, we can change our freezing characteristics, change them enough at least to minimize the segregation. All of the risers shown in the production castings, the track link (Fig. 4) and also the bracket (Fig. 8), have risers that are relatively high in relation to their diameter, and whose sections approach the casting section. Again, this reasoning is only theory. It is hoped that the N.R.L. will continue their investigations and show us how to at least minimize this segregation.

CHAIRMAN RASSENFOSS: We are talking about the effect of thin cores here and apparently the thinner the core, the less the segregation. With a given size core might not the composition of the core used have a tendency to alter the segregation? That is, maybe one core has a higher heat capacity or conductivity than another core. What is the nominal composition of the cores used?

MR. BISHOP: We used an oil sand core, with about one per cent linoil and one per cent mogul.

CHAIRMAN RASSENFOSS: What was the base sand and was any silica flour used?

MR. BISHOP: We used a 60 A.F.A. fineness sand, no silica flour, and 4 per cent water.

CHAIRMAN RASSENFOSS: Mr. Duma mentioned the use of cores that were a lot smaller in thickness than these. Did you have any swelling with those? What was the composition?

MR. DUMA: It is a composition, similar to the one Mr. Bishop mentioned, except the cereal binder is just a little different, but it is rodded; it is reinforced with four wires. The wires are midway between the center and the outside, not coming anywhere near the neck, and the four rodded wires reinforce it considerably. Then the method of introducing the metal into the mold eases the impact of metal upon the core. We introduce it from the bottom, bottom gate it, and then the large size gate on top of the core that is gated into the riser takes over the introduction of the metal into the riser. We get good hot metal into the riser with our second top gate, and the bottom gate gives us nice slow filling up and the complete evacuation of all mold gases. We were afraid entrapment of gases would result, and it does in actual practice, if we do not evacuate the gases by pushing the metal up slowly that way. There is a difference between the straight linseed oil and the core oils you buy.

MR. WENNINGER: I believe it should be brought out that there is a relationship between the density of the core material and its ability to absorb heat.

EARL PIERCE[10]: Our tests throughout the country, using an exothermic material around the opening are in line with this paper. We definitely decrease the carbon segregation at the juncture of the riser and the casting by using an exothermic material around the orifice. That will be substantiated by many foundries making stainless steel.

CHAIRMAN RASSENFOSS: Do you have any actual casting in mind in which you had a segregation and you changed to the use of an exothermic material and got a reduction in that segre-

[7] Crawford Steel Foundry Co., Bucyrus, Ohio
[8] American Steel Foundries, East Chicago, Ind.
[9] Pettibone-Mulliken Corp., Chicago
[10] Pittsburgh Metals Purifying Corp., Pittsburgh

gation? Can you give us any figures on the change in results?

MR. PIERCE: A foundryman on the Pacific Coast has definitely proven that. He ran several tests using that theory and it definitely shows improvement.

JOHN HOWE HALL[11]: I would like to ask Mr. Duma what size ladle he pours from when he uses these exceedingly thin cores, because it makes a difference if you are pouring from a small ladle or a large one. I have seen cases in pouring from a very large ladle where a nice fountain of steel would shoot clear above the cope, out of each of the necks.

MR. DUMA: We pour from a two-ton, bottom-pour ladle, and the impact of the metal is considerable on that Washburn core. We have found that in the application of the Washburn core to large 8-in. diameter sections, it is impossible to obtain a solid section or use the Washburn core at all in sections over that size. But in the smaller sizes under 8-in. we can pour them successfully. Most of the impact is taken by the side gate, the down gate, the sprue that comes down and then the gate makes a sharp right-angled turn into the billet. By controlling the stopper opening somewhat, we lessen and slow down the flow of metal into the mold.

HOWARD F. TAYLOR[12]: The authors are certainly to be congratulated for a very fine investigative job.

There are certain overtones to an investigation of this sort that might lead a purchaser of steel castings to think that this is just one more of those things that are wrong with steel castings. That feeling would be wrong. I cannot see why it should be so hard to understand why commercial castings do not show this type of defect. I think we all know that a section such as shown in this paper is one of the hardest to gate and riser properly. I think we all know also that the making of an actual casting is much simplified because of its design.

I did not ask Mr. Caine to elaborate on the size of the section of the castings that he was working with, but the way I feel about this problem is influenced by some things I saw at the Falk Corp. in connection with knock-off risers. I do not believe any user of this type riser has done so with the feeling that he has necessarily always made a perfect casting or that he could always use "choke cores." I think he has done so with the feeling that he must make certain reservations. If he ran into segregation or into shrinkage as a result of his use of them, then he simply made the neck larger.

I know in many cases the foundryman has actually eliminated the use of knock-off risers for the simple reason that he could not get the castings sound under them. Segregation of carbon should not be too unexpected, either. The natural segregation that occurs in an ingot or in a casting simply causes the carbon which is ejected as solidification proceeds to collect somewhere. In the case of a long, thin section with a high "slenderness ratio" (a term coined by Mr. Lutts of the Boston Navy Yard), then that carbon must collect along the length of the casting and is not forced to collect in the top of it, so that if one uses a so-called knock-off riser under those conditions, he will not find the gross segregation, even though he has the same mass of casting to consider. This segregate is not found analytically because of rapid diffusion at the high temperature. Further, I think that if you draw the casting under discussion, sketch it out and then illustrate the rate at which and the directions in which heat is removed from that casting, you will find that immediately beneath the knock-off riser you have a very drastic hot spot.

I do not exactly agree with the authors in that high carbon metal feeds back down or is drawn back down through the neck. I feel that it has simply come from all sides and has collected at the hot spot immediately beneath the choke, and if you enlarge the choke, then you promote directional solidification, giving the segregate a chance to rise up into the riser. The existence of a partial vacuum will surely aggravate the condition, but is not essential to the theory.

Furthermore, if the high carbon material is going to come from the riser and the pure metal crystals, which are stated to be the first to solidify, fall to the bottom, then they, and not the high carbon material, should collect at the bottom of the riser, later to be drawn into the void by the vacuum. So I rather consider this problem as one of attaining proper directional solidification, and if one runs into trouble with the choke core, he will have to make the opening bigger, and may even have to stop using it altogether.

I am fortified in my feeling by further things I saw at Falk some time ago when renewed interest in knock-off risers was first developing. They were running into a small shrink, a perfect little round cavity, beneath knock-off risers, and they were wondering, as I was wondering, just why that should be so. Mr. Miller brought up the observation that it was just ordinary shrinkage due to a hot spot at that point. The problem was solved by increasing the neck of the knock-off riser. So I feel that the present segregate is a low density area resulting from inability to get proper directional solidification. I would like to ask Mr. Bishop if he has made density determinations in the regions where the carbon is extremely high.

MR. BISHOP: We have not made density measurements.

MR. BISHOP (*Authors Closure*): The authors wish to thank those who participated in the discussion. It is of interest to know that the occurrence of segregation in commercial castings is not a widespread problem. Further studies are being made at the Naval Research Laboratory to study segregation in castings which have a greater surface area to volume ratio as suggested by Mr. Caine.

We cannot agree with Mr. Taylor's theories regarding the occurrence of segregation. During the solidification process, high carbon metal is not ejected from the growing casting wall, but is entrapped by the dendrites within the wall. Actually, rather than a rejection of metal the converse is probably true, viz., liquid metal from the casting interior is drawn into the pseudo solid wall to compensate for the solidification contraction of the interdendritic liquid.

We are in agreement with Mr. Taylor, that if we have true directional solidification no segregation will be found in the casting. However when this condition is attained in small castings and risers, gross segregation is not found in the risers either.

[11] Steel Foundry Consultant, Swarthsmore, Pa.
[12] Massachusetts Institute of Technology, Cambridge, Mass.

SIMPLIFICATION OF LIGHT METAL CASTING DESIGN AND ITS EFFECT UPON SERVICEABILITY

By
W. T. Bean, Jr.
Continental Aviation and Engineering Corp.
Detroit

ABSTRACT

The simpler the casting design, the greater will be its load carrying ability because the stress distribution within the structure is more apparent to the designer.

Design analysis by direct strain measurement on the component is a fundamental approach to the design problem. The geometry of a new component is based on fundamental strength concepts.

The brittle lacquer test is used in stress analysis. As loads are applied to the part under test the brittle coating follows the deformation of the part and indicates areas of high stress by cracking at right angles to the maximum principal tensile strains. This brittle lacquer may be applied to all types of machine elements.

A better casting design is achieved when the metal is removed from the redundant elements and from the points of inflection and placed at points of maximum moment.

Introduction

For centuries artisans have recognized the advantages inherent in the casting process whereby complex metal objects could be formed by the simple technique of pouring molten metal into a mold. The modern production casting requires the team work of a group of specialists and may be variously defined: (1) to the Purchasing Agent it is simply $.60 a pound; (2) to the designer it is a solidified dream; (3) to the foundry it is a riser here and a gate there; (4) to the metallurgist it is alloy "X", tensile strength "Y"; (5) to production it is just so many templates, jigs, and fixtures; (6) to the engineer it is a structural component; (7) to the stress analyst it is a part full of holes, ribs, and fillets; (8) in the stock room it is a gear case, number XXX; and (9) to the management it is a headache – only 500 units a day.

The shape of a well designed casting is determined by service requirements. An ornamental casting, to be marketable, should be pleasing to the eye. The designer's ability to produce an artistic design will be reflected in the geometry of the casting. Too frequently, artistic design concepts are applied to the design of machine elements – beauty is largely a matter of opinion, but load-carrying ability is not. Good structural design looks good – to an experienced designer. The endurance strength of a machine element is as closely related to design as it is to the metal from which it is made.

Today, a structure is cast when its shape has become so complicated that it cannot be produced economically by any other process. Thus, the casting process inadvertently promotes more complex machine designs than otherwise would be possible. This is unfortunate as far as the strength of the casting is concerned because the shape of the structure establishes its stress distribution. At the author's plant we have found that the simpler the casting design, the greater will be its load carrying ability because the stress distribution within the structure is more apparent to the designer.

The foundry may be concerned primarily with the pouring of a sound casting but the engine builder's chief concern is the endurance strength of the casting; for if it will not support the applied loads for the life of the machine, service difficulties are inevitable. In this regard, the internal-combustion engine is probably the most brutal fatigue machine ever devised. It is rated according to its ability to create high cyclic loads. The design of its components must be based on the strength of materials under repeated loading.

The development of a new engine formerly required years of "cut-and-try" design procedure before it emerged as a satisfactory production pilot model. A long list of fatigue failures were considered inevitable. The design of an engine component had two ratings: (1) it was either good enough; or (2) it broke. A bad design was like a rotten egg – its quality was apparent to everyone *after* it was broken.

Stress Analysis Employed

By employing the techniques available in the field of experimental stress analysis, the weak links in a structure can now be discovered prior to service failure. Design analysis by direct strain measurement on the component is a fundamental approach to the design problem. *When the stress-coat, photo-elastic, and wire strain-gage techniques are employed, the strength of an indeterminate structure may be predicted much in the same manner that the quality of an egg is determined by "candling."* The geometry of a new component is based on fundamental strength concepts and less on "history." Engine components,



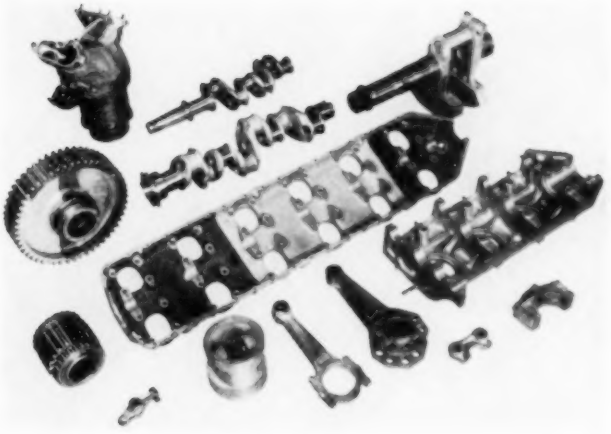


Fig. 2—Brittle lacquer may be applied to all types of machine elements as shown.

Fig. 1—Brittle lacquer coating being sprayed on connecting rod to be stress analyzed.

Fig. 3—Engine structure is generally utilized as the loading fixture in stress analysis of components.





Fig. 4—Fins had to be removed from a cylinder head to investigate strains set up by gas pressure.

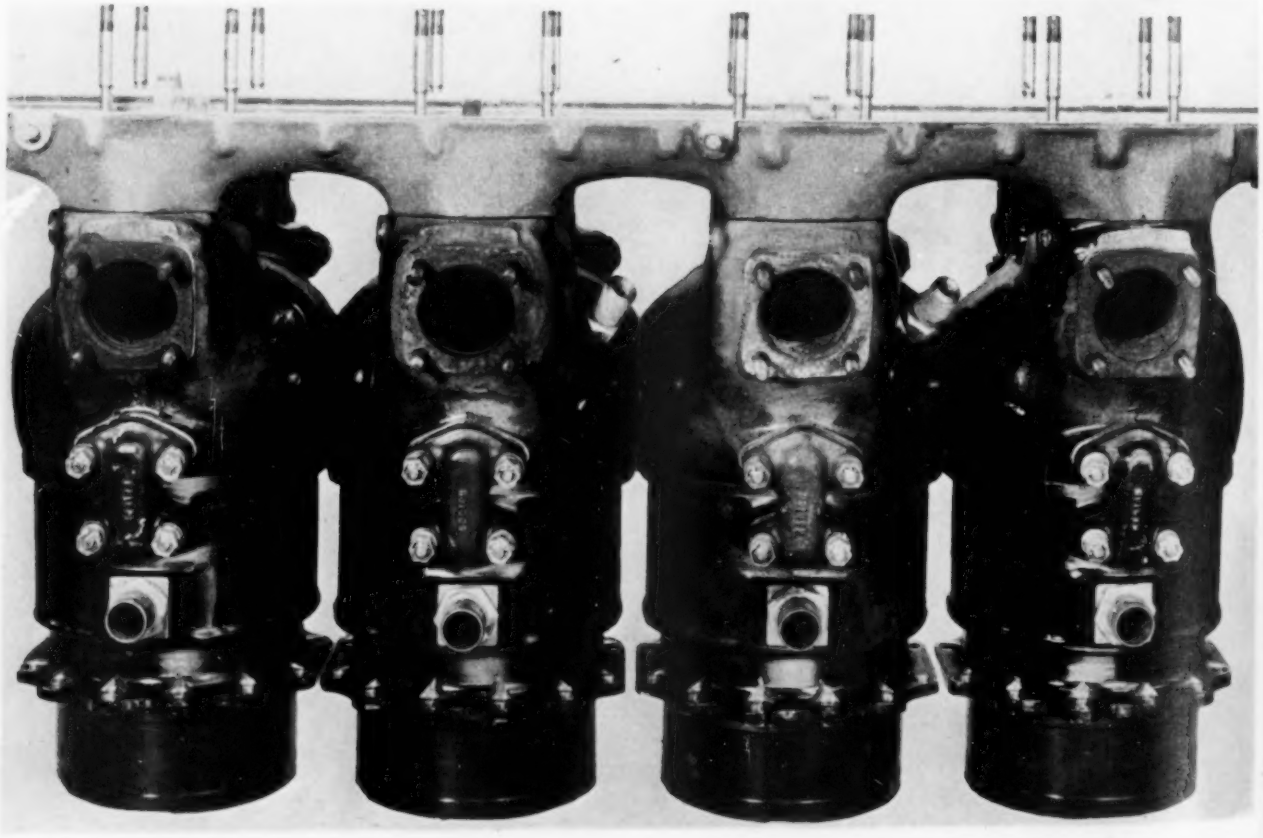
Fig. 5—Overhead camshaft and housing assembly attached to cylinder heads in performing a stress analysis test.

superior in strength and lighter in weight, may be developed in short order through the use of these powerful design tools. The manufacture of engine components to withstand fatigue should no longer challenge the ingenuity and patience of engine builders. The purpose of this paper is to review the design changes made in some highly stressed engine castings in order to eliminate service difficulties. Simplifying the casting design invariably resulted in improved serviceability.

METHODS OF DESIGN ANALYSIS

Service testing of an engine component is the "proof-of-the-pudding." An engine component may wear out, but it should not break. Service testing is a necessary phase in an engine's development, but the running of parts to failure in the full scale engine is a very expensive, time-consuming, and destructive method of design evaluation. Every design prior to service testing, used to be largely a matter of opinion. The design criterion was, "when a part does not fail it is too heavy — when it does fail it is too light." Thus, a part's endurance strength erroneously became associated with its weight, and as a result its strength was always marginal. Design improvements had to follow the "cut-and-try" route and optimum designs seldom were achieved.

Accelerated fatigue testing of full scale engine components in a fatigue machine is a time saver compared to full scale engine endurance testing, but it is still a destructive method of design analysis whereby all available parts and machines are broken as quickly as possible. *This method breaks the egg in order to determine its quality.*



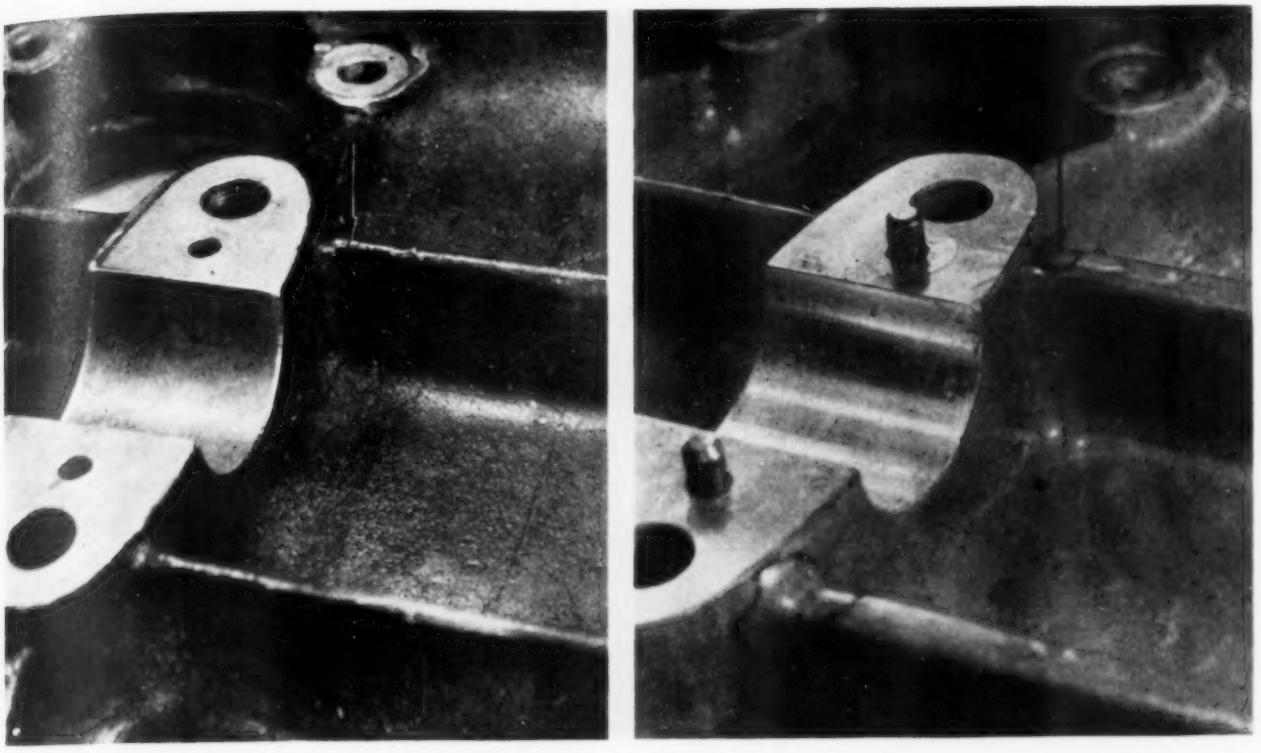
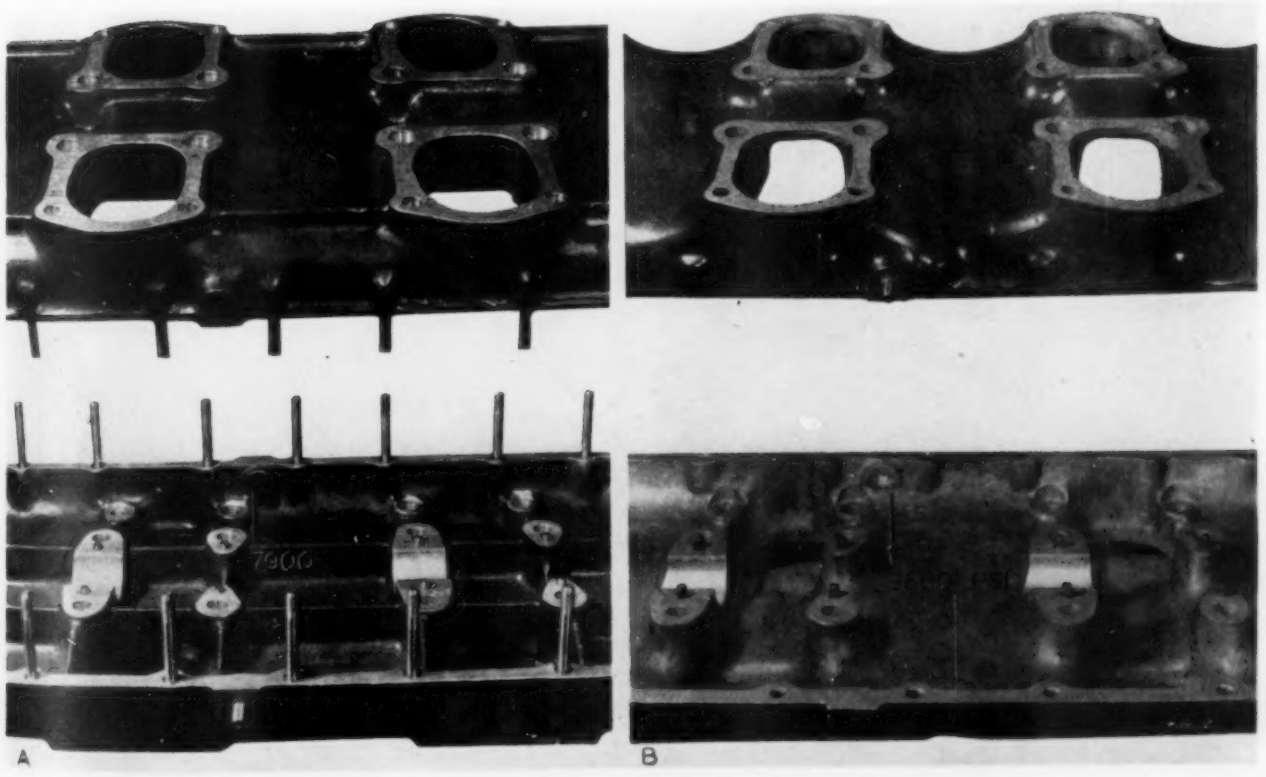


Fig. 6—Arrows indicate that fatigue failure correlated with the brittle lacquer test.

Fig. 7—Note the well blended sections in the new design compared with the old design. Stress reduction of 55 per cent was gained in redesign.



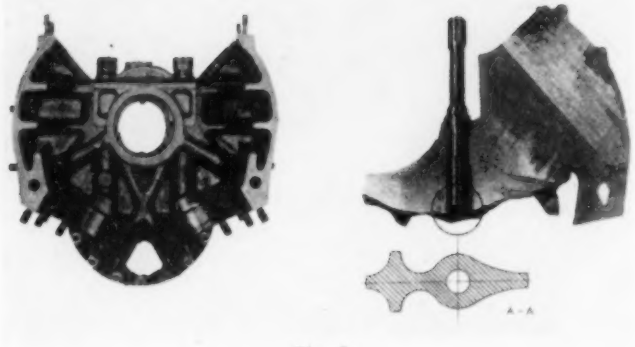


Fig. 8

Fig. 8—Original aluminum aircraft engine crankcase of conventional in-line design with well-ribbed sections.

Fig. 9—Crankcase in Fig. 8 redesigned removing metal from critical area below stud, but crankcase failed in endurance test.

Fig. 10—Crankcase design No. 3 with stress reduction of 45 per cent with elimination of rib failures.

Fig. 11—Design No. 2 (Fig. 9) modified by addition of molding clay to indicate desired contours.

Fig. 12—Note well blended sections of actual casting.

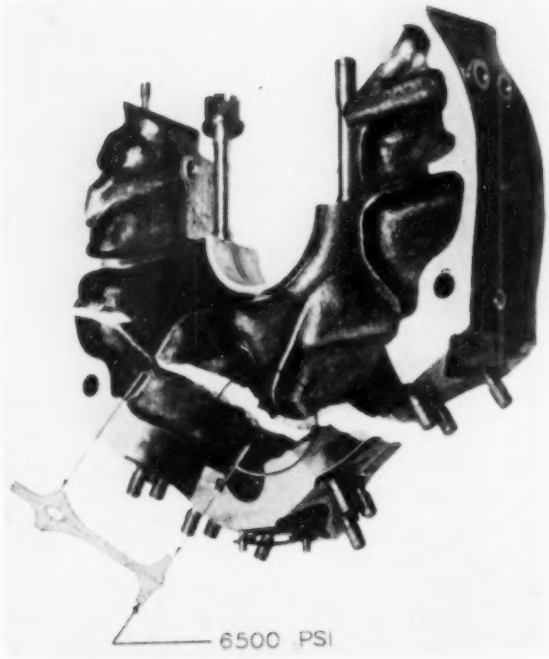


Fig. 9



Fig. 11



Fig. 10

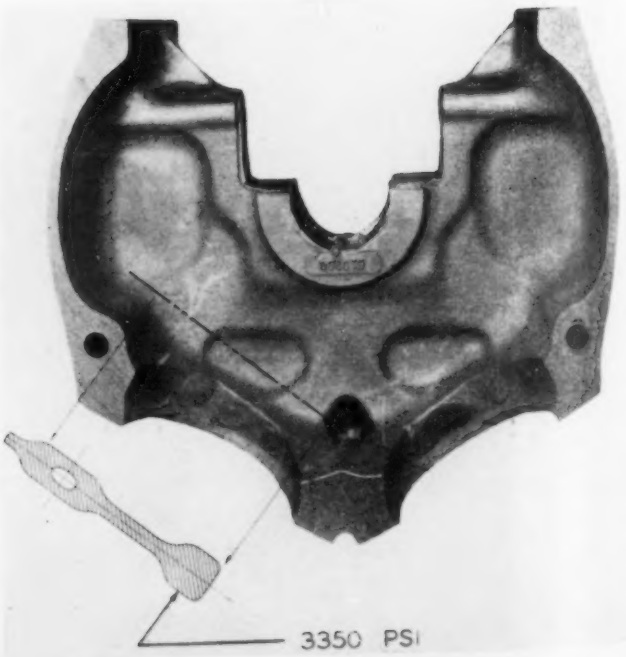


Fig. 12

The theoretical approach to the design of engine components by accepted text book methods is wholly inadequate. When properly applied, mathematics is a powerful tool, but even in the hands of an expert it cannot break the shell of a complex engine casting and reveal its stress distribution. The classical theories and equations most frequently used are based on simple statically loaded structures and do not apply to complex engine structures. A structural failure from a statically applied load seldom correlates with the fatigue failure. The basic concept neglected in applying theoretical formulas to the design of a machine element is that a part will fail in fatigue at the point of highest stress concentration, regardless of the cross-sectional area at that point. A stress concentration then, may be likened to a potato in a bin full of good potatoes —if given time it will ruin the whole works.

Brittle Lacquer Test Most Practical

Today, we are using the experimental strain technique, stresscoat, in the design analysis of engine components because it is the most practical and efficient tool available. The method is non-destructive. Full-scale engine parts may be tested and later used in the engine. This is important because all experimental parts are expensive. *Thus, the good eggs may be set aside for future use while the bad eggs are rejected — scientifically.*

The brittle lacquer coating is sprayed on the part to be tested (Fig. 1) and allowed to dry overnight. After the coating has dried, the part is placed on a loading fixture and simulated service loads are applied under controlled atmospheric conditions. As the loads are applied to the part the brittle coating follows the deformation of the part and indicates areas of high stress by cracking at right angles to the maximum principal tensile strains. A red dye etchant may be applied in order to increase the visibility of the cracks.

Quantitative strain measurements are made by calibrating the coating on a standard cantilever beam fixture. This brittle lacquer may be applied to all types of machine elements (Fig. 2). The engine structure is generally utilized as the loading fixture (Fig. 3). Successively increasing loads are applied until incipient cracks appear in the coating. Since no design analysis is better than its load analysis, the most critical loading condition is established by experimentation before the stress analysis of a component is attempted. When a service fracture has occurred prior to design analysis the critical loading condition is affirmed when the resulting strain pattern correlates with the fracture.

Brittle Lacquer Test Non-destructive

The brittle lacquer method of design analysis is non-destructive because it indicates strains that are well below the yield point of the material. However, sometimes the usefulness of a component must be impaired in order to determine the stress distribution of the basic structure. The fins had to be removed from an aluminum cylinder head (Fig. 4) in order to investigate the strains imposed by gas pressure. The critical areas were located and have been found to correlate with service fractures.

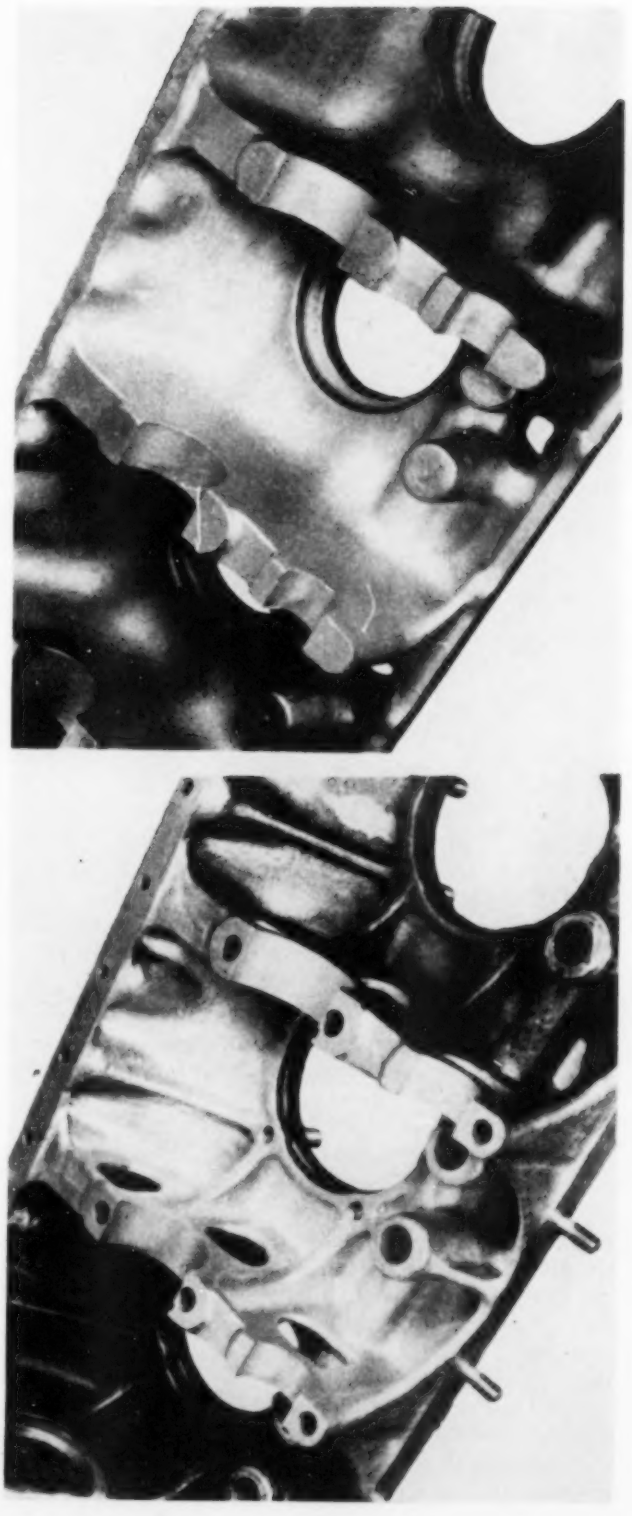


Fig. 13—Ribless magnesium crankcase with twice the endurance strength of former ribbed aluminum design and 25 per cent lighter than the aluminum design.

Design Simplification

During the development of a high output 12-cylinder military aircraft engine, cracks appeared in the internal "strengthening" ribs of the aluminum



Fig. 14—Stress distribution of ribs shown was good and maximum stress level was below endurance limit of the material.

camshaft housing. The overhead camshaft and housing assembly were attached to the cylinder heads as shown (Fig. 5). The arrows (Fig 6) indicate that the fatigue failure correlated with the brittle lacquer test. The test was made by applying cylinder pressure to a single cylinder. The housing was redesigned and the ribs were removed since they were acting as stress-raisers. Note the well blended sections in the new design compared with the old design (Fig. 7). A stress reduction of 55 per cent was obtained and the failures were eliminated without increasing the weight of the part. Contrary to popular opinion, the removal of the ribs did not increase the casting's flexibility.

The original aluminum crankcase for the military aircraft engine was of conventional in-line design with well-ribbed sections (Fig. 8). Gas and inertia loads were transmitted from the main bearing cap to the studs, down the studs, and directly to the cylinder deck. The failure (Fig. 8, Section A-A) was due to the stress concentration at the end of the stud. The stud design was changed: longer studs, shorter studs, shoulder studs, hollow studs, tapered studs, and studs having modified thread forms were tried. Some improvement was noted, but failures in the casting continued at the end of the stud. A new casting design was proposed which would change the stress path from the stud to the cylinder deck. Metal was removed from the critical area below the stud, but the crankcase failed in an endurance test (Fig. 9). A partial success was obtained, however, since the failure did not occur at the end of the troublesome stud. A brittle lacquer test on crankcase design number two was made by applying hydraulic pressure to a single cylinder. High thin ribs again were found to be acting as stress-raisers. In order to correct this difficulty, crankcase design number three was made (Fig. 10). Note the changes in the proportions of the ribs in the diaphragm section. A stress reduction of 45 per cent was obtained and the rib failures were eliminated. Since high thin ribs were found to be undesirable, a ribless design was proposed. An existing casting of design number two was reworked and modified by the addition of molding clay in order to indicate the desired contours (Fig. 11). Figure 12 shows the well blended sections of the actual casting. The elimination of the sidewall ribs and the redistribution of the metal resulted in a stronger, lighter, and more rigid crankcase that cost less to produce, was easier to cast and to clean. These ribless casting designs emphasize the importance of the efficient distribution of metal.



Fig. 15—A design eliminating ribs (Fig. 14) to reduce weight. Stress in remaining ribs became too high resulting in service failure.

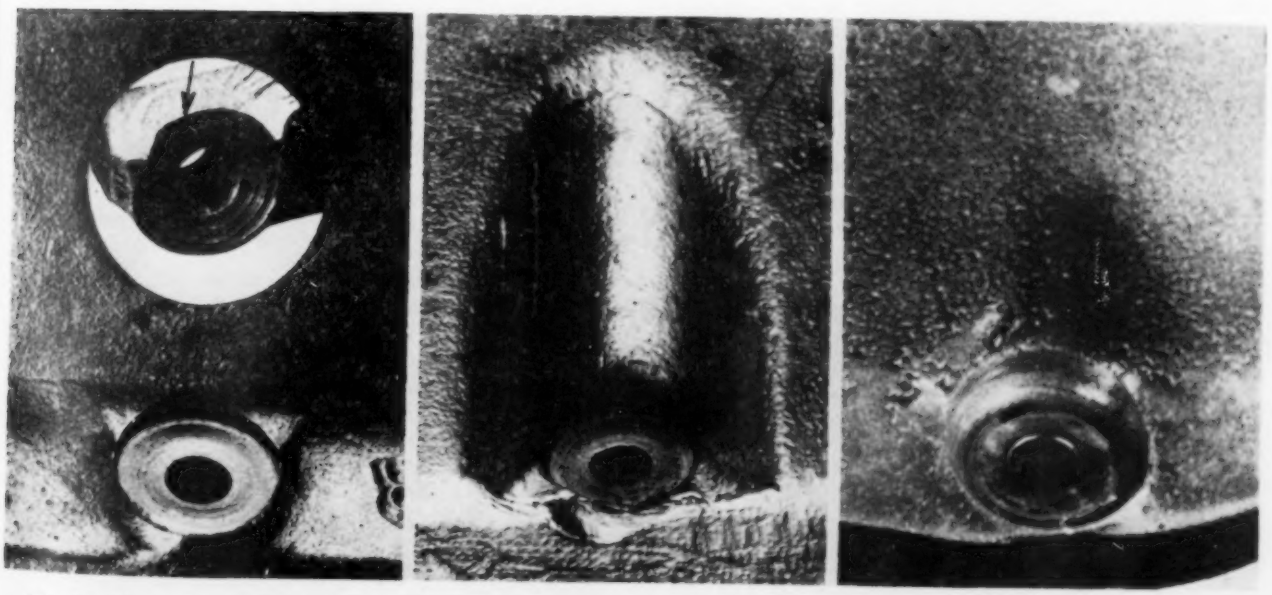


Fig. 16—Spotface designs, Type 1 (left), flange external of wall; Type 2 (center) flange internal of wall; and Type 3 (right) flange on centerline of wall. Arrow points to origin of failure in Type 1.

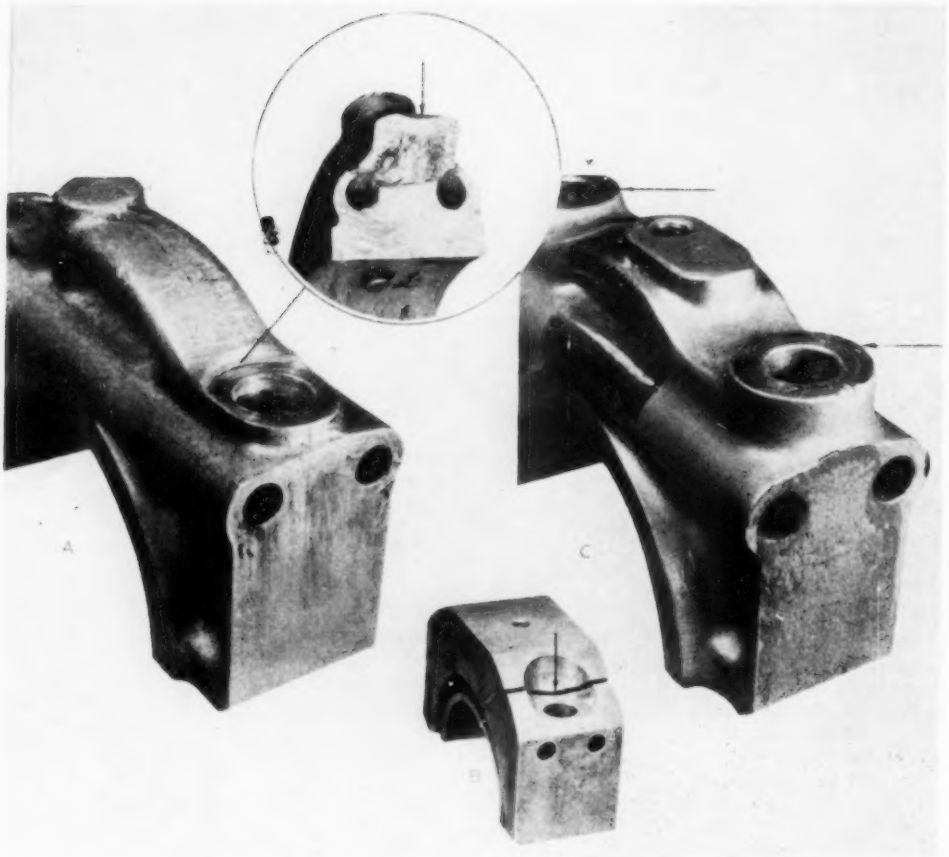


Fig. 17—High tensile prestress in spotface of a main bearing resulted in fatigue failure (A). Redesign (B) of (A) also failed in spotface. Design (C) with raised bosses on original design eliminated high tensile prestresses and ultimate failure.

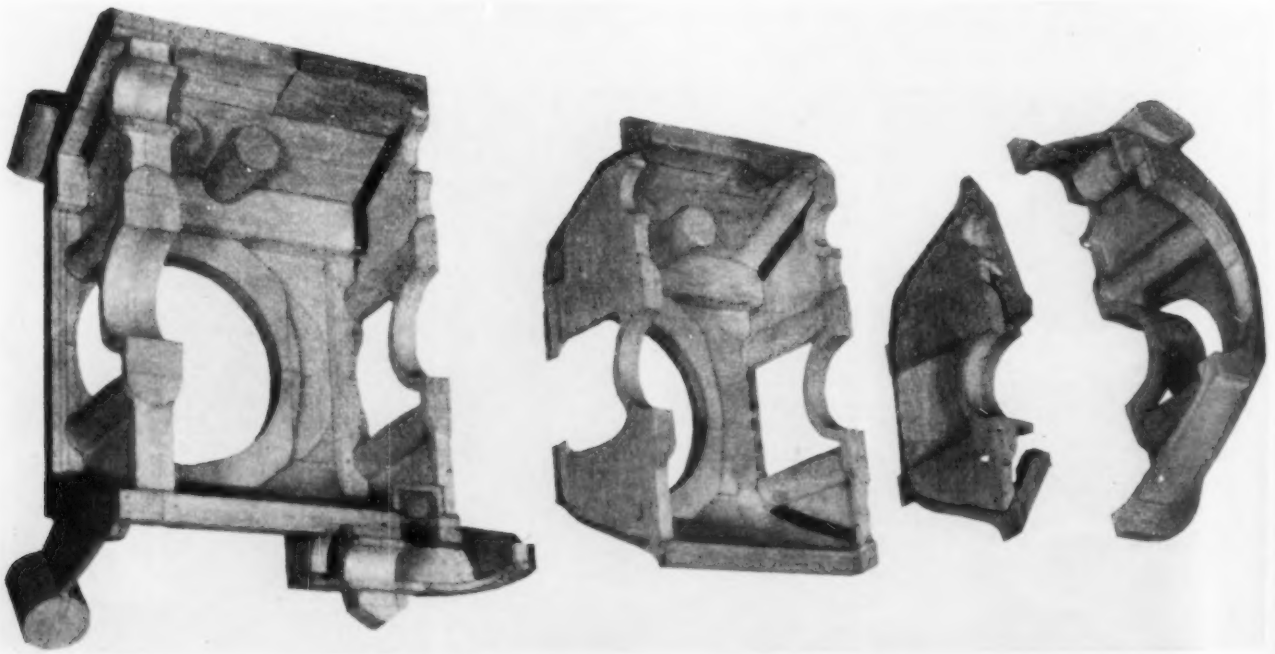


Fig. 18—Wooden mock-up of proposed design. A typical crankcase bay is shown.

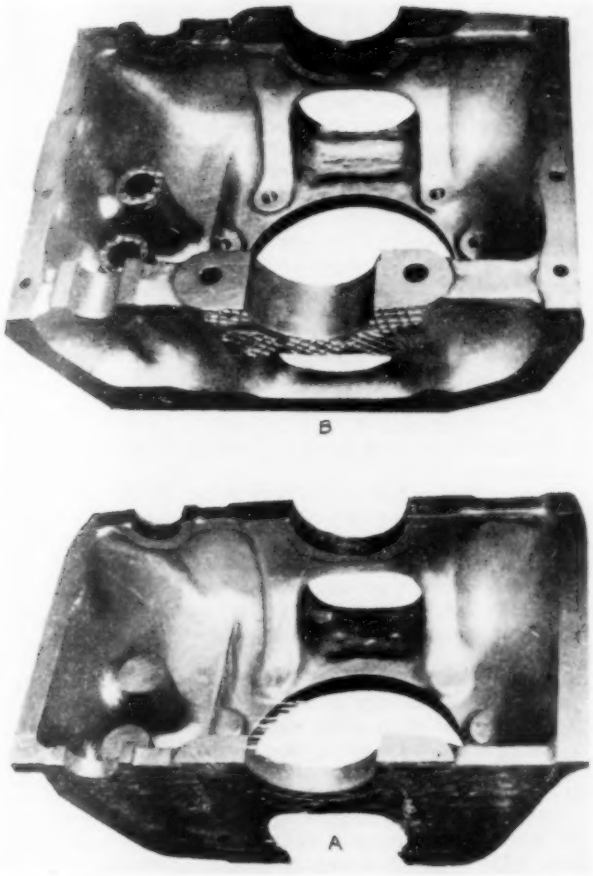


Fig. 19—Molding clay applied to mock-up, A, (Fig. 18) to complete structural details. Finished casting is shown at B.

A better casting design is achieved when the metal is removed from redundant elements and from the point of inflection and placed at points of maximum moment. Bending moments are generally maximum at wall intersections, therefore large fillets and gradual transition in wall sections will increase the load-carrying ability of the casting because the stress concentration is reduced and the section modulus is increased at the critical area.

The simplification of casting design permits the use of magnesium in highly loaded engine structures such as crankcases and results in further weight savings. When the metal is more efficiently distributed through the use of larger fillets, the stress concentration is not only reduced and the section modulus increased, but the low modulus of elasticity of the magnesium metal is more than offset by the increased rigidity of the structure. A ribless crankcase (Fig. 13) was cast in magnesium and had twice the endurance strength of the former ribbed aluminum design, and yet was 25 per cent lighter than the aluminum design. Partial removal of ribs is not recommended because the remaining ribs will be overstressed. The internal ribs of the aluminum crankcase (Fig. 13) were stresscoated. The stress distribution in these ribs was good (Fig. 14) and the maximum stress level was below the endurance limit of the material. In order to reduce weight, most of the ribs were removed "since they were acting as stress-raisers anyway" (Fig. 15). There was no redistribution of metal, however, and the stress on the remaining rib was more than doubled in value and service failures were encountered.

Stress Concentrations in Castings

Attention must be directed to small details because the strength of a casting is limited by the "sore" spots (stress concentrations) regardless of the cross-sectional area present. In light metal alloys spotface designs require particular attention. Three types are illustrated (Fig. 16); (1) flange external of wall, (2) flange internal of wall, and (3) flange on centerline of wall. In type (1) the bending moment is maximum at the point of maximum stress concentration and maximum tensile pre-stress from the bolt-fastening. Small cyclic loads doom this design to failure. When the flange fastening is internal of the wall, type (2), the stress distribution is improved but local bending cannot be avoided in the wall sections above the spotface. The lighter design, type (3), is considered the most efficient design; the flange is stiffer, has minimum bending moment, incorporates a larger fillet in the spotface, and does not require bending. A superior fastening design is achieved, however, when the spotface is eliminated entirely. A high tensile prestress in the spotface of an aluminum main bearing cap resulted in the fatigue failure as indicated (Fig. 17-A). Main bearing cap "B" was a "built up" version of cap "A" but it failed also in the spotface. Raised bosses were provided on the original design (cap "C"), the high tensile pre-stresses were removed, and the failures were eliminated.

Studs and thread forms also require special design consideration. A stud in a blind hole presents a serious stress condition (Fig.8). High stress concentration is inherent due to the thread form, but when the stud is tightened the condition is aggravated due to the high tensile stress gradient developed at the last engaged thread of the stud. As previously indicated, there are several approaches to the solution of this problem: (1) drill and tap through, if at all possible, (2) alter thread form in order to reduce the stress concentration factor, (3) modify the design of the stud in order to reduce the stress gradient (drill the end of the stud and taper the threads at the very end of the stud), and (4) alter the stress distribution in the casting by changing the casting design.

Wooden Mock-up Used

The procedure used to obtain a simplified casting design, free of serious stress concentrations is to make a wooden mock-up of the proposed design. Note the typical crankcase bay (Fig. 18). Next, molding clay is applied to complete the structural details (Fig. 19-A). Detail drawings of the model are made from templates cut to fit the model's contours. The finished casting is shown in Fig. 19-B and is a faithful reproduction of the model. This design procedure will result in (1) reduction in the designer's drafting time, (2) simpler pattern equipment, (3) a design that is easier to cast, (4) a stronger casting, (5) a more economical casting because it is (6) lighter.

DISCUSSION

Chairman: R. T. WOOD, American Magnesium Corp., Cleveland

Co-Chairman: HIRAM BROWN, Solar Aircraft Corp., Des Moines, Iowa

CHAIRMAN WOOD: The question I have is regarding the value of compressive prestressing. What is the value of shot peening on perhaps sand blasting magnesium or aluminum surfaces in order to increase the compressive prestress?

MR. BEAN: Cold-working magnesium either by machining, shot peening, or sand blasting is beneficial—the endurance of the material is increased. The benefits obtained by cold working a surface are two-fold: (1) the physical properties at the surface are improved and (2) surface compressive prestress is obtained. Magnesium, however, due to its low yield point at engine operating temperatures, is not likely to retain much of the surface compressive prestress. Therefore, magnesium castings should be more residual stress-free than aluminum castings. High residual tensile stresses are detrimental to the endurance strength of castings from either alloy.

Our service experience with aluminum indicates that the chilled surface of the casting is superior to cold-worked surfaces. It seems that the cast surface, if smooth, should not be disturbed. At Continental we are quite particular about burring operaions on highly stressed engine castings and are of the opinion that most foundries do an excessive amount of aimless burring. Flash should be removed, but the cast surface should be left alone. Burring to remove flaws should be done at the option of the manufacturer and not the foundry.

C. E. NELSON [1]: I would like to ask Mr. Bean if he would care to make any kind of statement as to the importance of the proper design in a casting as compared to metallurgical factors like grain size or porosity or something of that sort in light metal castings with respect to serviceability.

MR. BEAN: We have good metals—the metallurgist has been doing a splendid job. He has been doing his job much better than the designers have. Yet, in times past, the metallurgist did not question design and therefore was hard put to select a material that would make a poor design work. His testing methods did not evaluate design—they evaluated the quality of a material. If a good material failed due to poor design he had to come up with a better material—and quite frequently did!

Now, largely due to new techniques in the field of design analysis, our approach to the solution of a service failure is different: ***First, we question the design and second, the metallurgy.*** To illustrate the importance of *both* factors the following personal experience is related: In the aluminum camshaft housing which previously was discussed, the internal ribs had been failing in fatigue as indicated. The stress department came up with a new rib profile which resulted in a 17 per cent reduction in stress. Since this new profile could be obtained by modifying an existing casting, the design was endurance tested immediately. It broke much sooner than the original design and perturbed the stress boys no end! Because we believed the design analysis was satisfactory, an investigation of the material was made. The Brinell hardness checked O.K. In fact, it was identical with that of the first casting. Test bars from the two castings were up to specifications. Elongation was satisfactory. Samples from each casting checked as far as chemical analysis. The microstructure was normal. X-ray showed slightly more porosity in the latter casting. Maybe that was the answer, but then, other castings were performing satisfactorily in spite of porosity. Summing up, we had not learned a thing from the metallurgical investigation thus far. The author, therefore, took a 4-ft. piece of 2-in. pipe and proceeded to break up the remains of the two castings. This test method revealed some important evidence: The original casting would bend upon impact while the modified casting would crack and remember, the test bars that were pulled had indicated the same elongation! Upon questioning the purchasing agent, I found that a different foundry had cast the second camshaft housing—at last I was getting somewhere. Test coupons cut from the casting from the new source indicated a tensile strength anywhere from 17,000 to 25,000 psi. with a BHN of 80. The casting from the original source, when tested, indicated a tensile strength between 36,000 and 40,000 psi, with a BHN of 80.

Fortified with test coupons from each casting, I went to the new foundry and sought out the plant superintendent. I set one sample on the floor and bent it by stamping upon it, saying that it represented a casting from another foundry. I then set the second sample on the floor saying, "This is from your foundry." When I came down on it with my heel, it snapped into two pieces. I said, "Tell me, what is the difference?"

He said, "Come to the laboratory and I will let you talk to the metallurgist."

I said, "Let us talk to the men in the foundry who poured these castings." The men were located and the discussion led us nowhere until one said, "Your pattern equipment is in bad condition. The casting was difficult to pour." I said, "O.K., but for some reason the other foundry could pour good castings with it." He said, "Well, we poured it all right too, but we had to raise the metal temperature about 200° F. to do it."

I didn't know what they had done but whatever it was, it had reduced the endurance strength of the casting and it was not caught in the laboratory. We learned from this experience to be more concerned about the quality of the material as received in a fabricated structure than in the test bar. Test bars have their place and will yield fundamental information to the metallurgist, but we do not build engines out of test bars.

Referring again to magnesium castings, we are quite concerned about surface characteristics as revealed by visual inspection. Even though internal shrinkage may be present, our experience has been that if the surface is smooth and not interrupted by flaws, good endurance characteristics will be obtained. On the other hand, castings with no shrinkage and with superior tensile properties but with poor surface characteristics are no good at all. If foreign matter such as flux breaks through the surface, the tensile strength may not be impaired but the endurance strength assuredly is. I believe the only way to keep foreign material away from the surface is to keep it out altogether.

As for porosity in aluminum, we are not concerned as long as the density is not affected too much. We endeavor to maintain oil-tight castings in the manufacture of engines. We can seal porous castings but prefer that the foundry seal them with metal. More trouble with porosity has been experienced with aluminum than with magnesium. The magnesium castings generally exhibit a more dense structure.

Finally, we come to the most serious problem of all—price. Production engines are sold by the pound. The purchasing agent battles on the cost per pound front and is hit hard when he tries to use light metal castings. We would use a great deal more magnesium and aluminum in our engines if the price were more competitive with cast iron. Aircraft engines must fly, therefore light metals win out in spite of higher prices.

If more emphasis is placed on the simplification of casting design and more is done to improve mass production techniques in the foundry, and if base metal costs are cut, the use of magnesium and aluminum by American industry will be greatly expanded.

[1] Dow Chemical Co., Midland, Mich.

CUPOLA MELTING PHENOMENA

By
E. V. Somers[1] and D. W. Gunther[2]

ABSTRACT

There have been many discussions on the cause of variations in the chemical and physical properties of gray iron, but the effect of melting temperature has been generally disregarded. The effect of melting temperature, however, has been found to be potent, and the following phenomena have been observed:

1. The melting temperature affects the chemical composition of gray cast iron melted in a cupola.

2. The melting temperature affects the tensile strength and the chill depth of gray cast iron independent of the chemical analysis and the percentage inoculation.

3. The melting temperature affects the slag composition.

4. The above items can be explained on the basis of a varying amount of oxidation of the iron during the melting process.

MANY OF THE CAUSES of variations in the chemical and physical properties of gray iron have been investigated and the results of these investigations have been widely discussed in foundry publications. However, one variable, that of melting temperature, which may be assumed to be affected by the summation of the changes in coke ratio in this investigation from 5.5:1 to 7:1 plus the uncontrolled variables of cupola operation, has been greatly disregarded by most investigators. The effects of this variable on both chemical and physical properties are quite great within the normal variation of melting temperatures that are encountered in the manufacture of cast iron in a cupola.

The results of this investigation are split into two different groups of data. The first group determines the variation of both chemical and physical properties of the iron with a charge of constant composition. The charge was constant with respect to the metallic components—steel, pig iron, and foundry scrap—and with respect to the percentage of carbon and silicon contained in these components. The amount of coke in the charge was varied to change the tapping temperature. Because of the dependency of physical properties on chemical composition and inoculating additions, the second group of data contains the results of variation of physical properties with tapping temperature at constant chemical composition of the iron plus a constant percentage of inoculating addition.

[1] Manufacturing Engineer and [2] Formerly, Manufacturing Section Engineer, Westinghouse Electric Corporation, Trafford Foundry, now with National Radiator Co., Johnstown, Pa.

Tapping Temperature Used

In this investigation the tapping temperature was recorded throughout. Since the operation of the cupola was intermittent, the following method was used to obtain temperature readings comparable to a continuously tapping unit: Results on taps greater than 3000 lb were the only ones considered, and all tapping temperatures were taken less than one minute before the cupola was plugged. In this manner cooling effects in the basin were minimized. It is probable that a fairly constant relationship holds between the temperature of the iron passing through the free oxygen zone during melting (defined as melting temperature in this report) and the tapping temperatures taken as described above. From this, a high melting temperature is inferred from a high tapping temperature, and vice versa.

Melting temperature is the point of departure in this report; the variation in coke charged accounted for most of this temperature variation. The tapping temperatures, which are correlated with the physical and chemical properties of the resultant iron, can then be assumed to indicate the temperature at which any proposed oxidizing reactions occur.

The results are shown in both tabular and graphical form. In order to simplify the presentation the average results $\pm$ limits to indicate uncertainty of the average are given. This method is briefly discussed in the Appendix.

The first investigation was conducted on a charge used to melt iron with a 30,000 psi minimum for the tensile strength test. The following variables were held constant: the composition of the metallic charge, the blast volume, the cupola diameter at 60 in., and the amount of the limestone addition.

Slag Specimens Prepared

Chemical and physical tests were taken on each test bar poured during this investigation, and a slag sample was taken with a slag-spoon about a minute before the tap was made. For basic slag a rough correlation exists between the surface appearance and the iron oxide content; a similar relationship was assumed to exist for the acid slag of a cupola. A set of ten different cupola slags was selected to best repre-

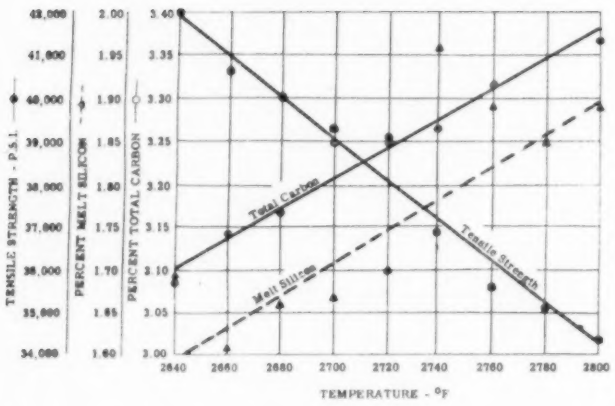


Fig. 1—Effect of tapping temperature on total carbon content, melt silicon (estimated by subtracting inoculant silicon from total silicon), and tensile strength of an ASTM Designation: A 48–41 Gray Cast Iron. The composition of the metallic cupola charge was held constant.

sent the complete variation of surface colors. The change in coloration was checked against the iron oxide content as the color of the slag turned from white, tan, tan-black, black, to dull gray (appearance designation from A to J).

The data showing the variation in carbon content, silicon content, tensile strength, chill depth, and slag appearance are tabulated in Tables 1, 2, and 3 and are plotted in Figs. 1, 2, and 3. The following facts can be observed about the chemical composition of the iron and slag from these data:

1. The carbon content decreases as the tapping temperature decreases.
2. The silicon content decreases as the tapping temperature decreases.
3. The iron oxide content of the slag increases as the tapping temperature decreases.

The data for the silicon content, given in Table 1, show the greatest variation. This is probably due to the method of calculating the melt silicon content from the final analysis. Both the error in the percentage of the addition and the error in the amount of inoculant dissolved would be included in the ± limits for the melt silicon. The data for the chill

TABLE 1.—EFFECT OF TAPPING TEMPERATURE ON CARBON AND SILICON CONTENT AND TENSILE STRENGTH OF A 48–41 GRAY CAST IRON

The composition of the metallic charge was held constant. (Results given for $P_s = 0.90$)

Tapping Temperature, °F	No. Samples	Carbon, %	Silicon, %	Tensile Strength, psi
2640	3	3.08 ± .10	1.69 ± .24	42,000 ± 1320
2660	6	3.14 ± .07	1.61 ± .14	40,600 ± 1,140
2680	12	3.17 ± .03	1.66 ± .09	40,100 ± 1,140
2700	13	3.25 ± .03	1.67 ± .07	39,300 ± 1,310
2720	13	3.25 ± .04	1.70 ± .11	39,100 ± 1,180
2740	6	3.27 ± .04	1.97 ± .15	36,900 ± 1,580
2760	6	3.32 ± .05	1.89 ± .10	35,600 ± 1,250
2780	4	3.34 ± .19	1.85 ± .18	35,100 ± 7,100
2800	4	3.37 ± .13	1.89 ± .25	34,300 ± 1,330

TABLE 2—EFFECT OF TAPPING TEMPERATURE ON CHILL DEPTH OF A 48–41 GRAY CAST IRON

The composition of the metallic charge was held constant. (Results given for $P_s = 0.90$)

Tapping Temperature, °F	.125% Ca-Si-Ti Chill Depth*	No. Samples	.125% Ca-Si-Ti .10/.15% Fe-Si Chill Depth*	No. Samples	.125% Ca-Si-Ti .20/.40% Fe-Si Chill Depth*	No. Samples
2640					4.2 ± .0.9	10
2660			4.2 ± 1.7	5	4.0 ± 0.5	23
2680	4.7 ± 1.3	6	4.3 ± 0.4	23	3.7 ± 0.3	42
2700	5.3 ± 1.0	7	3.7 ± 0.3	34	2.9 ± 0.3	34
2720	4.7 ± 0.6	15	3.5 ± 0.4	34	3.0 ± 0.3	32
2740	4.6 ± 0.5	17	3.4 ± 0.3	43	2.8 ± 0.5	15
2760	3.8 ± 0.5	23	3.0 ± 0.4	28	2.3 ± 0.9	10
2780	3.1 ± 0.6	16	2.7 ± 0.6	14	2.4 ± 0.9	5
2800	3.2 ± 0.7	9	1.8 ± 1.0	6		

* Chill depth measurements are recorded in sixteenths-of-an-inch.

depth given in Table 2 show large ± limits. A determination of the standard deviation of the chill test reveals that the large ± limits in Table 2 are mainly due to the inaccuracy of the chill test itself.

Molten Iron Oxidizes

Since the source of iron oxide in the slag is the iron in the charge, it is not unreasonable to assume that the increase of iron oxide in the slag is the result of an oxidation of the molten iron. The decrease of both the carbon and silicon contents accompanies an increase in the iron oxide content of the slag.

The direction of temperature dependence of these trends further corroborates the assumption of an oxidation on the basis of four simple oxidation reactions, which follow below. The question of carbon solubility at different temperatures must also be considered. These reactions are valid in the temperature ranges of 1500 Kelvin (approx. 2250 F) and 2200 Kelvin (approx. 3500 F).[1, 2]

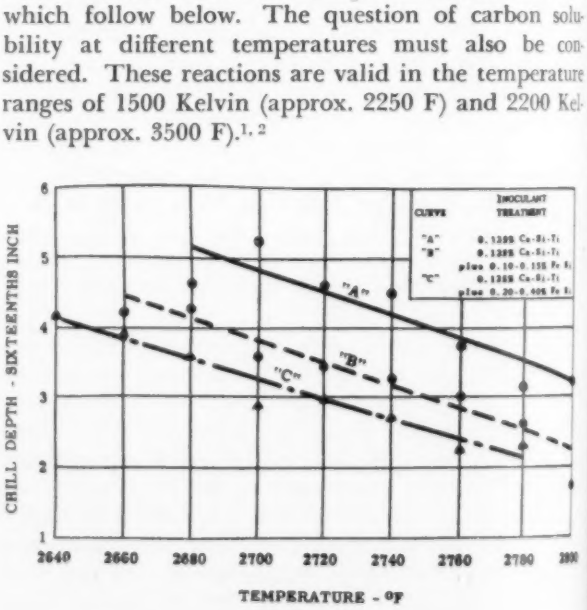


Fig. 2—Effect of tapping temperature and inoculation treatment on the chill depth of ASTM Designation: A 48–41 Gray Cast Iron. The composition of the metallic cupola charge was held constant. Note the required use of large quantities of inoculant at low tapping temperatures in order to maintain satisfactory chill depths.

TABLE 3.—EFFECT OF TAPPING TEMPERATURE ON SLAG APPEARANCE

Tapping Temperature, °F	Slag Designation* A	B	C	D	E	F	G	H	I
	1 to 5% FeO			2 to 8% FeO			4 to 12% FeO		
2620				1			1	3	1
2640				1	3			6	1
2660			1		3	2	2	7	3
2680			2	3	4	8	13	6	6
2700	1	2	5	8	6	8	7	8	
2720		3	13	7	5	9	3	2	
2740		6	11	13	6		1	1	1
2760	1	9	12	6	6		2		1
2780	3	11	11	1	4	2			
2800	3	5	2	1	1	1			

* Numbers refer to frequency of occurrence.
Slag type classified by appearance with colors running from white, tan, tan-black, black, to dull gray with letters running from A to J respectively.

1. $Fe\ (liq.) + \frac{1}{2}\ O_2 = FeO\ (liq.)$
$$\log K = \frac{8710}{T} - 0.752$$
2. $Si\ (liq.\ Fe) + O_2 = SiO_2\ (solid)$
$$\log K = \frac{31200}{T} - 4.57$$
3. $C\ (liq.\ Fe) + O_2 = CO_2$
$$\log K = \frac{19880}{T} - 1.282$$
4. $C\ (liq.\ Fe) + \frac{1}{2}\ O_2 = CO$
$$\log K = \frac{5330}{T} + 3.158$$
5. $C\ (graphite) = C\ (liq.\ Fe)$
$$\log K = \frac{188}{T} + 1.665,$$

where K is the equilibrium constant for the reaction, T is temperature of the reaction in degrees Kelvin, and *log K* is the Briggs logarithm of the numerical value of K, the value of the equilibrium constant for the reaction.

As T increases for reactions (1), (2), (3), and (4), the *log K* decreases and K decreases. As T increases for reaction (5), the *log K* increases and K decreases. The oxidation of iron, carbon, and silicon by these reactions would be more likely at 2660 F than at 2800 F. Reaction (5) indicates an increasing solubility of carbon at higher temperatures, but the change in K is not great for this reaction because the standard heat content value (included in the coefficient of $1/T$) is very low. Any textbook on the thermodynamics of chemical reactions will explain the principles of the temperature variation of these reactions.[3]

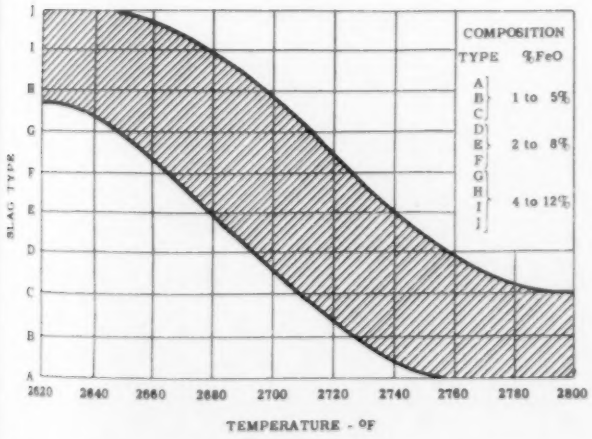


Fig. 3—Effect of tapping temperature on slag type (related to per cent FeO) with constant limestone addition. Slag type classified by appearance, per cent FeO range listed is typical for slags of that appearance, irrespective of the effects of lime and silica.

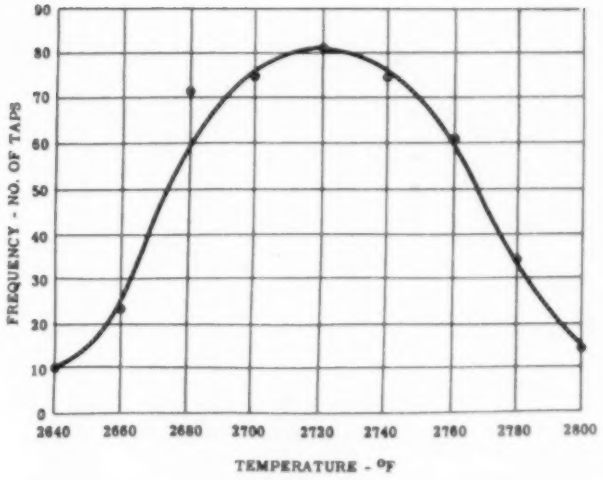


Fig. 4—Frequency distribution of tapping temperatures taken during the test with a constant charge composition.

Two important facts concerning the physical properties of the iron can be noted from these data: (1) cast iron can be melted to conform to A.S.T.M. designation A 48–41, classes 30, 35, or 40 by altering the tapping temperature of the iron and without changing the metallic composition of the charge; (2) the chill depth increases considerably at low tapping temperatures.

Cupola Tapping Temperatures

The distribution of tapping temperature of a cupola must be considered. The tapping temperatures occurring during this test (taken from Table 2) are tabulated in Table 4 and are shown in Fig. 4. Although different coke additions were used, their effect was mainly one of increasing the spread of temperatures. It is probable that 100 F will cover the temperature variation of most cupolas under accurately controlled

TABLE 4.—DISTRIBUTION OF TAPPING TEMPERATURES TAKEN DURING THE TEST WITH A CONSTANT CHARGE COMPOSITION

Tapping Temperatures, °F	Frequency
2640	10
2660	23
2680	71
2700	75
2720	81
2740	75
2760	61
2780	35
2800	15

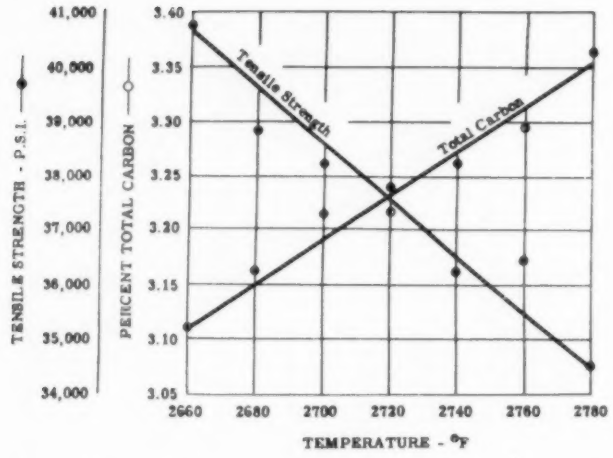


Fig. 5—Effect of tapping temperature on the carbon content and tensile strength of an ASTM Designation: A 48–41 Gray Cast Iron. The composition of the charge was adjusted from day to day to meet production requirements.

conditions with a constant coke addition. The variation of both physical and chemical properties within the determined range of temperatures must then be considered.

A further check of the results of a six-month production run is tabulated in Table 5 and is plotted in Fig. 5. During this interval the attempt was made throughout various charge changes to control the carbon percentage of the resulting iron between 3.15 and 3.28 per cent. However, the silicon content of the charge was varied considerably during this period to suit various production requirements, and the test bar results for silicon content cannot be used. The carbon content and tensile strength show very good relationship with tapping temperature. The larger number of samples included in this period as compared with the number of samples taken during the test using a constant composition reduce the ± limits of both the carbon content and tensile strength considerably. These data also demonstrate the role of tapping temperature in normal production operations.

TABLE 5.—EFFECT OF TAPPING TEMPERATURE ON THE CARBON CONTENT AND TENSILE STRENGTH OF A 48–41 GRAY CAST IRON

The composition of the charge was adjusted from day to day to fit production requirements; it was attempted to hold the carbon content between 3.15 per cent to 3.28 per cent and to vary the silicon content.

(Results given for $P_s = 0.90$)

Tapping Temperature, °F	No. Samples	Carbon, %	Tensile Strength, psi
2660	12	3.12 ± .03	40,800 ± 780
2680	37	3.17 ± .02	38,800 ± 620
2700	36	3.22 ± .02	38,200 ± 760
2720	24	3.23 ± .03	37,800 ± 890
2740	13	3.27 ± .03	36,300 ± 920
2760	11	3.29 ± .05	36,600 ± 1,370
2780	6	3.37 ± .11	34,700 ± 3,820

CaO in Slag Approaches Maximum

A series of slag samples was collected at different tapping temperatures during the latter test. The limestone of the charge was varied from two per cent to four per cent of the metallic charge weight. The analyses of these samples are grouped into three tapping temperature ranges in Table 6, and the analyses, converted to a 100 per cent FeO, CaO, and SiO_2 basis, produce a three phase diagram as shown in Fig. 6. The grouping of the various analyses into temperature bands reveals a liquidus surface of the CaO-FeO-SiO_2 system in the range of cupola slags. It was also noted during these tests that doubling the limestone addition did not double the per cent CaO in the slag; at most, the increase in per cent CaO approaches a maximum of 35 per cent, and additional limestone results in bad erosion of the cupola basin.

Since the physical properties are affected by the chemical properties of the iron, the effect of tapping temperature on physical properties, only, must be checked with results of equivalent chemical composition. The second investigation was conducted for this purpose, and the results of all test bars poured in a given period were checked for a chemical analysis corresponding to 3.80 to 3.95 per cent carbon equivalent. The physical properties corresponding to the selected tests were tabulated, and the results of tensile strength and chill depth versus tapping temperature are given in Tables 7 and 8 and are plotted in Figs. 7 and 8.

The variable of inoculation percentage had to be held constant for these results in order to obtain better correlation. A pronounced increase in chill depth explains the use of the common term, "hard" or "oxidized" iron. The reason for the variation in tensile strength is obscure; the variation in the slope of

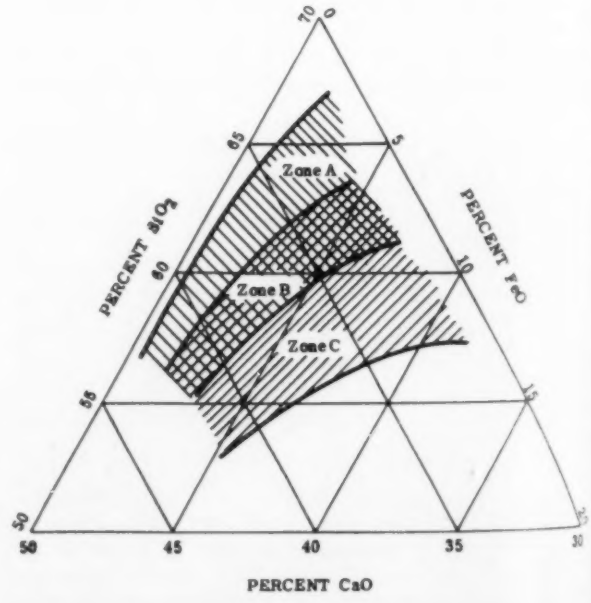


Fig. 6—Constitutional diagram of cupola slags. All components reduced to a 100 per cent FeO-CaO-SiO_2 basis. Zone A, tapping temperatures 2740–2780 F; Zone B, 2700–2720 F; Zone C, 2640–2680 F.

these curves at different inoculation amounts cannot be reasonably explained at present.

Conclusions

The following points can be summarized from the test results obtained:

1. The tapping temperature (melting temperature as defined in the first part of the report) affects the chemical composition of gray cast iron melted in a cupola.

2. The composition of the cupola slag, especially the FeO content, is affected by the tapping temperature.

3. The temperature dependence of the carbon and silicon contents of the iron and of the FeO content of the slag can be explained on the basis of a varying amount of oxidation of the iron during the melting process.

4. The reduction of chill depth by use of inoculants can then be explained partially on the basis of deoxidation, as powerful deoxidizing elements are contained in most commercial inoculants. Although this report deals with data that were taken using a Ca-Si-Ti type of inoculant, any of the other common silicon alloyed types containing aluminum, nickel, zirconium, etc., are satisfactory for reducing chill depth.

5. The composition and surface appearance of cupola slag is dependent on tapping temperature. The composition of the slag appears to be determined by a liquidus surface, which is influenced mainly by the relative amounts of CaO, FeO, and SiO_2 at any given temperature.

6. The depth of chill varies with tapping temperature at any constant chemical analysis and with a fixed percentage of inoculation. This variation is such that if a variable state of oxidation is assumed as an explanation, a higher chill depth occurs with a higher state of oxidation.

7. The tensile strength varies with tapping temperature at any constant chemical analysis and with a fixed percentage of inoculation. The variation is not too reasonably explained at present.

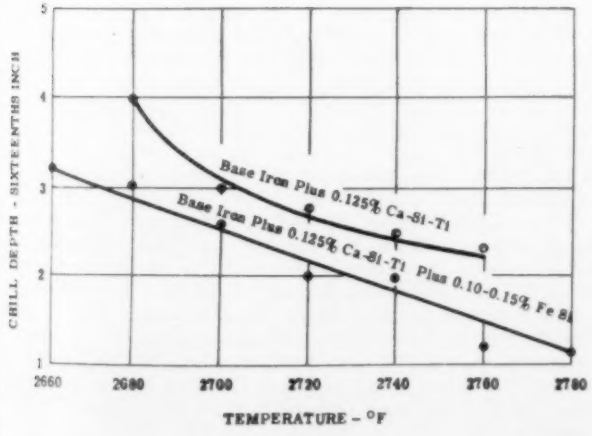


Fig. 7—Effect of tapping temperature and inoculation on chill depth of an ASTM Designation: A 48–41 Gray Cast Iron with a chemical composition of 3.80 to 3.95 per cent carbon equivalent (per cent T. C. plus ⅓ per cent Si).

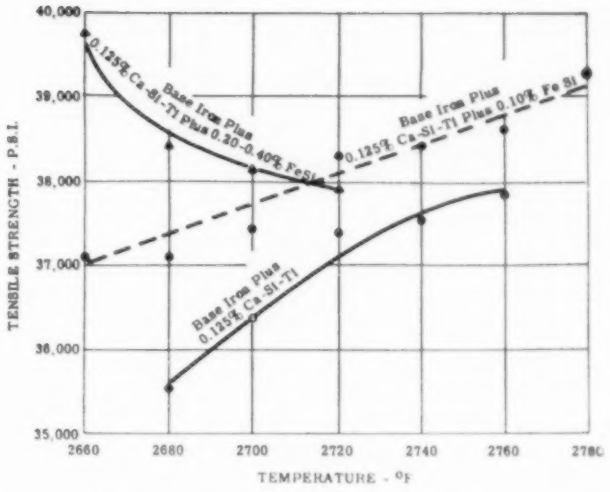


Fig. 8—Effect of tapping temperature and inoculation on the tensile strength of an ASTM Designation: A 48–41 Gray Cast Iron with a chemical composition of 3.80 to 3.95 per cent carbon equivalent (per cent T. C. plus ⅓ per cent Si). Note beneficial effect of correct inoculating technique and effect of over-inoculation.

Appendix

The values of the observations listed in the tables are reported as:[4]

$$\overline{X} \pm a\ \sigma\ (Ps = 0.90)\ (n\ \text{observations}),$$

where $\overline{X}$ is the average value of n observations, σ is the root-mean square deviation about the average $\overline{X}$, a is a computed numerical value for the given number of observations n and for a given statistical probability Ps. Thus for these data, $\pm$ a σ are the limits within which $\overline{X}$ may be expected to lie nine times out of ten, in problems involving a sample of n observations.

A result can then be presented as follows:

Tapping Temperature	*Silicon, %*	*No. Samples*	*Ps*
2720 F	$1.70 \pm 0.11\%$	13	0.90

It will be observed that the limits of some of the data are large. This is due to a large σ, a large a, or a combination of both. Qualitatively, a smaller number of samples increases the numerical value of a. This can be seen in comparing the $\pm$ limits for per cent carbon in Tables 1 and 5. Wherever the $\pm$ limits were found to be large, they are reported as such, as it would be unreasonable to expect uniform results of averages and limits with so small a number of samples.

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4. *ASTM Manual on Presentation of Data.*

TABLE 6.—CUPOLA SLAG COMPOSITIONS AT VARIOUS TAPPING TEMPERATURES

Tapping Temperature, °F	% CaO	% FeO	% SiO_2	(100% CaO + FeO + SiO_2 Basis) % CaO	% FeO	% SiO_2
2680 and Lower	31.3	1.9	46.0	39.5	2.4	58.1
	34.5	1.4	43.1	43.7	1.8	54.6
	35.2	1.0	43.7	44.1	1.3	54.6
	25.2	6.3	45.4	32.8	8.2	59.0
	26.6	6.2	43.3	34.9	8.1	57.0
	28.5	4.9	44.5	36.6	8.3	57.1
	29.1	2.8	43.6	38.5	3.7	57.8
	28.9	4.7	44.8	36.9	6.0	57.2
	27.1	2.9	44.5	36.3	4.0	59.2
	27.0	1.6	44.5	36.9	2.2	60.8
	36.3	1.4	41.3	46.0	1.8	52.3
	29.6	2.0	46.6	37.8	2.6	59.8
	33.5	1.9	45.1	41.6	2.4	56.2
	30.1	3.4	42.1	39.8	4.5	55.6
	20.1	8.0	49.4	25.9	10.3	63.5
	18.3	10.8	45.4	24.6	14.5	61.0
2700–2720	33.0	0.5	46.0	41.6	0.6	57.9
	25.4	1.6	48.3	33.9	2.1	64.5
	34.3	0.4	42.8	44.2	0.5	55.2
	32.3	1.8	42.9	41.9	2.3	55.9
	30.8	2.0	45.0	39.6	2.6	58.0
	29.2	1.4	46.5	37.9	1.8	60.3
	32.7	2.0	43.9	42.7	2.6	57.4
	32.0	1.7	44.6	40.9	2.2	57.0
	31.0	2.1	45.4	39.5	2.7	57.9
	31.9	1.9	44.3	40.8	2.4	56.8
	32.2	2.9	45.2	40.1	3.6	56.2
	32.7	3.3	44.0	40.9	4.1	55.0
	36.8	2.1	41.5	45.8	2.6	51.6
	36.2	2.3	43.0	44.4	2.8	52.8
	33.7	1.4	45.2	42.0	1.7	56.2
	30.0	1.7	46.6	38.3	2.2	59.5
	31.9	3.4	45.0	39.7	4.2	56.1
	22.0	6.4	47.0	29.2	8.5	62.4
	29.6	4.9	45.9	36.8	6.1	57.1
2740 and Higher	31.0	1.0	43.5	41.2	1.3	57.5
	32.4	1.1	42.0	42.8	1.5	55.7
	31.0	1.2	43.0	41.3	1.6	57.2
	26.3	1.5	50.8	33.5	1.9	64.6
	30.7	1.7	47.3	38.6	2.1	59.3
	29.3	1.9	45.0	38.5	2.5	59.1
	32.4	2.1	43.5	41.6	2.7	55.8
	35.8	1.5	42.1	45.2	1.9	53.1
	35.2	1.6	41.8	44.8	2.0	53.3
	33.6	1.9	41.1	43.9	2.5	53.7
	30.6	0.9	44.1	40.5	1.1	58.2
	26.2	1.6	50.6	33.4	2.0	64.6
	32.8	0.8	43.7	42.4	1.0	56.6
	29.8	1.4	46.0	38.5	1.8	59.6
	34.7	1.2	42.8	44.1	1.5	54.4
	34.7	1.7	42.8	43.8	2.1	54.1
	31.0	1.3	41.9	41.8	1.8	56.5
	25.9	1.3	51.7	32.8	1.6	65.6
	26.0	0.9	49.8	33.5	1.1	64.4
	24.4	0.7	51.0	32.1	0.9	67.0

DISCUSSION

Chairman: R. G. McELWEE, Vanadium Corp. of America, Detroit

Co-Chairman: W. M. LEVI, Lynchburg Foundry & Machine Co., Radford, Va.

E. A. LORIA and A. P. THOMPSON[1] *(written discussion):* The explanations for cupola melting phenomena are many and in some respects complicated by several intervening variables. Certainly this paper deserves and requires much attention We need the point of view that the authors have with facility brought to such problems. Their paper invites an almost interminable discussion.

[1] Fellow and Senior Fellow, Mellon Institute of Industrial Research, Pittsburgh

TABLE 7.—EFFECT OF TAPPING TEMPERATURE ON CHILL DEPTH OF A 48–41 GRAY CAST IRON WITH A CHEMICAL COMPOSITION OF 3.80% TO 3.95% CARBON EQUIVALENT (PER CENT CARBON PLUS 1/3 PER CENT SILICON)

(Results given for $P_s = 0.90$)

Tapping Temperature, °F	.125% Ca-Si-Ti Chill Depth	.125% Ca-Si-Ti Chill No. Samples	.125% Ca-Si-Ti Plus .10/.15 Fe-Si Chill Depth	.125% Ca-Si-Ti Plus .10/.15 Fe-Si No. Samples
2660			3.2 ± 1.0	6
2680	4.0 ± 1.4	7	3.0 ± 0.9	7
2700	3.0 ± 0.8	6	2.6 ± 1.0	7
2720	2.8 ± 1.5	4	2.0 ± 0.7	8
2740	2.5 ± 1.5	4	2.0 ± 0.3	12
2760	2.4 ± 1.1	5	1.2 ± 0.5	5
2780			1.2 ± 0.8	4

TABLE 8.—EFFECT OF TAPPING TEMPERATURE ON THE TENSILE STRENGTH OF A 48–41 GRAY CAST IRON WITH A CHEMICAL COMPOSITION OF 3.80% TO 3.95% CARBON EQUIVALENT (PER CENT CARBON PLUS 1/3 PER CENT SILICON)

(Results given for $P_s = 0.90$)

Tapping Temperature, °F	Tensile strength at different inoculant percentages, psi: 0.125% Ca-Si-Ti	0.125% Ca-Si-Ti Plus .10 Fe-Si	0.125% Ca-Si-Ti Plus .20/.40 Fe-Si
2660		37,100 ± 740 (4)*	39,800 ± 1,260 (6)*
2680	35,500 ± 1,630 (3)*	37,100 ± 1,040 (4)	38,400 ± 490 (14)
2700	36,400 ± 1,690 (5)	37,400 ± 1,240 (4)	38,200 ± 630 (10)
2720	37,400 ± 1,070 (8)	38,300 ± 1,450 (6)	37,900 ± 1,010 (5)
2740	37,500 ± 1,710 (6)	38,400 ± 1,270 (7)	
2760	37,800 ± 990 (7)	38,600 ± 890 (6)	
2780		39,300 ± 1,220 (6)	

* No. of samples given in parentheses behind tensile strength figure.

The whole field of slag-metal reactions for cast iron is practically unexplored and deserving of study because of the relationship between such reactions and the type of iron melted. For example, the acidity or basicity of the slag may determine the FeO or SiO_2 content of the melt, which may in turn affect the tendency to under-cool.

Also, the work of Diepschlag[1] demonstrated that melting iron in contact with aluminous slags had a graphite refining effect and melting in contact with silicious slags a coarsening effect. On the other hand, Morrogh[2] has shown microscopically that cast iron is characterized by the absence of visible oxides and silicates under normal conditions. In this respect cast iron is much cleaner than steel and the character of the slag-metal equilibria involved make it so.

The immiscibility of slag and metal increases at lower operating temperatures and the effect of the higher carbon content is to increase the region of immiscibility of slag and metal. The Fe-C-Si constitution diagram shows that cast iron tends to be melted and cast at much lower temperatures than steel. In addition, the much higher carbon content of cast iron facilitates the separation of slag. This paper confirms the experience of the iron founding industry that slag composition does influence the chilling characteristics and the graphite size of cast iron.

Considering the experimental results more specifically, the following questions arise which we would like to refer to the authors:

1. Since they have shown that oxidation (tapping temperature) is the variable affecting the condition of iron produced under identical circumstances, would they care to speculate as to the effects of hydrogen (which varies with the moisture con-

tent of the blast) and atmospheric moisture on the depth chill and tensile strength?

2. In the authors' opinion, would not the presence of sulphur in cupola melting have a decided influence on chilling characteristics and a correlation with tapping temperature? The evidence is that sulphur behaves as a carbide stabilizer only when in the form of FeS but as MnS it behaves neutrally in the melt, thereby promoting the formation of flake graphite. Yet, oxides and silicates, particularly FeO, are known to be soluble in FeS, so that this compound may remove oxygen by partition.

3. Have the authors any data correlating the V' ratio (% CaO/% SiO_2) and hence the effect of slag on chill depth and tensile strength? It is known that the V' ratio of cupola slags varies between 0.4 and 0.9 but no significant trend of slag properties within this range has been reported.

The V' ratios for the cupola slag compositions at various tapping temperatures listed in Table 6 were calculated and found to vary markedly within the specified range so as to bear no apparent relationship with tapping temperature or FeO content. Rather the FeO is dependent on temperature and CaO content of the slag. In general, the higher the CaO content of the slag the stronger the tendency to fix excess oxygen; this may be connected with a formation of calcium ferrite.

The writers agree with the authors' general conclusion that the variation in chill depth is associated with a variable state of oxidation and would like to present some data to further substantiate these statements. In Table A are shown the characteristics, chemical composition and fusion point determinations for slag taken during cupola melting of gray iron for automotive blocks, the tapping temperature being in the range 2775–2850° F. For comparison purposes, slag samples A, B, and C were taken from silicon carbide-treated cupola mixtures while slag samples D and E were taken from untreated mixtures; each sample, however, being taken at the cupola slag spout. The samples were then analyzed for their chemical constituents and fusion point tests were made by placing 20 grams of each slag on fusion blocks and heating the samples in a Globar furnace. The heating rate averaged 248° F. per hours during the fusion period.

As may be seen from Table A, the time and temperature were noted when each slag first showed signs of melting, when it began to come over the lip of the fusion block and when it reached the bottom of the fusion block. The fluidity of each slag is indicated by the length of time it takes for the slag to flow down the block and the temperature interval. Compared to slags D and E, slags A, B, and C are deoxidized to an even greater degree and have better fusion point properties.

Since all of these slags were in the authors' 2750° F. and higher temperature classification range for cupola slags low FeO slag contents would be realized ordinarily. The addition of silicon carbide briquettes in the cupola mixture has lowered the FeO slag content even further and the effect is especially apparent in the reduced depth of chill in these irons.[5,6]

TABLE A—Chemical Analyses and Fusion Point Results for Cupola Slags at Tapping Temperatures of 2775–2850° F.

Slag	% FeO	% CaO	% MgO	% SiO_2	% Al_2O_3	% MnO	% Na_2O + K_2O	V' Ratio
A	1.15	27.00	1.04	52.27	13.14	2.89	2.18	0.52
B	0.81	26.25	0.95	55.07	11.73	2.67	2.49	0.48
C	0.72	26.08	1.01	55.25	11.63	2.70	2.42	0.47
D	2.33	28.20	1.01	51.00	12.33	3.65	1.56	0.55
E	7.11	23.28	0.76	50.34	12.34	4.59	1.59	0.46

Fusion Point Results

Slag	Start to Melt Time	Start to Melt Temp. F.	Start over Lip Time	Start over Lip Temp. F.	Down Block Time	Down Block Temp. F.	Time Interval, Min.	Temperature Interval, °F.
A	4:02	2134	4:05	2143	4:18	2188	13	45
B	3:55	2106	4:00	2127	4:30	2233	30	106
C	4:00	2127	4:15	2178	4:45	2293	30	115
D	3:40	2044	3:45	2061	4:29	2230	44	169
E	3:30	2008	3:40	2044	3:56	2109	16	65

Slag A—glassy, spongy, black and friable. Silicon carbide briquettes added in cupola mixture.
Slag B—glassy, spongy, gray and friable. Silicon carbide briquettes added in cupola mixture.
Slag C—glassy, dense, gray and strong. Silicon carbide briquettes added in cupola mixture.
Slag D—glassy, spongy, green-gray, and friable. Untreated cupola mixture.
Slag E—glassy, spongy, green-black, and friable. Untreated cupola mixture.

The writers' experience has been that results with respect to V' ratios and slag properties are not too conclusive. The V' ratio as used by the iron founding industry is only an empirical factor which does not tell the complete story of slag behavior. A more definitive relationship in special slags such as those described in Table A would probably be obtained by calculating the mole fractions per 100 grams of slag of all the basic and acidic oxides and then setting up a more complete ratio of base to acid oxide ratio on the premise that CaO, MgO, and MnO are usually considered bases of equal value, whereas FeO is neutral.

However, this procedure is not devoid of complications for the amount of base required to neutralize the amount of acid on a molar basis cannot be ascertained with certainty. In view of the fact that the cupola is an acid melting medium it may be assumed that a 1 mole base: 1 mole SiO_2 neutralization ratio will exist. Also some assumption as to the behavior of the amphoteric Al_2O_3 in such slags would have to be made.

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3. E. A. Loria, F. S. Kleeman and A. P. Thompson, "The Silicon Carbide Addition in Gray Iron Automotive Coatings," *Metal Progress*, vol. 51, p. 587, April (1947).

4. E. A. Loria, H. D. Shephard and A. P. Thompson, "Reduction in Chilling Tendency Through Silicon Carbide Inoculation of Gray Cast Iron," American Foundrymen's Association Preprint 47-49.

E. V. SOMERS *(authors' reply)*: 1. The chief effect of hydrogen on cast iron can be briefly stated to be that of increasing porosity with the accompanying occurrence of gas holes and similar defects. References to hydrogen defects are numerous in past literature (Zapffe,[1] Moore and Smith,[2] Somers and Gunther[3]). The variation of atmospheric moisture content on cast iron properties is well discussed in an article by Eash and Smith.[4] A cross check of Eash and Smith's data shows the effect to be coupled with that of tapping temperature as discussed in both this report and a similar report by Somers and Gunther.[3]

It is our opinion, backed by some experimental evidence, that hydrogen pickup is strongly dependent on the melting temperature of the iron. Moisture content of the blast affects the properties of the iron mainly through the melting temperature of the iron, that is, by either increasing the tapping temperature or by decreasing the tapping temperature with the coke addition and other variables being held constant, the hydrogen content of the iron will vary.

The above would thus indicate what would appear to be "anomolous" behavior during melting—all variables being fixed except the moisture content of the blast; a high moisture content with lower tapping temperatures will oxidize the iron, while a low moisture content with higher tapping temperatures will produce an iron with a greater amount of dissolved hydrogen. Other sources of hydrogen pickup by molten iron, such as wet ladles, sand, etc., will not be discussed here.

2. The presence of sulfur may have a decided influence on the chilling characteristics of the iron—the sulfur range for all the tests in this report was 0.10 to 0.16 per cent, and no observable effect of sulfur on physical properties was determined for this small range. A correlation exists betwen sulfur content and tapping temperature. The higher sulfur contents (0.13 to 0.16 per cent) occurred with tapping temperatures above 2720° F., while the lower sulfur contents (0.10 to 0.12 per cent) occurred with tapping temperatures below 2720° F. The sulfur content of the metallic charge was not too variable, and the above results were not masked by such a variation; the sulfur content of the coke was not determined and may have been quite variable.

Attempts have been made to correlate the V' ratio with chill depth and tensile strength. The data on chill depth was

inconclusive because of the wide standard deviation accompanying the chill test. The data on tensile strength correlated very well with a ± 3 σ of less than 2000 psi. for a given V' ratio; however, the correlation was non-linear, and since we definitely lack an explanation for the correlation, we are withholding these results. We disagree that the V' ratios did not correlate with tapping temperature; although the correlation is not too good, we were able to plot the three-phase diagram shown in Fig. 6. The comment on the dependence of FeO, temperature, and CaO content of the slag is quite true—it was the discovery of this correlation that eventually led to the three-phase diagram. Further work on slag properties may eventually lead to slag control as discussed by Mr. Loria and Mr. Thompson. The field of cupola slags is very promising.

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3. E. V. Somers and D. W. Gunther, "Absorbed Gases in Cast Iron," *Foundry*, March (1947).

4. J. T. Eash and E. R. Smith, "Effects of Moisture and Preheat of the Cupola Blast on Gray Cast Iron," TRANSACTIONS, AFA, vol. 49, pp. 1022–1052 (1941).

CHAIRMAN MCELWEE: You show a relationship between tensile strength and tapping temperature. Are we to assume that the test bars were poured with a definite differential below the temperature shown or did you allow it to come below a definite pouring temperature?

MR. GUNTHER: The temperature was not allowed to go below a given pouring temperature. There would be a relationship between the tapping temperature and pouring temperature.

R. P. SCHAUSS[2]: I want to know how the temperatures were taken, the degree of temperatures, and also how the slag appearance was figured? In the range of 1 to 5 per cent FeO and 2 to 8 per cent FeO and 4 to 12 per cent FeO in the slag was the FeO content determined just by color?

MR. GUNTHER: In answer to the first question concerning taking the temperatures, our tapping temperatures are accurate within approximately 20° F. They were taken with an optical pyrometer which was constantly calibrated; that is, it was daily calibrated to maintain satisfactory operation of the instrument. We feel that experience would indicate we can get an accuracy of 20° F. with this pyrometer.

The second question concerning slag analysis: We had a total of 100 different slags analyzed completely. We used several hundred samples for getting the data on the surface color. That is the way we judged the samples. Of course, at first, we did not take a chemical analysis (Fig. 3); these samples were judged only on appearance. Undoubtedly, there were influences which would effect the appearance other than the iron oxide and that is what we attempted to show in the three-dimensional equilibrium diagram (Fig. 6).

MR. SCHAUSS: Were those slags which were checked by color in a fritted or pancake sample?

MR. GUNTHER: They were pancake samples taken with a slag spoon a minute before the tap was made during the cupola slagging period. We had intermittent slagging and intermittent cupola tapping. They were judged on the top surface by color.

B. P. MULCAHY[3]: From the curves as drawn, are the temperatures directly related to the quantity of coke you added?

MR. GUNTHER: The coke was not the only thing that contributed to variation of temperature. The quality of coke, the amount of coke and anything else that was incidental besides the metallic charge and the blast change would ultimately affect the temperature and was reflected in the final tapping temperature.

MR. MULCAHY: In other words, you did not increase the coke between charges?

MR. GUNTHER: We did. I would like to point this out. In many cases with higher amounts of coke we had lower temperatures and vice versa, depending upon the coke quality and other factors which were not evaluated.

MR. MULCAHY: Did you also adjust the air?

MR. GUNTHER: No, we did not. The air was kept constant.

MR. MULCAHY: Should not those curves have been plotted against oxidizing conditions instead of against temperature?

In my opinion and interpretation of the data shown, the statements under conclusions that "the tapping temperature" is the factor responsible for the chemical composition of the metal, of the slag, chill, etc., is erroneous. The paper states that certain variables were held constant while the coke only was varied to effect the temperature variations encountered. This means, of course, that with the air rate constant, conditions of combustion were shifted appreciably in the degree of oxidation obtaining. This being so, the change in temperature resulting becomes simply one more effect rather than the cause for the variations in the factors claimed. Actually it belongs in the same category as the metal composition, slag composition, chill depth, etc. Both the slag and condition of metal reported show a radical change in oxidizing conditions throughout the range of metal temperatures given. The content of iron oxide in the slag changes radically. In other words, the metal temperatures were coincident to the conditions existing rather than the fact that it (per se) is the governing influence; my point being that in burning coke with a variable amount of air the amount of heat released per pound of coke can vary from 4,000 Btu. to 14,000 Btu. (approximately), which in turn changes the ratio of the CO to CO_2 in the resultant gases and concurrently changes the degree of oxidation.

MR. SOMERS: You are apparently trying to make an explanation on the basis that there would be a smaller amount of oxygen appearing in this so-called oxidation. You are referring to a change in the CO to CO_2 ratio.

We have no doubt that this is another variable, and affords a possible second explanation. However, our conclusions are based on the fact that there are other variables besides the chemical components that enter into the reaction. Generally, we specify the reaction by the chemical components only. A chemical reaction in order to be *definitely* specified should have the pressure and temperature appended in order to *definitely* show the direction the reaction is to take. The remarkable thing about this data is that the temperature dependence theoretically indicates an explanation on the basis of oxidation and that this is corroborated by the data.

MR. MULCAHY: In your frequency curve it shows maximum temperature of 2720° F.

MR. SOMERS: That is right. In most cases, you will get a probability distribution similar to the one you have seen there.

MR. MULCAHY: That is correct and to be expected, but my point is that this range of temperatures can exist and yet the degree of oxidation will influence composition and characteristics to a greater extent.

MR. SOMERS: When you are referring to maximum temperatures, you will have to specify the limit of variation, is that correct? There is no process by which you make measurements exactly. You will always have variations. As we stated in answer to the first question, our pyrometer was accurate only within 20 degrees on either side of the temperature average.

MR. MULCAHY: I am not questioning the practical limits at all. I am not raising that as a question. In other words, you show the curve from 2600 to 2800. That is a range of about 200 degrees, so the question of accuracy does not have any bearing on my question.

MR. GUNTHER: You have reference, I believe, to the fact that we did have the probability curve, is that right? In other words, to the fact that we were attempting to run around 2700 to 2800° F. These were the operating temperature limits we were more or less striving for. We had temperatures running, for example, from 2660 to 2760° F., getting roughly a 100 degree variation between the lowest and highest tapping temperature for a given run. We expanded that variation by changing the amount of coke in order to get the broader probability curve; i.e., to get a greater temperature spread. We were not concerned as to how we obtained the temperature changes. Our concern was to see how the temperature change would affect these variation characteristics we were talking about.

MR. MULCAHY: That is exactly my point. The temperature that is coincident with a specific condition which reflects this chemical condition as a state of oxidation.

MR. GUNTHER: That is right, that is the point we are trying to make. The temperature reflects the state of oxidation.

[2] National Malleable & Steel Castings Co., Cicero, Ill.
[3] Consultant, Indianapolis

MEMBER: I would like to know if the charges going into the cupola had the same degree of oxidation such as if you charge with all pig iron and steel or pig iron and cast scrap. Would that have some effect on the color of your slag?

MR. GUNTHER: You are right on the effect of the oxide on your charges and we cannot guarantee that the charges going in had a uniform degree of oxide coating. The data was collected over a long period of time, and this gave us a good average condition.

MR. SOMERS: One other point, we did not use bought scrap. We used foundry returns, steel rails, and pig iron so that such things as grate bars, etc., were eliminated.

H. W. LOWNIE, JR.[4]: I wish to make the observation that I agree with Mr. Mulcahy that the authors have not sufficiently emphasized the basic factor which is involved in their tests—the degree of oxidation of the iron during melting. For example, in the first and sixth conclusions, the authors state that *tapping temperature* affects the chemical composition of the iron and that the depth of chill varies with *tapping temperature* at any given constant analysis. Throughout the paper, and in several of the other conclusions, the authors have related properties of the slag and metal to the tapping temperature. Solely for the immediate purpose of this discussion, we will make the assumption that the tapping temperature as measured by the authors was an indication of the melting temperature, or more properly of the superheat temperature. Observing that variations in tapping temperature were accompanied by changes in slag composition and changes in the properties of the iron, the authors have indicated that these factors are cause and effect—that one can be controlled by controlling the other. I feel that a more accurate viewpoint would be to regard tapping temperature (superheat temperature) also as a *coincident effect* of various conditions existing during melting.

I have seen the large number of original data which the authors have accumulated during this investigation and believe that the value of the paper would have been considerably enhanced by consideration of the effects as *results of the degree of oxidation* of the metal during melting. The authors obviously had this effect in mind while conducting the work but have not made it clear in their paper. The degree of oxidation of the metal during melting is hard to evaluate, and is not nearly so easy to plot as other variables (such as tapping temperature) which can be conveniently measured quantitatively. Nevertheless, the degree of oxidation is strongly suspected as being the important culprit and, therefore, the one to be considered. Consideration of tapping temperature as the independent variable or "cause" of certain effects has led the authors to make certain misleading statements which certainly will be questioned by foundrymen. For example, the authors state on p. 443 "Cast iron can be melted to conform to ASTM designation A48-41, Classes 30, 35, or 40, by altering the tapping temperature of the iron and without changing the metallic composition of the charge."

"Degree of oxidation" is one of those factors which are frequently lumped together in discussions of cupola operation and called by the indefinite term of "melting practice." Since the degree of oxidation of the metal during melting in the cupola is under consideration, it is important to remember that this oxidation tendency is influenced by a number of operating variables.

A list of these variables would include (1) the bed height, or more properly, the height of the zone of maximum temperature above the tuyeres; (2) the iron to coke ratio, because of its effect upon the bed height; (3) the rate of combustion of the coke as determined by its inherent combustibility and its size; (4) the ratio of air to coke; (5) the temperature of the air entering at the tuyeres; (6) the moisture content of the air entering at the tuyeres; (7) the improper use of secondary tuyeres; (8) the rate of lining burnout, because of its effect upon bed height and blast concentration; (9) tendency for the cupola to bridge, because of resulting distortion of blast distribution and unevenness of the bed height; (10) the size of the scrap charged and its tendency to "burn up" in the stack (light scrap) or to persist past the zone of maximum temperature (heavy scrap); (11) the uniformity of charge distribution across the cupola, because of its effect upon blast distribution; and (12) the amount and type of flux used, because of its effect upon lining burnout, the tendency for the cupola to bridge, or the tendency for tuyeres to freeze over. Other factors can undoubtedly be added to this list.

Because these factors do not all affect "tapping temperature" in the same manner as they affect oxidation of the metal, it is apparent that "tapping temperature" should not be regarded as an indication of degree of oxidation as has been done in this paper.

MR. GUNTHER: I think the purpose of the paper has been a little misunderstood here. We were trying to prove the point that Mr. Lownie said we proved; now he wants us to turn around and say that our conclusions are the starting points. Actually I think item three in the conclusions which says that: "The temperature dependence of the carbon and silicon contents of the iron and of the FeO content of the slag can be explained on the basis of a varying amount of oxidation of the iron during the melting process" expresses our conclusions accurately. We are attempting to express chemical variation on the basis of oxidation. We will concur with Mr. Lownie 100 per cent to the effect that the oxidation does the job. Both of us think that, however, we are trying to say the same thing in a different manner.

MR. SOMERS: I think Mr. Mulcahy's point is this: Let us take this first reaction: Fe (liq) + ½ O_2 (g) = FeO (liq) (variable temperature specified). The temperature at which the reaction takes place must be specified to determine the extent of the reaction. In our analysis we have assumed that the activities of the iron and the oxygen are constant so that we would have only two variables connected with it, the temperature and the activity of FeO. The silicon and carbon percentages can then be theoretically plotted vs. temperature, if the equilibrium constants are known. The silicon and carbon percentages plotted in Fig. 1 corroborate this assumption qualitatively.

In addition, if we finally check the data we do get an increase of FeO in the slag with decreasing tapping temperature. Mr. Mulcahy's point is apparently that the activity of oxygen, which we assume is constant, is also a variable. Our data shows remarkable co-variance with just two assumed variables. We certainly cannot rule Mr. Mulcahy's assumption out; but, at best, its effect would be to increase the inaccuracy limits given in Table I. In order to check the effect of pouring temperature on the chill depth, we made extensive studies of chill depth. This has been done during the past nine months and the work involved as high as 5000 samples. We found pouring temperature was unimportant in the ranges in which these tests were poured.

T. E. EAGAN[5]: I would like to call attention to one thing with reference to the tensile strengths that are given by the authors. They show that lower pouring temperatures give higher tensile strengths. We must remember that they have used only the 1.2-in. arbitration bar. With any given chemical analysis the tensile strength obtained is a function of the cooling rate of the casting. Therefore, we must be careful in interpreting the results shown. However, the authors have used a positive size of casting which can be used for comparison purposes and that is the arbitration bar. You will not always get the same results when you pour a different casting. The results shown by authors can be very misleading to the practical foundryman. He must still use pouring temperatures that are consistent with good foundry practice. His arbitration bar tests must show tensile strengths that he can interpret in relation to the casting he is pouring.

MR. GUNTHER: We agree with that. The purpose of the paper was only to analyze from the test bar standpoint and not from the casting standpoint.

MR. SOMERS *(author's closure)*: It is not quite clear as to whether the objections raised to the temperature dependence of oxidation of cast iron have been considered from a *basic* viewpoint. To this end a cursory review of the principle of temperature dependence of chemical reactions may be in order.

The oxidation of cast iron may be characterized *purely* by a chemical reaction; thusly,

$$(1)\ Fe + \tfrac{1}{2} O_2 = FeO$$
$$(2)\ C + \tfrac{1}{2} O_2 = CO$$
$$(3)\ Si + O_2 = SiO_2, \text{ etc.}$$

Associated with each reaction is an equilibrium constant, i.e.,

$$K = \frac{(a\ FeO)}{(a\ Fe)\ (a\ O_2)^{1/2}}$$

[4] Battelle Memorial Institute, Columbus, Ohio

[5] Cooper-Bessemer Corp., Grove City, Pa.

From the second law of thermodynamics the van't Hoff equation can be derived:

$$\frac{d(\ln K)}{dT} = \frac{\Delta H^0}{RT^2}\, dT$$

This equation expresses " . . . the effect of temperature on the equilibrium constant K of any reaction involving gases."* As K varies, the components expressed by the above chemical equations vary; or, there is exhibited a *temperature dependence of the chemical reactions in cast iron.* As stated by Mulcahy, " . . . the metal temperatures were coincident with conditions . . . " is correct with respect to the variables listed by Lownie, but to add that " . . . the conditions . . . rather than it (the metal temperature) . . . is the governing influence . . . " is somewhat at divergence with the thermodynamics.

Lownie lists twelve variables which may cause the tapping temperature to be a "coincident effect"—a covariant. Of the twelve, excepting number two, not one can be selected as varying "coincidently" with the tapping temperature for the data in this paper. For instance, a few possible "coincident" effects can be listed:

(1) The moisture content of the blast was high *at all times* when the temperature was low, and vice-versa.

(2) The temperature was low *at all times* when heavy scrap was used, and vice-versa.

(3) A bridge formed whenever the temperature was low.

These "coincident" effects and others listed did not exist for these data, and most of the variables listed by Lownie were probably "random" for any given tapping temperature.

Lownie states that our conclusion that " . . .'cast iron can be melted to conform to ASTM designation A48-41, classes 30, 35, or 40, by altering the tapping temperature of the iron and without changing the metallic composition of the iron charge' . . . is misleading." The authors used with good success the principle stated in the above conclusion in the melting of cast iron to the listed designations since 1944 at the Trafford Foundry of the Westinghouse Electric Corporation.

The authors wish again to reemphasize that the variation of the tapping temperature during the melting operation will have a strong effect on the composition of the cast iron. The tapping temperature is by far the most important of the daily melting variables affecting the properties of high strength cast iron, and it is usually the least considered.

* Noyes and Sherrill, *A Course of Study in Chemical Principles*, p. 471, Macmillan (1938).

MALLEABLE FOUNDRY FINISHING AND INSPECTION

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ABSTRACT

The author reviews steps and procedures for delivering a satisfactory product beginning with raw casting after pouring and following through the cleaning, finishing and inspection operations.

THE ROMANCE OF CREATING a practicable casting from a beginning of sand and molten metal has always been allied primarily with the foundry itself, while the finishing and inspection departments have been considered mechanical operations to be assigned to any layman after the artist has exercised his skill. On the contrary, this same creative spirit does exist in every operation of these departments for it is here that many discoveries are made which result in effective casting improvements.

Although many of the steps are interrelated, this paper on finishing and inspection methods as practiced in a malleable foundry will be presented in two parts, the first being the finishing methods.

A finishing department is sometimes considered an evil, necessary to the completion of a commercial casting, because it accounts for a considerable portion of the total cost of producing the casting. Too often this phase of the foundry operation is not scrutinized in the same way as the melting, core, and molding departments are from the cost standpoint. It is an established fact that were it not because of something which the foundry has caused to happen to the casting, either through gating, feeding, or other foundry technique, the finishing department would not be in existence. Because of this fact it is of the utmost importance that the finishing department together with the inspection department should at all times work in close cooperation and harmony with all of the departments instrumental in producing a casting which they will finish, and, in turn, sell to the customer.

Finishing Methods in a Malleable Foundry

In the majority of malleable plants, the first step in finishing castings after they leave the spruing room, is to remove them to the hard mill room where they are tumbled to remove sand and some of the lesser fins on the castings. In the particular plant to which this paper applies hard iron milling was eliminated some years ago.

Hard iron trimming, then, is the first step in finishing malleable castings in the author's foundry. In this department, the annealing pots are set on the floor in rows, allowing enough room between the rows for the trucks of hard iron castings.

The operator takes the castings from the trucks and removes all fins, cracking strips and excess material by means of a hand chipping hammer. He also gives the castings a visual inspection and throws all castings with obvious defects into the trucks stationed for this purpose. The good castings are placed directly in the annealing pots in such a way as to obviate distortion as much as possible while going through the annealing oven (Fig. 1).

Annealing pots are 27½ in. long, 19½ in. wide, 17 in. deep and have trunnions on two opposite corners of the pots. They weigh approximately 480 lb. Experiments are being conducted at the present time with new alloy pots which weigh 300 lb. The weight of the castings placed in the pots average between 350 and 400 lb. per pot.

There is very little packing material used in readying pots for the anneal, there being only a small percentage of the pots in which any is used. This packing is placed in the pots by means of a hopper which is picked up by the overhead crane and run across the tops of the pots in which packing is used.

Charging Annealing Oven

After the pots have been filled to their capacity with castings, they are stacked four high by an overhead crane, and a metal cover is placed on the top pot. The seams of each section of pots and also the cover are then sealed with mud to eliminate the danger of oxidation of the castings in the annealing oven. These stacks are then placed on an annealing car, which has a capacity of six stacks or a total of 24 pots. Each of these cars is then placed in a Dressler continuous type furnace.

To remove soft iron castings from annealing pots, after the annealing cycle has been completed, the pots are removed to the dump floor by an overhead crane. An important step during this operation is the sorting of castings into various containers. Through sorting, certain types of castings are segregated to facilitate cleaning. At this station, each pot is picked up with

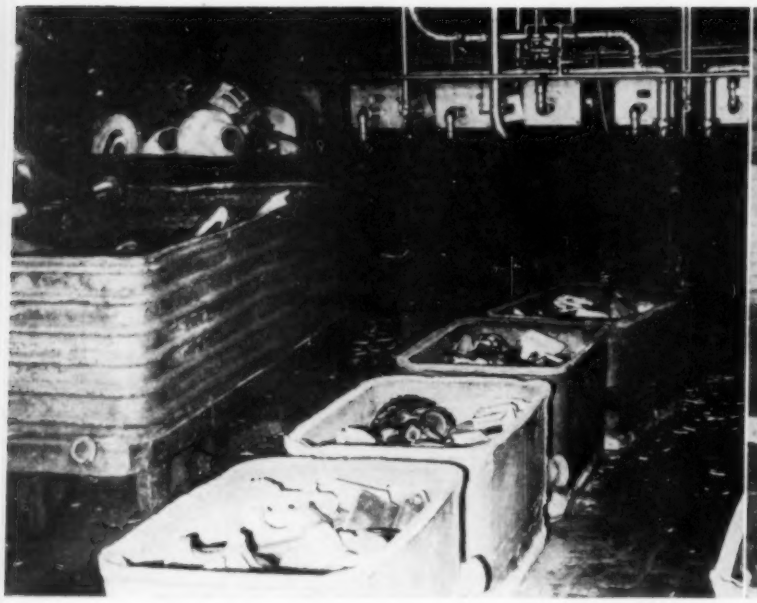


Fig. 1—Hard iron castings packed in annealing pots prior to annealing.

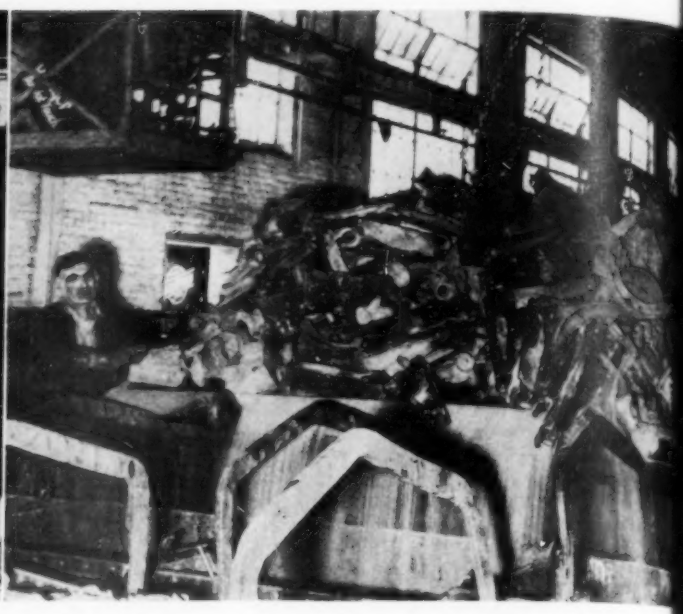


Fig. 2—Removal of castings from dump floor rack after annealing.

sling chains and upset by two men, the castings falling into receiving containers in which they are transferred to the cleaning station.

Pots in which packing material has been used are upset onto a large shakeout rack which allows the sand to fall through while retaining the castings (Fig. 2. When this shakeout rack is filled with castings, it is lifted by an overhead crane and removed to a rail in front of which is placed a suitable number of receiving containers. One end of this rack is set on the rail, and the other is up-ended, the castings sliding into the containers. The containers filled with castings are then moved by an overhead crane and emptied into the receiving hopper on the shotblast machines where the castings are cleaned.

The machines in which soft iron castings are cleaned are completely automatic, the operator only pushing buttons to load, close the machine, start and stop, open the door and unload the castings. The time for cleaning castings of all sand and scale ranges from six to eight minutes per load, varying according to the types of castings. This cleaning is done with No. 20 shot.

It is the practice to experiment continually with new types and sizes of shot to secure cleaner castings in shorter cleaning times.

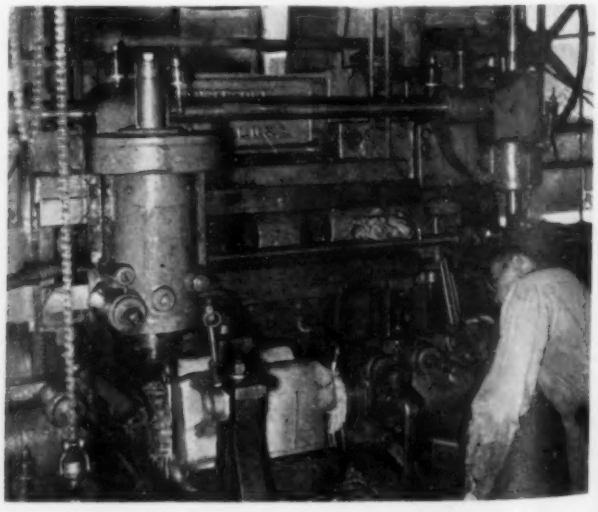
After the cleaning is completed the castings are dropped to a container which, in turn, transfers the castings to a conveyor belt which runs the entire length of the soft iron finishing department.

This department is made up of two divisions, a large and a small castings finishing department. The small castings finishing department is located on the second floor of a two-story building, and the conveyor belt runs on an incline to the second floor.

The sorting of castings begins in the large castings finishing department where the large castings are removed from the conveyor belt and sorted into the proper skid-type containers (Fig. 3). The small cast-

Fig. 3 (below)—Sorting of castings from belt conveyor.
Fig. 4 (right)—Milling two gates simultaneously from large malleable casting.





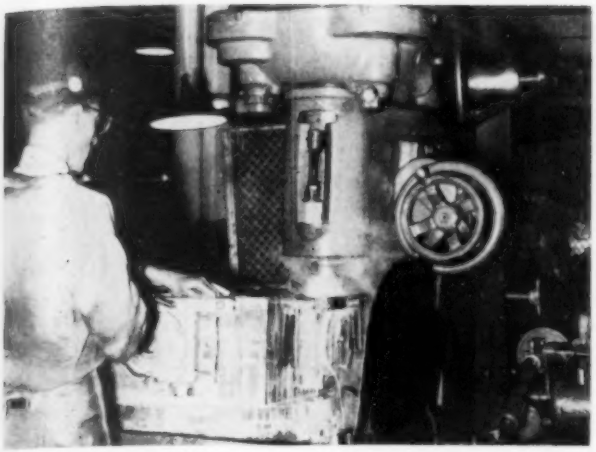




Fig. 5 (above) —Milling castings to close tolerances on rotary table. Fig. 6 (right) —Foot-pressure stand grinding of castings.

ings continue on up to the second floor where they are sorted to a truck-type container.

Finishing of Large Castings

In the large castings division the skid containers are transferred to the various operations by means of a power lift truck, and the department is laid out so that a minimum of this trucking is necessary.

As the first operation on large castings, all excess material which cannot be ground from the castings is removed by chiselling with an air hammer.

From this point the castings travel either to the stand grinders and swing frame grinders or to the milling machines. Here a decision is made as to which operation is the most economical to perform, grinding or milling. Certain gates or feeders which ordinarily might be ground on the swing frame grinder because of the gate size, pad size or excessive weight of castings are more economically removed by milling. For this purpose there is a battery of milling machines.

In the plant tool room, fixtures have been constructed for the various types of milling machines operated. On long-running jobs there are multiple fixtures on rotary tables of the machines so that the operator has only to place the castings in these fixtures to complete the operations. The plant with which the author is associated also has a milling machine which has one vertical and two horizontal cutters into which the castings are fed. Two or three feeders are removed at the same time from a number of parts (Fig. 4).

The savings realized because of this method of removing gates and feeders runs from 20 to 50 per cent under the cost of grinding. It has been found that where close tolerance inspection is necessary, the milling machine is a valuable adjunct to the finishing department (Fig. 5). An additional saving through the use of milling machines is the saving of the metal removed, which is returned to the foundry for remelting whereas in the case of grinding all metal removed is a complete loss.

Fig. 7—Straightening castings in hydraulic press.



Fig. 8—Punching five holes in a casting.

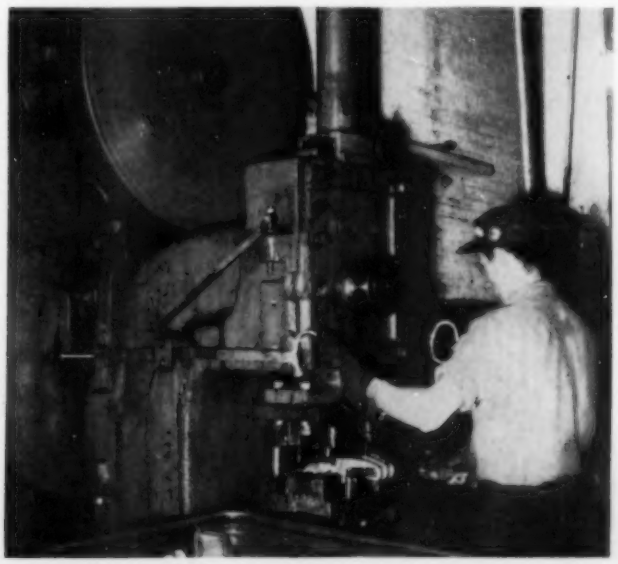




Fig. 9—Fracturing hard iron castings for inspection.

On the grinding stands, the author's foundry uses what may be termed foot-pressure grinding. The fixture used has been developed over a period of years, and through its use the gate of the casting is forced against the grinding wheel with high pressure, which is exerted by the operator's stepping on a foot lever (Fig. 6). This method has proved superior to all other types of grinding.

Several important advantages of foot-pressure grinding are: it decreases cost; it increases the output per machine; it allows the man to have both hands free to hold castings thereby eliminating the hazard of a casting slipping between the wheel and the stand; it is less fatiguing to the operator; it allows setups to be made to grind castings semi-automatically. From an economy standpoint savings of 20 to 40 per cent have been realized through the use of these fixtures as compared to previous hand grinding methods.

Grinding Equipment

Grinding wheels found to be the most satisfactory for use in conjunction with pressure fixtures are the 30 in. x 2 in. or the 30 in. x 2½ in. resinoid-bonded wheels running at a speed of 9500 surface ft. per min. Some grinding stands are designed so that the speed can be increased as the wheel decreases in diameter, thus keeping the surface ft. per min. the same.

In cases where a battery of grinding stands are run by a line shaft, it is good practice to use new grinding wheels on the larger castings until the diameter of the wheel has decreased through grinding to 24 in. At this diameter the wheel is removed and placed on another stand in the line, where, by means of appropriate pulleys the speed is increased to 9500 surface ft. per min.

Grinding wheels, as a rule, are the largest single item of material cost in the finishing department. Therefore, continuous testing of wheels of different makes and types is necessary, with the object of realizing greater efficiency at less cost together with a satisfied operator.

After considerable research, it has been concluded that it benefits all concerned with the operation of grinding wheels to test continually and retest makes, types and grades of grinding wheels. In this way, improvement in cleaning production may be realized.

After the preceding operations are completed, the castings are ready to be straightened. For this purpose there are drop hammers and oil presses (Fig. 7). In the author's plant there are four drop hammers and four oil presses. The oil presses are rated at maximum pressures of 150 tons, 500 tons, 750 tons and 1000 tons. The amount of pressure exerted on the castings is, of course, determined by the requirements of the customer and whether or not the blueprint calls for coining.

Straightening Dies

The dies for all of these machines are designed and fabricated in this particular plant's own tool room. They range from cast dies for drop hammers to large complicated dies which allow castings of intricate design, such as housings, flanged hubs, etc., to be easily inserted and removed. Nevertheless, when the dies are bottomed they straighten the castings to blueprint dimensions. Such convenience and accuracy are made possible through the use of hardened inserts and strategically located spring arrangements. This type of die is particularly adaptable to castings which are somewhat warped.

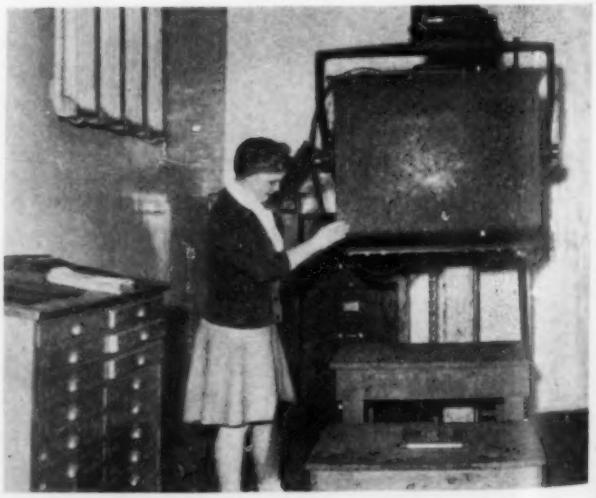
After the castings are straightened, they are ready for the finish inspection department. Castings which must be machined punched, milled, drilled, turned, etc. pass on to the finish inspection department only after these operations are completed.

Finishing Small Castings

The small castings finished in this particular plant go through substantially the same operations as the large castings but on a smaller scale. In the small castings division, the castings are transported to the various work stations in four-wheel trucks and are moved from floor to floor by means of elevators.

Operations performed on the small castings are grinding, dropping, pressing, reaming holes, milling to close tolerances, punching holes to close tolerances (Fig. 8), chiselling, burring and straightening on drop hammers, small oil presses and punch presses. After these operations the castings pass on to the finish inspection department.

Fig. 10—X-ray equipment for casting inspection.



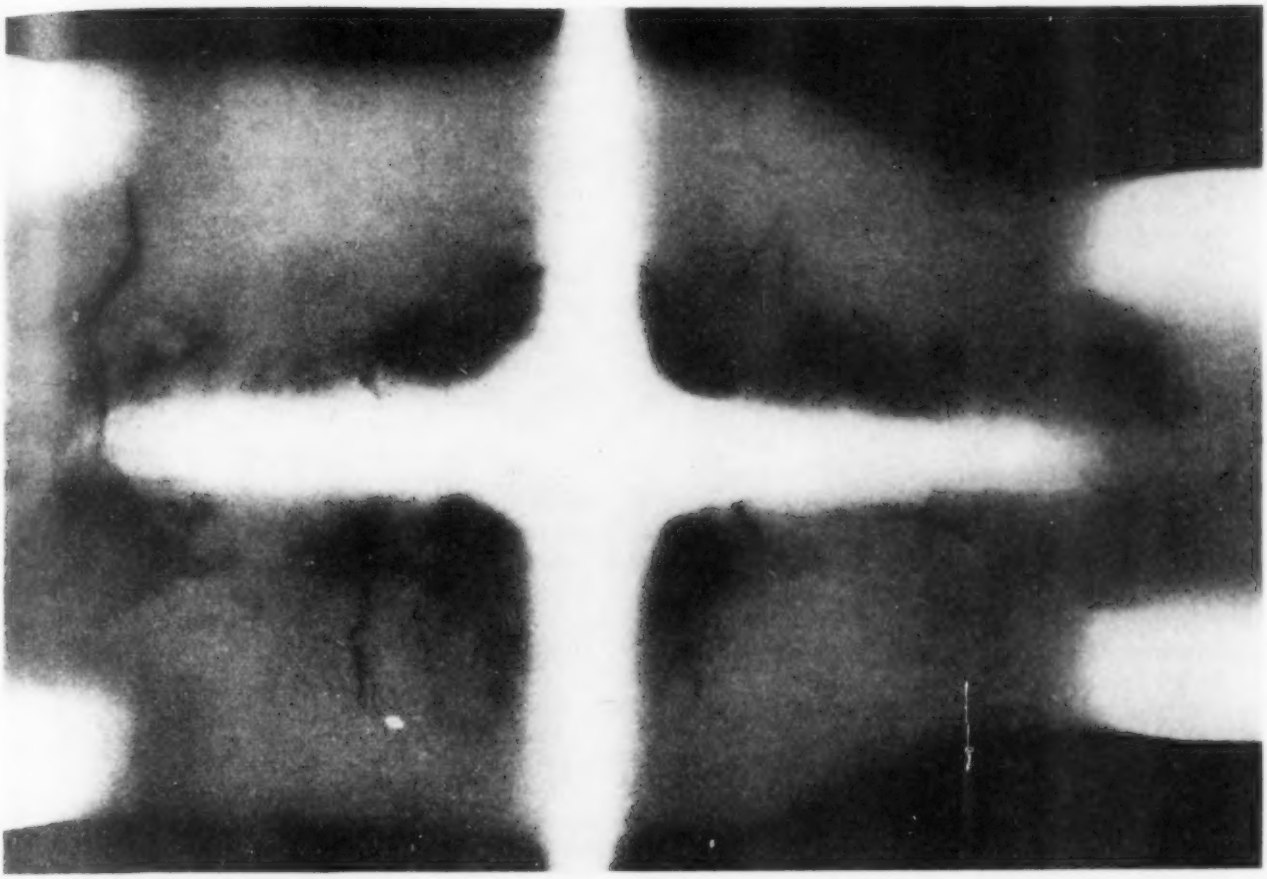
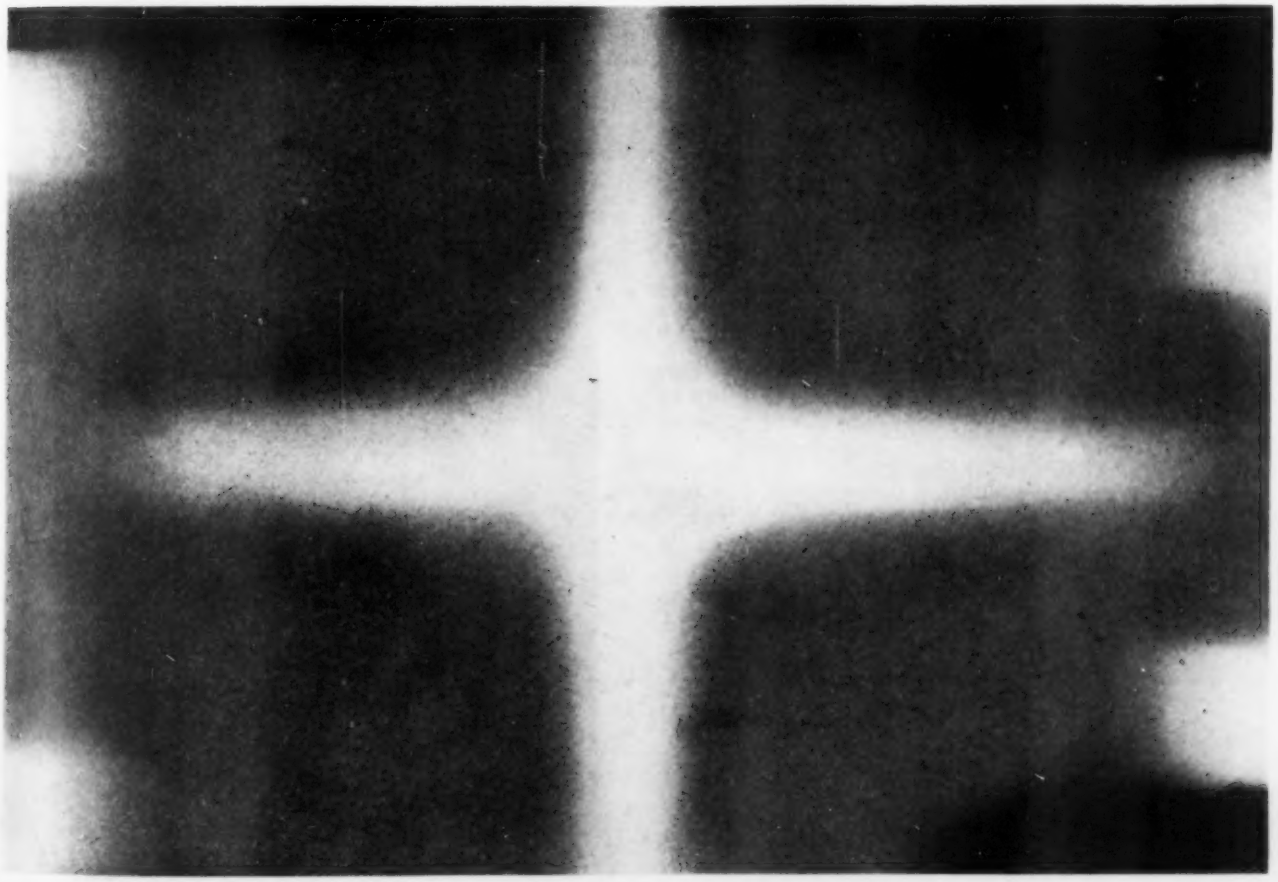


Fig. 11 (above) —Casting with shrink and hot tear. Fig. 12 (below) —Condition corrected after proper feeding by addition of pad.



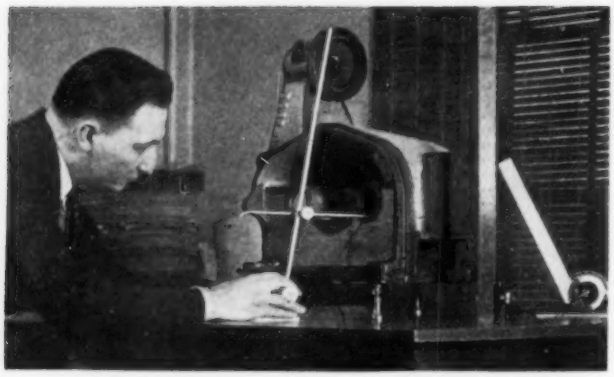


Fig. 13—Inspection layout for dimensional check.

Malleable Foundry Inspection Methods

Inspection control starts with the arrival of the blueprint and the request for quotations. A conference of the superintendent and the foremen of the pattern shop, foundry and coreroom is held and the job is studied from various points of view. If slight changes in design enable the foundry to produce a better casting, the customer is contacted and something usually is worked out to the benefit of both. These men also determine how the job is to be parted and where the feeders are to be placed to insure a sound casting.

Sometimes it is difficult to visualize from the print just how an intricate job should be molded and gated. In this case, wood models are made and samples are cast from them to develop proper methods of procedure. After the method of molding, gating and feeding has been determined, the foremen of finishing and of the tool room and chief inspector are called in. They consider the job from the finishing point of view. Considerations include elimination of the grinding of parting lines through good pattern equipment and proper molding, reduction of core fins to a minimum, whether the gates shall be removed by grinding or milling and whether the gates and risers could be put at some other place on the casting which would be more advantageous to remove and still produce a good casting. The amount of finish allowed and the tolerances on dimensions are gone over carefully and decisions are made as to their practicability. If it is thought that the tolerances are closer than can be held in the rough casting, straightening and coining operations must be included.

Customer Contacts

The chief inspector, through his contacts with customers, can usually tell how the job is to be used, its application, etc., and also which dimensions are important, which less important, and where the locating points for machining are to be. The locating points for machining are important in straightening the casting and laying it out exactly as the customer will machine it, and in the making of jigs and gauges for final inspection. Some customers recently have agreed to show the locating points on the blueprints. Most customers are cooperative and make suggested changes that enable the foundry to produce better castings.

After the above problems have been settled to the satisfaction of everyone involved, the pattern is made and delivered to the foundry for sample castings. The samples are made exactly as they are to be made in production, that is, if the job is to be done on a squeezer, it is squeezed and not rammed by hand by a day worker.

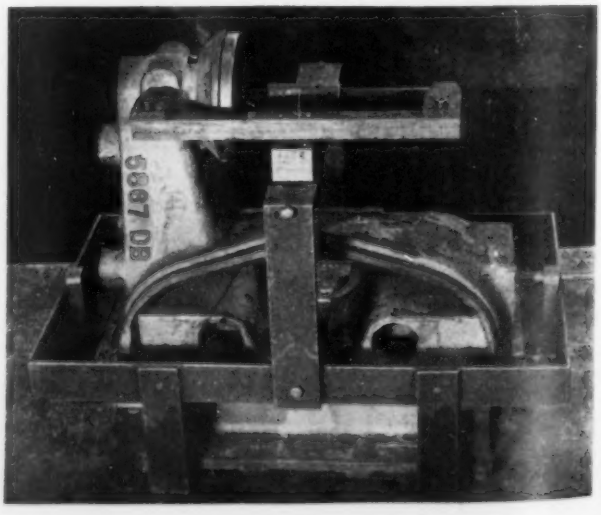
After the samples are made and poured they are cleaned and brought to an inspection bench in the foundry. Here they are carefully examined by a committee consisting of the superintendent, the foundry foreman, the coreroom foreman, the sand control man, and the chief inspector. The casting is examined for the proper breaking of feeders and gates, cleanliness of parting lines, core fins, surface finish and appearance, surface defects, checks and rips or hot tears, and the chief inspector again points out the important dimensions and the locating points. After this surface inspection, the casting is broken and examined internally for shrink and primary graphite (Fig. 9). When the committee is satisfied that there is no visible shrink in the casting, another sample is sent to the laboratory to be x-rayed.

X-ray Inspection of Castings

The x-ray has been found to be valuable as a tool of inspection (Fig. 10). Many times there is a slight lack of density in a hard iron casting, although the fracture appears white, but after the casting is annealed and broken this lack of density shows up as a shrink. This type of shrink is very difficult to see in the hard iron and might be over-looked, but the x-ray will show it plainly (Fig. 11, 12). The x-ray will also penetrate sections which might be difficult to break in the hard iron and which consequently might be overlooked.

At the same time a casting is sent to be x-rayed, another sample is sent to the layout room where it is set up and laid out for all dimensions and finishes (Fig. 13). When the production department has received approval of the casting from the x-ray and layout departments, the casting is sent to the customer for his approval, and upon receipt of this, the job may be started in the foundry.

Fig. 14—Dimensional check gauge.



In the foundry, the foremen and the men breaking off the gates and risers look for feeders breaking-in, gates and feeders that are hard to break off and which might crack the casting, castings which have been poured short in the feeders, runouts, and the dumping of molds too hot, which sets up strains that lead to cracks. On the large castings the connections are examined for signs of mottle.

Hard Iron Inspection

A hard iron inspector continually checks castings as they come off the shakeouts of the mechanized lines and he also checks the castings poured on the side floors. His full time is spent at this point and the spruing room adjacent to the foundry. An inspector at this point is extremely valuable because he shortens the time it takes to correct a defect and he eliminates large runs of castings with any defect.

In the writer's foundry melting is a duplexing process, the metal being melted in a cupola and held in an air furnace. Samples are taken at one-half hour intervals and analyzed for carbon, silicon and manganese, and throughout the day periodic checks are made of the sulphur, phosphorous and chromium content.

Annealing Room Inspection

From the foundry, the castings are sent to the annealing room where they are chipped and packed in pots. Here they are given their first thorough inspection. The man in charge of inspection in this department is equipped with snap and plug gauges for dimensional check in the hard iron. As a large percentage of the jobs are straightened in a hydraulic press, it is important that dimensions be checked in the hard iron, in order to insure a properly straightened casting.

The inspector also looks for excessively heavy cracking strips which might start small cracks in breaking from the casting. It is his duty to inspect the heavy castings from each heat for primary graphite. All heavy castings poured in the foundry are either dated or have a heat number cast on them. He also breaks castings to check for shrink. This is done periodically on long run jobs as a matter of routine.

The castings from each heat are annealed with test bars poured at intervals throughout the heat. The annealing is done in a continuous tunnel-type kiln which is controlled by 34 thermocouples throughout the oven. These couples are read every hour and recorded on a chart for that purpose, affording excellent control of the annealing cycle.

Metallurgical Inspection

As a further check on the annealing cycle, the laboratory has a man stationed on the dump floor, and he breaks and examines castings from every car for under anneal, decarburization and pearlitic frame. As some customers have very small finish allowances and machine at high speeds and feeds, it is essential that there be absolutely no frame on the casting. This is especially true in the case of operations performed by fly cutters and spot facing tools.

After the castings have been cleaned, finished and straightened, they are ready for final inspection. Every casting that goes out of the plant must go through the inspection department.



Fig. 15—Fixture for checking concentricity of castings.

The inspection department is equipped with mercury lights to facilitate the surface inspection which every casting gets. Inspectors look for smoothness of surface, sand and slag holes, checks, rips or hot tears, and cracks, cleanliness, etc.. Castings which seem to be commercially clean, but which might cause the customer difficulty in machining are rejected and sent back for recleaning.

Gauges are used to check the castings dimensionally (Fig. 14). Flanged castings with hubs on either side of the flange such as hubs, differential cases, etc. are tested for concentricity on a special fixture made for this purpose (Fig. 15). Many fixtures have also been made for checking the alignment and straightness of the castings. For many of the larger castings, elaborate and expensive fixtures have been made that locate from the customers locating points for machining, and check the amount of finish on each machined surface. Pearlitic malleable is Brinell hardness tested 100 percent and any castings not falling within the Brinell hardness range are rejected.

Chief Inspector's Duty

It is the duty of the chief inspector periodically to break soft castings and examine them thoroughly for internal defects. Periodically, he sends castings to the layout room and has them completely checked. He is constantly on the lookout for under-annealed castings and frames as a double check on the laboratory.

The chief inspector and also the hard iron inspector work very closely with the foundry. It is their duty to catch and immediately report any defects to the superintendent and the foundry foreman. The chief inspector's decision as to the suitability of the product is final and the men responsible for production very seldom dispute it. They know the chief inspector's knowledge of the customer's requirements is superior

to theirs and the aim of the foundry is to produce quality castings, entirely satisfactory to the customers.

Through frequent visits of the chief inspector and the sales force to customers' plants, it is possible to bring their difficulties back to the foundry for immediate attention.

All complaints go through the chief inspector who delegates the responsibility for the investigation to the departments involved. Complaints on dimensions and finish are handled by layout, pattern, and foundry. Complaints on machining or of a metallurgical nature are investigated thoroughly by the laboratory. Chemical analysis may be made, x-rays may be taken, and generally a section is cut from the casting, polished and examined microscopically.

After a complete investigation has been made, a complete report of the findings is made to the customer with any action taken to correct the trouble.

During the past year or two, the customers have changed the foundry industry's ideas regarding the nature of a commercial casting. The foundry industry today is being asked for castings with close tolerances which only a few years back would have been classed as an impossibility. Castings which were formerly given visual inspection or checked with rough gauges are today, because of constant pressure on the part of the customer, checked for dimensions in terms of thousandths of an inch.

The foundry is also asked to produce castings which may be used without machining as running parts of operating equipment. This condition calls for the best in all foundrymen continually to give to the customer his needs at a low cost.

It has been found that producing castings to these rigid requirements need not increase the cost of manufacturing the product. In most instances, production costs are as low as before, or lower.

At this particular plant there is a constant effort to improve the quality of the product, and there is not a man in the organization who does not realize that increased recognition will come to the company as the quality of the castings produced improves. And as the quality of castings produced by each foundry improves, the castings industry gains prestige.

DISCUSSION

Chairman: C. F. JOSEPH, Central Foundry Div., General Motors Corp., Saginaw, Mich.

Co-Chairman: W. D. MCMILLAN, International Harvester Co., Chicago.

P. KNOP:[1] You mentioned milling in gate removing to comply with close tolerance set by the customers. Do you set up those castings individually to mill them?

MR. POULSON: Yes. We place them on pins on the sides of the fixture.

MR. KNOP: Do you set up all the general run castings on this one fixture? Are the castings warped in such a manner that they have to be set up individually? You mill them before you straighten them, do you not?

MR. POULSON: No, we straighten them first.

N. J. HENKE:[2] Is your foot pressure grinding fixture a universal fixture or is it something you adapted to certain jobs?

MR. POULSON: We started using that years ago and made new versions of it right along. We just added or subtracted from it as we went along.

MR. HENKE: In other words, you use it 100 per cent with no leverage stick.

MR. POULSON: No, everything ground is by this pressure fixture. We use it entirely for our malleable, steel and gray iron castings.

D. TAMOR:[3] In the early part of your paper, where you talked about alloy steel pots, were you referring to alloyed carbon steel or were you referring to heat-resisting or stainless steels?

MR. POULSON: We make our own pots from a 15 per cent chrome, 35 per cent nickel alloy.

MR. TAMOR: What is its carbon content?

MR. POULSON: I could not tell you that.

R. A. LODER:[4] You think machining off of gates is cheaper than grinding or other methods of gate removal. Do you take into consideration the cost of the machine, the cost of building the equipment to mill the gate, the upkeep of the tool, the grinding, etc., when you make the statement that it is cheaper to mill than it is to grind?

MR. POULSON: I would like to qualify that statement. I do not know whether I said it or not; I do not think I did, that it is cheaper to mill than grind. I am no authority for that. On certain jobs that we have, on milling machines, where we do very much milling, probably 35 per cent of our production is milled, the gates and pads on the job are so placed that it is more economical for us to mill them. Our cost for milling is not greater than our cost for grinding. I am talking about our average costs. There are many jobs that we would not mill; in fact, we like our grinding pretty well.

MR. LODER: I did not mean that you mill every gate, but I mean that in the ones you set up for, do you take into consideration the cost of the machine, the cutters, the tool-up, the upkeep of the machine and the cost of machining?

MR. POULSON: That is correct. We have found that it runs very close to being equal in cost.

C. JELINEK:[5] Do you use X-ray inspection for control in spot checking or do you use X-ray inspection 100 per cent in your production?

MR. POULSON: We do that on every new job that comes into the plant; all samples are X-rayed. After that, periodic X-rays are taken of production runs.

MR. JELINEK: How does your inspection cost increase due to the cost of X-ray equipment, etc.?

MR. POULSON: After the original investment, the X-ray does not cost a great deal. The cost is minor. We would not be without the X-ray today.

J. H. LANSING:[6] Just to cover both sides of the picture, it was mentioned that largely low-speed wheels are used. I got the impression, because you said this was a resinoid wheel, that you largely use high-speed wheels. Is that correct?

MR. POULSON: I cannot get involved in this argument because years ago when I first started there, we had low-speed wheels. Before I had anything to do with it, they had changed over. The person who changed them over is a qualified gentlemen and he knew what he was doing when he changed over to high-speed wheels. We are completely satisfied with them.

In connection with the high-speed wheel, I doubt very much whether you could exert the pressure on resinoid bonded wheels unless you had a high-speed wheel. I think the survey showed that there is more output per machine, although the grinding wheel cost may be somewhat higher. However, in this day and age, when foundry workers are difficult to get, that is a very important item, to get the maximum output per machine. It sometimes overbalances a little extra cost, and I am firmly convinced that if everyone would continually check and re-check emery wheels, that eventually these emery wheel manufacturers are going to give us a far superior emery wheel to what we are getting today.

E. M. STRICK:[4] In regard to being able to remove metal much faster and needing that high speed in grinding, it is possible to apply enough pressure on a grinding stand. I have proven it to the point of stopping a 35-h.p. grinding machine with vitrified wheel to a standstill without breaking the wheel or having any difficulty with it.

MR. POULSON: I am not well enough acquainted with vitrified

[1] Albion Malleable Iron Co., Albion, Mich.
[2] Central Foundry Div., General Motors, Saginaw, Mich.
[3] American Chain & Cable Co., York, Pa.
[4] Erie Malleable Iron Co., Erie, Pa.
[5] National Malleable & Steel Castings Co., Cleveland.
[6] Malleable Founders' Society, Cleveland.

wheels to give an intelligent answer to it. I know with our foot pressure fixtures, it is very simple to stop practically any size motor with it.

MR. KNOP: Do you use any other method such as shearing besides milling and grinding for gate removing?

MR. POULSON: No; we have at present one job that we shear only because the break-off is large. We just shear it off with the ordinary sprue cutter to eliminate excess grinding and save that metal. We have tried shearing many times and hope to go to it some day. We have never been successful to the extent that we did not have to grind afterwards. Generally a casting has a little grinding on it besides just that gate. You have got to pick up and drop the casting anyway, and many times the pick-up and drop takes as much time on some of the gates as it does to grind the gates off.

V. A. CROSBY:[7] You mentioned Brinell hardness inspection of pearlitic malleable. I would like to know if you accept or reject pearlitic malleable castings on Brinell hardness specifications?

MR. POULSON: Yes, we do.

MR. CROSBY: Is this the only routine test of pearlitic malleable that you take into consideration in the final inspection?

MR. POULSON: No, that is in our final inspection. The balance is taken care of from the metallurgical standpoint.

MR. CROSBY: Do you make this pearlitic malleable to any standard specification?

MR. POULSON: Yes.

MR. CROSBY: Is there an agreement between the purchaser and the supplier regarding strength, hardness, etc., outside the specification?

MR. POULSON: We have and sell pearlitic malleable according to specifications which include physical properties and hardness tests.

MEMBER: We have changed from use of slow-speed wheels to high-speed wheels. It was done not only from the standpoint of cost but for safety. You can break a vitrified wheel, while it is nearly impossible to break a resinoid wheel.

MR. POULSON: I think you are right.

MEMBER: We found after we changed to 30-in. wheels, that we got a lower cost, but the safety factor was the more important factor.

MR. STRICK: We have used vitrified wheels on thousands of tons of castings and had only two vitrified wheels and six high-speed wheels break over a period of seven years.

MEMBER: That has not been our experience and, of course, it can be proved by test that you can break a vitrified wheel easier than you can a resinoid wheel. We have run resinoid wheels 1 in. in width, 30 in. in diameter at 9500 fpm. under pressure.

MR. POULSON: Vitrified wheel salesmen and engineers have always told us we could not do the type of grinding we are doing with a vitrified wheel, from a safety standpoint. They would not think of putting a vitrified wheel in our plant.

M. J. HENLEY:[8] Would you care to comment on the cost of your stainless steel pots compared to your standard pots or cast iron pots?

MR. POULSON: The life of the pots is tremendous. Ordinarily, you might get 22 to 25 heats running through our annealing process, on the regular steel pots. We have pots in now that have gone over 100 times through our annealing cycle. The new alloy pot, the light one, has gone through over 30 times already and we think there are many advantages in having them. They keep their shape. They allow for a cleaner condition, especially going through the continuous type kiln; you have no scale dropping off all the way through whereas with the old type pot you do get a lot of scale.

MR. STRICK: Have you had any experience running these pots through the anneal without mudding them, as you state that you do?

MR. POULSON: We would not do that at all.

MR. STRICK: Why?

MR. POULSON: We might burn the castings causing scale.

MR. STRICK: Is it not a fact that as soon as the temperature hits that mud cracks develop in the mud and air goes through those cracks?

MR. POULSON: If we have pots that scale off and have cracks or holes that go through the pot, we will end up with some burned castings. The mud seals them. I can tell you frankly that we would not put a pot in without mudding it, and we do not have as much as you think of the cracking of the mud causing us a great deal of difficulty.

CHAIRMAN JOSEPH: I might say that the cost of the alloy pots usually runs from 10 to 12 times more than the cast iron pots. Therefore, you would have to get from 15 to 20 times more life. The capital that you have tied up in your pots adds up to a great deal of money. The weight of our cast iron pot is around 600 lb. The alloy pot weighs only around 350 lb., but the cost of the alloy pot would be from 10 to 12 times the cost of the cast iron pot.

MEMBER: I do not think they cost 10 to 12 times more. You can purchase a good alloy pot for 70 cents a pound, but you can make it for less than half of that. We make all our own alloy pots and they do not cost anywhere near that price. With the present market on nickel and chrome, you can stay well below 40 cents per pound including labor in pouring 15-35 alloy pots.

CHAIRMAN JOSEPH: I do not know what an alloy pot would cost us, but I would say about 50 cents a pound. A white iron pot would probably cost around four cents a pound.

MEMBER: That is true, but the weight of the pot is the principal factor. You are getting four to five times as much weight.

CHAIRMAN JOSEPH: It has been quite a long time since we figured this out, but I know at one time the pot that we were talking about would actually cost from 10 to 12 times more than the cast iron pot. Probably today the price ratio might be different. But the capital tied up in the alloy pots would be quite high.

MEMBER: You get a scrap value of nine or ten cents per pound from your alloy pot, but practically nothing from your other pot.

CHAIRMAN JOSEPH: That is right. You can also bring your charge up to heat possibly a little sooner, because you do not have that extra weight of pot to heat up. It does not amount to much but it is something. Where the white iron pot would weigh around 600 lb., the alloy pot would weigh about 350 lb. That would be 250 lb. less weight per pot. I just mention that because I know there are certain angles that you have to consider when you think about changing from a cast iron to the alloy pot.

MEMBER: We are getting 1200 lb. of iron through a 200-lb. pot. There is the difference.

MR. POULSON: What is the size of your pot?

MEMBER: The size of our pot is 36 x 28 in.

CHAIRMAN JOSEPH: How much more did your cast iron pot weigh to start with?

MEMBER: Our cast iron pots weighed 300 to 400 lb. You would get 250 or 300 lb. to the pot. Now, we have reversed them, we get as high as 1200 lb. per net tray with a 200-lb. tray. We use the open tray type with no cover in the continuous oven. We have an atmospheric control oven.

CHAIRMAN JOSEPH: How many heats do you get from the alloy pots?

MR. POULSON: We have records of over a year of continuous service.

CHAIRMAN JOSEPH: That is not many trips.

MEMBER: Yes it is. We were on a 15-hr. cycle at that time. That is a lot when you take a pot going through every 15 hr. in a year.

MR. POULSON: I suppose that depends on the particular process you are using in comparison with ours. Probably for each person it might be a little different.

CHAIRMAN JOSEPH: He is talking about a larger pot than yours, I believe.

MR. POULSON: Ours is 27½ x 19½ in.

CHAIRMAN JOSEPH: We use the nickel-chrome trays and rollers in our atmosphere kilns but not in our large tunnel kilns. In our large tunnel kilns, we still use the white iron pots. Where you can use your pots over and over in a very short time, the nickel-chrome pots are probably cheaper. But where you have a large Dressler kiln and you get only about five trips a month, it is questionable whether the nickel-chrome pays for itself.

MEMBER: I would grant that due to the type of fuel you use for heat, but if you use electricity, you would not do that. We use electricity for fuel.

MR. POULSON: We use butane gas and have been running

[7] Climax Molybdenum Co., Detroit.
[8] Texas Foundries, Inc., Lufkin, Texas.

those pots about two years and we have received well over 100 runs through on them.

CHAIRMAN JOSEPH: From our calculations we would get around 35 trips with our white iron pot. You would have to get 350 trips to break even on the alloy pots, and we did not figure we could get that many trips through the Dressler kiln to break even.

MR. POULSON: We would not be without the alloy pot today.

MR. MCMILLAN: White iron pots have gone up in price to about $90.00 per ton. There are three pots to a ton as the pot weighs about 650 lb. These pots will hold about 335 lb. and are good for about 30 trips through the annealing kiln; thirty trips for about $30.00. The posts cost about $18.50 each. Due to the increase in cost of white iron pots there is a better chance of saving by making your own alloy pot using a 35-15 analysis. I believe your alloy pot weighs about 340 lb., and is lower in alloy content than 35-15.

MR. POULSON: Our new pot runs around 325 lb. Our old pots weighed 480 lb. Now, it runs around 300 to 325 lb.

MR. MCMILLAN: It will take you two years to find out how much you are saving over the old pots.

MR. POULSON: We have found out. We are going to keep the alloy pots.

MR. MCMILLAN: It is your own alloy. You are not paying 69 cents a pound.

MR. POULSON: No, that is an important item. Years ago you could not go into the business of making alloy pots because the cost of nickel was too great.

J. E. REHDER:[9] There is one question on the comparison of alloy pots and cast iron pots. You are talking about different types of kilns when you are trying to compare the atmosphere controlled with the Dressler type or periodic type of kiln. The very fact that you have atmosphere control in the electrically-fired furnace means that you are automatically protecting your pot. I do not see how you can compare the two different types of furnaces. You are protecting the pot in the controlled atmosphere furnace and probably adding greatly to its life.

MR. POULSON: That is right. It is difficult to make comparison unless they are identical in both plants.

CHAIRMAN JOSEPH: Of course, in your case we are talking about the same kind of pot, in the same kind of furnace. What I mean is the same shape pot in the same oven.

MR. POULSON: The comparison is identical.

A. M. FULTON:[10] In view of the fact that you do no cleaning in the hard iron, do you find a material increase in the amount of soft iron scrap? Further, how do you adjust your molder casting count for defective castings that are not thrown out due to their not being cleaned?

MR. POULSON: We do not charge the molders for any scrap in the soft iron, and our scrap, when we changed over to no cleaning in the hard iron, our soft scrap increased between one and two per cent. That is an approximate figure.

MR. STRICK: Are you making the same type of castings continuously?

MR. POULSON: It is a regular job shop. Year after year we make pretty much the same type of castings.

MR. STRICK: Would that not be the answer? If you are making the same type of castings continuously you are able to lick those jobs and keep your scrap losses low in the soft iron.

MR. POULSON: I wish that were true.

MR. STRICK: I did not ask you what your scrap loss was.

C. MADDICK:[11] You referred to an increase of 1½ to 2 per cent in increased scrap. Is that right?

MR. POULSON: Yes, by not cleaning in the hard iron.

MR. MADDICK: I would like clarification on that point. Assuming you had a hard iron scrap loss of three per cent, and a soft iron scrap loss of three per cent, totaling six per cent, by eliminating your hard iron cleaning, you eliminated 3 per cent loss. Would you expect an increase of only 1½ per cent over your three per cent?

MR. POULSON: We do not eliminate three per cent scrap in the hard iron. We still have our hard iron scrap. The only difference when we are not cleaning the hard iron is that some of the castings get by the inspectors because the defects are not visible because of not cleaning. In other words, that small portion of increase in the soft scrap should have been caught in the hard iron. That is the only difference. I will say our castings come from the foundry fairly clean, even without the cleaning operation on them.

MR. MADDICK: You made reference to an inspection in the hard iron phase. How does that inspector function?

MR. POULSON: He is there mainly to check. Our hard iron trimmers work piecework and fill these pots on a piecework basis. They throw out all obvious scrap and the inspector keeps close tab on these trimmers to see that they throw it out. He will go over these pots of castings after they have been filled and throw out scrap and call their attention to it. It is a constant check on the hard iron trimmers.

MR. STRICK: Does it not mean, then, that your foundry help has to spend more time knocking out cores and cleaning the castings much better than they would if you cleaned them all in the hard iron? There is some intangible cost there that you are not taking into consideration.

MR. POULSON: No, because we have not changed our process one bit.

MR. STRICK: How can you get all the sand and cores out of your casting then?

MR. POULSON: They come out exactly as they did previously. We have not changed anything. We do have some mechanized lines now, but we still have side floor molders. I do say we get fairly clean castings coming directly from the sand pile.

MR. WITENHAFER: Your plant is partly mechanized, is it not?

MR. POULSON: Yes, we have what is called a hopper unit and also a slinger unit.

MR. MCMILLAN: The castings are pretty clean from the shakeout, are they not?

MR. POULSON: Yes, on the mechanized lines.

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[9] Physical Metallurgy Research Laboratories, Bureau of Mines, Ottawa, Canada.

[10] Northern Malleable Iron Co., St. Paul, Minn.

[11] International Harvester Co., Hamilton, Ont.

FEEDING OF METAL CASTINGS

Derivation of a General Equation for Gating Castings and Experimental Evidence in Support of Same

By A. F. Faber, Jr.[1] and D. T. Doll[2]

ABSTRACT

The principal object of this paper is to present a simple formula or graph which can be used to calculate the optimum size of a riser and the gate between riser and casting. An equation is derived and experimental data is used to support the mathematical and heat transfer analysis. Other information and data are given for several non-ferrous alloys.

ALL CASTING PROCESSES have certain inherent features irrespective of the casting method employed. To obtain cast metals which have a prescribed external form and the desired internal soundness, it is necessary to consider the physical changes occurring with change of state. Most commercial metals, when cooling as a liquid and when passing from the liquid to solid state, undergo a contraction in volume. To prevent this effect from producing defective products, means are employed to counteract the shrinkage within the limits possible.

Practical solution to this shrinkage problem takes the form of one or more reservoirs of liquid metal, external to the casting proper, and linked to the casting proper by certain channels. Foundry terms for these features are risers and/or feeders and gates.

The fundamental character of the casting process is the extraction of heat from the liquid metal while at the same time compensating for any shrinkage in volume. Several factors immediately appear which influence the casting process. These might be stated as: the size and shape of the casting, the properties of the mold material, the properties of the metal being cast and considerations which affect the method of gating and feeding.

Each mold material and metal will have properties which are unique. By this is meant that a particular substance has more or less precise physical constants which need to be employed in the solution of the heat transfer problem. And in this sense it should be noted that one cannot escape a noticeable lack of reliable data.

From a theoretical consideration it is quite reasonable to assume that there is one best way by which a given casting can be produced, in so far as any particular casting method is concerned. By this is meant no defective pieces with a minmum of cleaning room scrap. Perhaps this ultimate goal cannot be reached practically but it is a constant endeavor on the part of foundry personnel.

It is recognized that there are a number of variables to be considered in any casting process, and it takes time, effort and experience to correlate these factors to their proper value and effect. However, it is felt that the technique to successful casting is largely concerned with proper gating and feeding. In many cases the placing of gating and positioning of feeders has relied upon the dictates of past experience. It is easy to understand the individual approach to the gating problem and why gating methods may show considerable variation. Each of any several methods may provide satisfactory castings but it also poses a question. Is there not one best way from the standpoint of economy of metal?

A number of contributions have been made toward the solution of the problem of gating and risering of castings. This paper is largely a mathematical approach based upon the premise that the production of sound castings is closely related to heat transfer. It is not offered in a too positive sense. However, the fundamental consideration (heat transfer), acting as a guide, has helped in the solution of a number of foundry problems over the past several years.

While the experimental work was carried out using plaster molds, the authors feel that the principles can be successfully applied to other mold materials.

General Equation for Gating Castings

The principal object of this paper is to present a simple formula or graph, which can be used to calculate, in a matter of a few minutes, the optimum size of shrink-bob or riser and the gate between the riser and casting. It is felt that the formula can be applied to get sound castings using any alloy in any molding material and will suffice for the geometry of a large percentage of commercial castings.

In practical application, benefits may be derived in the form of more castings per mold, lower metal usage, and for sound castings an increased safety factor against foundry operating variables.

[1]Staff Engineer, H. B. Salter Mfg. Co., Marysville, Ohio.
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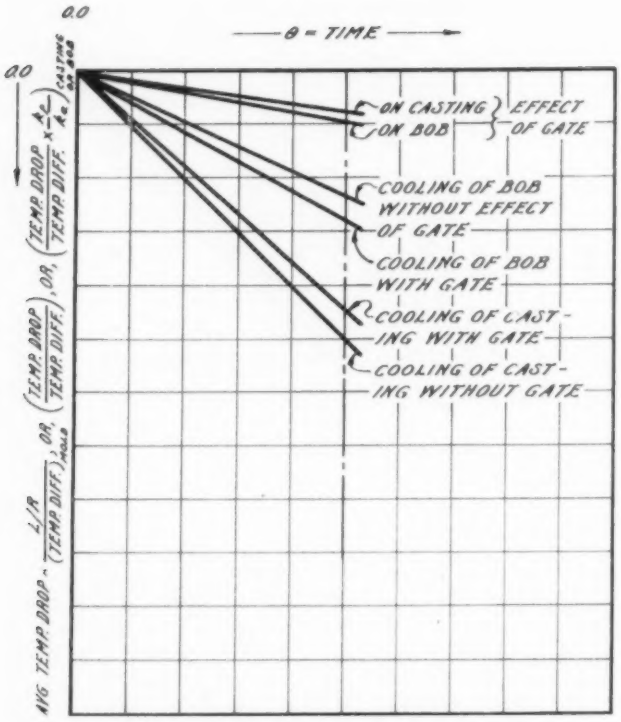


Fig. 1—Graph of Cooling Curves

At the instant a mold is filled with molten metal, the casting, bob,* and connecting gate, all are at the same temperature or very nearly so. There begins then directional solidification which may be likened to a race against time. Heat is flowing out of the bob and casting into the mold and, as the casting should begin to lose temperature faster than the bob, heat flows from the bob through the gate into the casting.

The cross-sectional area and length of the gate act as a valve on heat flow. This valve is sensitive for high thermal conductivity alloys and may destroy otherwise good proportions of bob to casting and force the casting to lose the race with the bob. Liquid pressure derived from height of sprue or riser or bob is to no avail unless the conditions of heat transfer are properly satisfied.

Mathematics of Heat Transfer

To simplify the mathematics of heat transfer the casting is considered as a heat unit, the bob as a heat unit, and the gate connection as a valve. The interrelation of these units is then derived. It will be shown that casting and bob are essentially at the same temperature at the instant the mold is filled.

The most important assumption made is that the initial rates of cooling determine the entire balance of cooling despite the interruption due to heat of fusion. The geometry of the casting and the gating pre-determine the rates of cooling. The mathematics say in effect that the final result will be satisfactory if cooling gets started correctly. Test data on sound castings will show that it is possible for both casting and bob to be at the same temperature while the heat of fusion is being dissipated in the mold.

The overall picture presented may be outlined as follows:

1. Derivation of the General Gating Equation.
2. Derivation of an equation for the temperature distribution in the mold which must be known to solve the general gating equation and which facilitates the use of test data.
3. Description of a simple test procedure.
4. Application of test data to support assumptions made in the derivation of equations and to indicate the manner of integration of temperature for use in equations.
5. Test data obtained on a number of different nonferrous alloys.
6. Curves for various alloys to be used in calculating gating for commercial castings.
7. Discussion.

1. Derivation of General Gating Equation: Definition of Terms.

A. $\alpha^2 = \frac{k}{\rho C_p}$ diffusivity

V = volume in cu ft
A = area in sq ft
R = distance in ft to midplane across which there is no heat flow
t = temperature in deg F
θ = time in hr
k = thermal conductivity in Btu per ft² per deg F per ft per hr
h_t = conductance, in Btu per ft² per deg F per hr
ρ = density in lbs per ft³
C_p = specific heat—Btu per lb per deg F
L = length or linear dimension in ft
Q = total heat in Btu

Subscript *B* refers to bob
Subscript *C* refers to casting
Subscript *G* refers to gate
Subscript *P* refers to plaster or mold material
Interface refers to a metal surface across which there is heat flow.
Subscript *a* refers to metal

B. The mathematical approach indicated by Gurney and Lurie[1] is used as a starting basis. For heat transmission in the unsteady state a body of metal of any shape is considered to cool by losing heat to some surrounding medium. The heat given up by the metal in cooling is equal to the heat absorbed by the mold. The mathematical expression of this heat transfer may be written

$$dQ = h_t A (dt)_{mould}\, d\theta = V \rho C_p\, (dt)_{metal} \quad \text{(Eq. 1)}$$

Now h_t, which is the heat conductance through the mold-metal interface, must be dependent upon the conductivity of the mold material adjacent to that face. Since the derivation is only concerned with initial cooling rates while the metal is liquid, it is obvious that there is no air gap to complicate matters. Then $h_t = \frac{k_2}{L}$

Terms of the equation will now be rearranged as

*The word "bob" will be used to mean feeder, header or riser and not necessarily restricted to the usual meaning of bob. The word "gate" will be used to designate the metal section between "bob" and casting and not restricted to means of metal entry to the casting cavity.

shown by Gurney and Lurie. The expression for integration of the temperature terms will only be indicated, but later solved by test data.

It may then be written—

$$\theta = \int \frac{(V\,{}^{\circ}C_p dt)_a}{(h_t A dt)_p} \quad \text{(Eq. 2)}$$

Multiply and divide by R^2 and K_a.
Substitute for h_t as defined above.
Substitute for dt_a, $(\text{Temp. drop})_a$.

Substitute for dt_p, $(\text{Temp. differential})_p$. Rearrange terms, then—

$$\theta = \frac{R^2 \times (\text{Temp. drop})_a}{\left(\frac{AR}{V}\right)_a \left(\frac{k}{{}^{\circ}C_p}\right)_a \left[\frac{k_p}{k_a} \times \frac{(\text{Temp. Differ})_p}{L/R}\right]} \quad \text{(Eq. 3)}$$

The term Temp. differential per L/R is an expression derived from temperature distribution in the mold.

C. Refer to Fig. 1 in which straight lines represent cooling of bob and casting. In one direction is plotted time and in the other direction is plotted a temperature function.

The slope of the cooling curve for either bob or casting as an independent cooling unit is changed by some small angle as the effect of the gate is added.

D. Consider an imaginary plane cutting through the gate transversely and let this plane be called an interface. As cooling progresses a temperature differential develops and causes heat flow across this interface. As before, heat given up may be equated to heat received.

$$\theta = \frac{(V\,{}^{\circ}C_p dt)\text{bob or casting}}{(h_t A dt)\text{ gate}} \quad \text{(Eq. 45}$$

and

$$\theta = \frac{(V\,{}^{\circ}C_p dt)\text{bob or casting}}{(h_t A dt)\text{mold}} \quad \text{(Eq.}$$

at equal θ, and $(V\,{}^{\circ}C_p)$ being constant, combine equations, then

$$\left(\frac{\text{Temp. change}}{\text{due to gate}}\right)_a = \left(\frac{\text{Temp. change}}{\text{due to mold}}\right)_a \times \frac{(h_t\ A_G\ dt)_{\text{gate}}}{(h_t\ A_B\ dt)_{\text{mold}}} \quad \text{(Eq. 6)}$$

by previous definition of conductance,

$$(h_t)_{\text{gate}} = \frac{k_a}{L_G} \quad \text{(Eq. 7)}$$

and,

$$(h_t)_{\text{mold}} = \frac{k_p}{L} \quad \text{(Eq. 8)}$$

$$\left(\frac{\text{Temp. change}}{\text{due to gate}}\right)_a = \left(\frac{\text{Temp. change}}{\text{due to mold}}\right)_a \times \frac{A_G \frac{R}{L_G} \frac{(dt)\text{ across gate}}{}}{A_B \left[\frac{k_p}{k_a} \frac{(dt)\text{ mold}}{L/R}\right]} \quad \text{(Eq. 9)}$$

The term in brackets [ ] is equivalent to (dt) metal, the temperature differential in the bob due to heat lost to the mold.

E. Refer again to Fig. 1. Apply the law of tangents for the angles of slope and make substitutions as derived in item D.

$$\frac{(\text{Temp. Drop})_a}{\frac{(\text{Temp. Diff.})_a}{\theta}} \times \frac{k_p}{k_a} = -\left[\left(\frac{AR}{V}\right)_a \alpha_a^2 \times \frac{k_p}{k_a} \times \frac{1}{R^2}\right]$$

$$\left[1 + \frac{A_G}{A_B} \frac{R}{L_G} \frac{(dt)_G}{(dt)_a}\right] \quad \text{(Eq. 10)}$$

Test data indicate that $\frac{(dt)\text{ gate}}{(dt)\text{ metal}}$ is approximately unity for values of k_a of about 30 and for the design ratios used in the test. Experience in a particular foundry would indicate desirable design ratios to be adapted as standards, thereby permitting more accurate determination of the (dt) ratio to be used. Strict mathematical derivation would not permit generalization of assumptions with regard to the value of the (dt) ratio but it can be seen shortly that rather wide variations will have a negligible practical effect.

Refer to test data of cooling curves at any time while temperatures are in liquidus range, Fig. 3-I. The term (dt) gate is the temperature differential between casting and bob: the value of this term is found to approximate the value of temperature differential $(dt)_a$ across the "R" of the bob.

Therefore, for high conductivity metals move the bob farther away from the casting than for other metals.

For same θ, equate functions for casting and bob and solve for R_B^2/R_c^2.

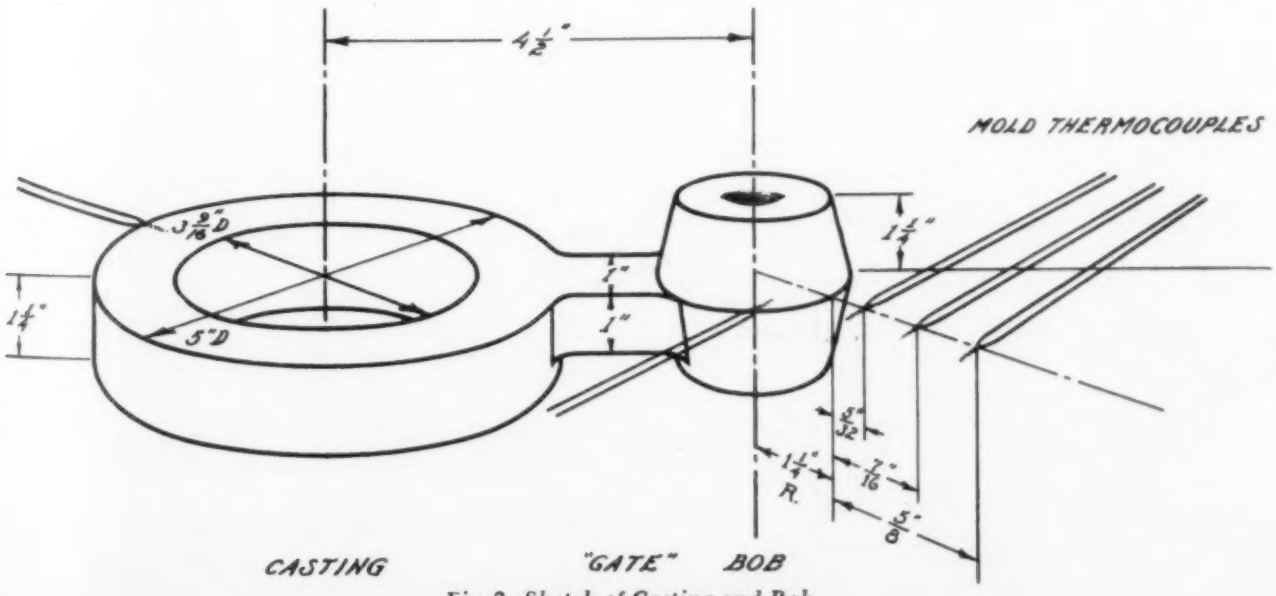


Fig. 2—Sketch of Casting and Bob

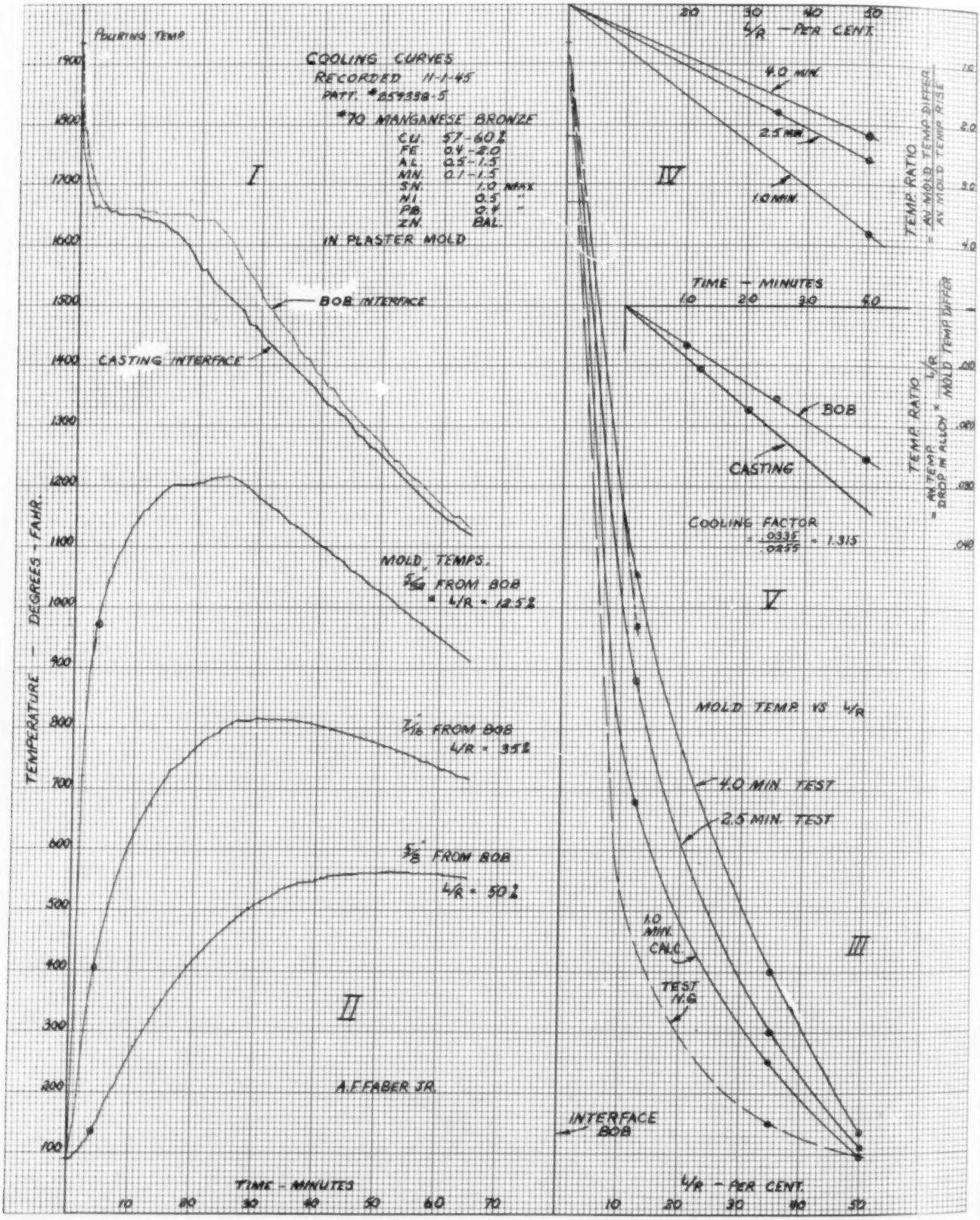


Fig. 3—Curves for Manganese Bronze

$$\frac{R_B^2}{R_C^2} = \frac{\left(\frac{\text{Temp. Drop}}{\text{Temp. Diff.}} \times \frac{k_p}{k_a}\right)\text{cast}\left[\frac{AR}{V} \propto^2 \frac{k_p}{k_a}\right]\text{bob}}{\left(\frac{\text{Temp. Drop}}{\text{Temp. Diff.}} \times \frac{k_p}{k_a}\right)\text{bob}\left[\frac{AR}{V} \propto^2 \frac{k_p}{k_a}\right]\text{cast}}$$

$$\frac{\left(1+\frac{A_G}{A_B} \times \frac{R}{L_G}\right)\text{bob}}{\left(1-\frac{A_G}{A_C} \times \frac{R}{L_G}\right)\text{cast}} \quad \text{(Eq. 11)}$$

Simplifying:

$$\frac{R_B^2}{R_C^2} = \frac{\left(\frac{\text{Temp. Drop}}{\text{Temp. Diff.}} \times \frac{k_p}{k_a}\right)\text{cast}\left[\frac{AR}{V}\right]\text{bob}}{\left(\frac{\text{Temp. Drop}}{\text{Temp. Diff.}} \times \frac{k_p}{k_a}\right)\text{bob}\left[\frac{AR}{V}\right]\text{cast}}$$

$$\frac{\left(1+\frac{A_G}{A_B} \times \frac{R}{L_G}\right)\text{bob}}{\left(1-\frac{A_G}{A_C} \times \frac{R}{L_G}\right)\text{cast}} \quad \text{(Eq. 12)}$$

For most castings the ratio of gate area to casting area approaches zero so that the term $\frac{A_G}{A_C} \times \frac{R}{L_G}$ may be omitted.

For analysis of test data, it is more convenient not to make substitution as in item "D" for the term of temperature distribution in the mold. The general gating equation may then be written:

$$\frac{R_B^2}{R_C^2} = \frac{\left[\left(\text{Temp. Drop} \times \frac{L/R}{(\text{Temp. Diff.})\text{ mold}}\right)\text{cast}\right]}{\left[\left(\text{Temp. Drop} \times \frac{L/R}{(\text{Temp. Diff.})\text{ mold}}\right)\text{bob}\right]}$$

$$\frac{\left(\frac{AR}{V}\right)_{\text{bob}}\left(1+\frac{A_G\,R}{A_B\,L_G}\right)_{\text{bob}}}{\left(\frac{AR}{V}\right)_{\text{cast}}\left(1-\frac{A_G\,R}{A_C\,L_G}\right)_{\text{cast}}} \quad \text{(Eq. 13)}$$

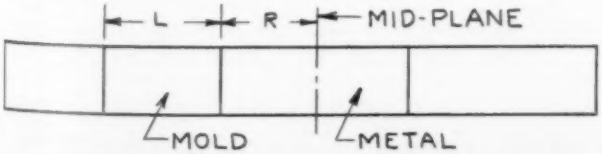
The term in brackets [ ] is readily resolved from test data for any particular alloy and mold material. Experience in a particular foundry may indicate a slight change to give more or less of a safety factor for varying conditions such as inconstancy of alloy composition or pouring temperatures. The value of this term is on the order of 1½.

The term (AR/V) is called the shape factor. A thin sheet has a value of (AR/V) = 1 while a round or square bar has a value of 2 and a sphere has a value of 3. Most commercial castings have shape factors in the range of 1.3 to 1.7 and with a little experience values can be selected without calculation. Shrink bobs and risers have shape factors between 2 and 3. When a runner is used for feeding it may be considered to have a value of 2.

For a casting of varying section, R_c is selected as the section thickness of the heaviest section to which the feeder is being applied.

2. Derivation of Penetration Depth (or, temperature distribution in the mold).

Assume an element of volume through the mold and metal.



Write the heat balance for mold heat mass and for metal heat mass, solving equation for Θ. Then for equal values of Θ,–

$$\Theta = \frac{(V\int C_p\,dt)\text{ alloy}}{(h_t\,A\,dt)\text{ mold}} = \frac{(V\int C_p\,dt)\text{ mold}}{(h_t\,A\,dt)\text{ alloy}} \quad \text{(Eq. 14)}$$

Substitute for h_t and solve for L/R which is the penetration depth as a percentage.

$$L/R = \frac{\left(\frac{AR}{V}\right)\text{mold}}{\left(\frac{AR}{V}\right)\text{metal}} \times \frac{\propto^2 \text{mold}}{\propto^2 \text{metal}} \times \frac{\text{Ave. Temp. Drop of Metal}}{\text{Ave. Temp. Diff. of Metal}} \times \frac{\text{Ave. Temp. Diff. of Mold}}{\text{Ave. Temp. Rise of Mold}} \quad \text{(Eq. 15)}$$

For an given Θ, it may be written

$$L/R = \text{Constant} \times \frac{\text{Ave. Temp. Diff. of Mold}}{\text{Ave. Temp. Rise of Mold}} \quad \text{(Eq. 16)}$$

Definition for terms used above:

1. Average temperature difference of mold equals average metal temperature at the interface (from pouring temperature to temperature at time Θ) minus the temperature at L/R being considered.

2. Average temperature rise of mold is the mean rise of temperature for depth of L/R being considered from original mold temperature to curve of temperature distribution.

3. The average temperature change of the metal is taken as the total temperature change of the metal from time of pouring to time Θ being considered.

4. The temperature differential in the metal from mid-plane to interface can be calculated knowing that the temperature distribution on each side of the interface is inversely proportional to conductivities of the mold and alloy.

Procedure to analyze test data:

A. Plot test data of temperatures in metal and mold versus time.

B. At time shortly before casting begins to freeze plot temperature distribution vs. L/R.

C. Calculate from straight line portion of curve "B" temperature distribution in the metal.

D. Calculate from curve "B" mean temp. rise for values of L/R at thermocouple positions.

E. Calculate average temperature differential for values of L/R as in "D."

F. Determine temperature ratios of values calculated in "D" and "E."

G. Plot L/R versus temperature ratio and draw straight line average to origin. Accurate test data will plot as a straight line to origin, thereby making a good check on test data. The form of the equation for penetration depth shows that this curve should plot as a straight line to origin.

H. Check slope of curve in "G" using reported or assumed values of diffusivity.

Note:–$\left(\frac{AR}{V}\right)$ term does not appear in calculations from test data because temperature ratios are measured at the (AR/V) being used.

Note:–For use in general gating equation it can readily be seen that ratio of penetration depths for casting and bob is proportion to the ratio of $\left(\frac{AR}{V}\right)$'s for casting and bob.

With the above procedure established, it can be re-

duced to a short-cut method as follows:

A. Select time Θ for temperature shortly before freezing. Draw graph of temperature distribution vs. L/R.

B. Calculate mold temperature ratio at largest L/R, for time selected. Plot straight line graph of temperature ratio to origin vs. L/R.

C. From curve in "A" select a convenient value of L/R and determine temp. ratio from curve "B." Then true temperature for curve "A" can be established for straight line portion.

D. From established values of conductivities calculate temperature differential in the metal.

E. Calculate metal temperature ratio at time selected in "A" and multiply by ratio of k_p/k_a as used in "D." Plot this ratio vs. time and draw straight line to origin. Data selected for other times will plot on this line as to be expected from derivation of general gating equation.

F. Reconcile cooling curve forecasting to a value of (AR/V) common to the bob cooling curve so that both curves can be plotted on same graph.

G. Select a convenient value of time in curves "F" and determine ratio of slopes. This ratio is the value of temperature factor used in the general gating equation.

Description of Test Procedure

3. A test casting having a simple geometry was chosen so that correlation of test data with calculations would not be questionable. The size of casting and bob is large enough so that sufficient and accurate readings of temperature and time could be taken. Castings were were made in plaster molds.

Thermocouples were made of 20-gage wire to lessen temperature lag of readings. Iron-constantin wires were used in combination with a potentiometer instrument. The junction of the thermocouple was made by twisting the wires tightly together for a short length and welding the tip. Wires were asbestos insulated. Thermocouples reading metal temperatures were so arranged as to read interface temperatures.

A mercury thermometer was used for mold temperature at station L/R = 50 per cent. Time was considered to start at start of pouring and the mold was filled in less than 15 sec. This technique was readily reproduced so that test data may be considered reliable. See sketch of casting and bob, Fig. 2.

Application of test data to support assumptions made in the derivations of equations and to indicate the manner of integration of temperature.

Material: No. 70 Manganese bronze.

Data recorded November 1, 1945.

The test data were obtained from measurements at bob-mold interface and at locations in the mold as described in the sketch, Fig. 2. This data is for a 2.5 min. test.

TABLE 1—TABLE OF PHYSICAL CONSTANTS[2]

	Coeff. of Expansion linear, in/in. $\times 10^6$	Density lbs/cu ft	Specific Heat Btu/lb/F	Thermal Conductivity Btu/sq ft/ft/hr/F	Heat of Fusion Btu/lb
Aluminum Alloys	1.234	157-174	.222 at R.T. .260 at 1200° F	58-92	75.5-79.6
Copper	0.887	550.4	.101 at R.T. .116 at 2200° F	224	39.3
Brasses	0.957 1.077	524.1 536.6	.094 at R.T.	56 at R.T.	50.
Mn Bronze	0.986	524.0 552.2	.100 at M.P.	70 at M.P.	Approx. 50
Sand		93.6 102.0	0.28	65-95	
Plaster		34	0.185 (by test)	0.1775 (by test)	

Constants	Plaster Mold Material	No. 70 Bronze at M.P.
density, ρ	34	500
thermal conductivity, k	.1775	70
specific heat, Cp	.185	.100

The average metal temperature differential can be calculated from the slope of the temperature distribution in the mold. (See graph of temperature distribution vs. L/R, Fig. 3, III).

$$\Delta t = 480 \times \frac{.1775}{70} \times \frac{1.0}{.05} = 24.4F$$

In the above equation the meaning of the numerical values is thus: 480 is the temperature differential over a 5 per cent range of L/R taken in the straight line portion of the 2.5 min. curve, Fig. 3, III. The values 0.1775 and 70 refer to the thermal conductivities of plaster and bronze. The ratio 1.0/0.05 is the ratio of R over L.

The temperature drop of the metal at the mold-metal interface is 1935 F (casting temperature) minus 1700 F (temp. at 2.5 min.) equals 235 F.

A. At a ratio of L/R = 50 per cent the mold temperature at 2.5 min. was 110 F by test.

The average mold temperature differential will be:

$$\frac{1935+1700}{2} - 110 = 1708F$$

The average temperature rise of the mold will be:

$$\text{1st 25 per cent of L/R: } \frac{1935+880}{2} = 1408$$

$$\text{2nd 25 per cent of L/R: } \frac{880+510}{2} = 695$$

$$\text{3rd 25 per cent of L/R: } \frac{510+230}{2} = 370$$

$$\text{4th 25 per cent of L/R: } \frac{230+110}{2} = 170$$

$$(1408+695+370+170) \div 4 = 661F$$

The mold temperature ratio is equal to:

$$\frac{\text{Average mold temperature differential}}{\text{Average temperature rise of mold}} = \frac{1708}{661} = 2.58$$

By derivation L/R = constant x mold temp. ratio.

therefore $0.50 = \text{constant} \times 2.58$, and constant = 0.194

B. At a ratio of L/R = 33 per cent the mold temperature at 2.5 min. was 300 F by test.

The average temperature rise of the mold will be:

$$\text{1st 33.3 per cent of L/R: } \frac{1935+900}{2} = 1418$$

$$\text{2nd 33.3 per cent of L/R: } \frac{900+520}{2} = 710$$

3rd 33.3 per cent of L/R: $\frac{520+300}{2}=410$

$(1418+710+410)\div 3=846$F

The mold temperature ratio will be: $\frac{1518}{846}=1.79$

and, L/R = constant × mold temp. ratio

therefore, $0.35=\text{constant}\times 1.79$

constant = 0.1955

C. In the above items A and B the calculated value of the constant from test data was determined to be 0.194 and 0.1955. If, then, a graph is made relating the mold temperature ratio to L/R in per cent, a straight line will be obtained as indicated by the constant. This graph is shown in Fig. 3, IV.

The value of the constant can be calculated from the equations for penetration depth, Eqs. 15 and 16.

$$\text{Constant}=\frac{\alpha_p^2}{\alpha_a^2}\times \text{metal temperature ratio}$$

$$=\frac{0.0282}{1.4}\times\frac{235\ (\text{av. temperature drop})}{24.4\ (\text{av. metal temp. differential})_B}$$

$$=0.194$$

It should be noted that the $\left(\frac{AR}{V}\right)$ term is included in the value of the metal temperature ratio.

D. With the value of the constant obtained by test, any point in the temperature distribution curve, Fig. 3-III, can be calculated. For instance, assume a temperature of 1235 F in the mold and calculate L/R.

Average mold temp. differential: $\frac{1935+1700}{2}-1235=583$ F

Average mold temp. rise: $\frac{1935+1235}{2}=1585$ F

Mold temp. ratio: $\frac{583}{1585}=0.368$

$$L/R=0.194\times 0.368=7.14 \text{ per cent.}$$

The above procedure may be used for a test time of 4 min.

A. At a ratio of L/R = 50 per cent, the mold temperature at 4 min. was 135 F by test.

The average mold temperature differential will be:

$$\frac{1935+1670}{2}-135=1667 \text{ F}$$

The average temperature rise of the mold will be:

1st 25 per cent. of L/R: $\frac{1935+1050}{2}=1443$

2nd 25 per cent. of L/R: $\frac{1050+630}{2}=840$

3rd 25 per cent. of L/R: $\frac{630+350}{2}=490$

4th 25 per cent. of L/R: $\frac{350+135}{2}=243$

$(1443+840+490+243)\div 4=754$ F

The mold temperature ratio $=\frac{1667}{754}=2.21$

B. This mold temperature ratio is plotted vs. L/R and a straight line drawn to zero, Fig. 3-IV.

Selecting an L/R equal to 11 per cent the temperature ratio is found to be 0.47.

$$\text{Then } 0.47=\frac{\frac{1935+1670}{2}-x}{\frac{1670+x}{2}}$$

and $x = 1140$ F temperature at L/R equals 11 per cent. This value is plotted on the temperature distribution curve, Fig. 3-III.

A. At a time of one minute and an L/R ratio equal to 0.50 the mold temperature is 95 F.

Average mold temperature differential will be:

$$\frac{1935+1780}{2}-95=1763 \text{ F}$$

The average temperature rise of the mold will be:

1st 25 per cent. of L/R: $\frac{1935+450}{2}=1192$

2nd 25 per cent. of L/R: $\frac{450+230}{2}=390$

3rd 25 per cent. of L/R: $\frac{230+115}{2}=173$

4th 25 per cent. of L/R: $\frac{115+95}{2}=105$

$(1192+390+173+105)\div 4=465$ F

The mold temperature ratio $=\frac{1763}{465}=3.8$

B. The temperature ratio, 3.8, is plotted against L/R equal to 0.50, Fig. 3-IV, as a straight line to the origin. At an L/R value of 7 per cent the temperature ratio is found to be 0.55.

Solving for mold temperature at this point:

$$0.55=\frac{\frac{1935+1780}{2}-x}{\frac{1780+x}{2}}$$

then x equals 1070 F at L/R equal to 7 per cent.

In the same manner the mold temperature can be determined for an L/R value of 12.5 per cent. This temperature is found to be 680 F.

Temperature Distribution

On the basis that the thermocouples evidence a temperature lag the curve for temperature distribution in the mold is plotted using the calculated values, Fig. 3-III.

A. Calculation of the cooling curve of manganese bronze as related to the bob.

A graph, Fig. 3-V, is determined in which either of the following relationships are plotted.

$$\text{Metal temp. ratio}\times\frac{k_p}{k_a}\text{ vs. time }(\Theta)$$

$$\text{or, Av. metal temp. drop}\times\frac{L/R}{\text{Mold temp. diff.}}\text{ vs. time }(\Theta)$$

B. From the temperature distribution curves, Fig. 3-III, determine the metal temperature differentials at several selected times.

$$\Theta=1.0 \text{ min. } \frac{k_p}{k_a}=\frac{32.2}{600}\times\frac{0.05}{1.0}$$

$$\Theta=2.5 \text{ min. } \frac{k_p}{k_a}=\frac{24.4}{480}\times\frac{0.05}{1.0}$$

$$\Theta=4.0 \text{ min. } \frac{k_p}{k_a}=\frac{18.75}{370}\times\frac{0.05}{1.0}$$

C. Time	Bob Temp.	Temp. Change	Metal Temp. Ratio	Temp. Ratio $\times \frac{kp}{ka}$
Pour	1935 F			
1	1780	155	$\frac{77.5}{32.2}$	$\frac{0.05}{1.0}\times\frac{32.2}{600}\times\frac{77.5}{32.2}=0.00645$
1-1/2	1740	195		
2	1720	215		
2-1/2	1700	235	$\frac{147}{24.4}$	$\frac{0.05}{1.0}\times\frac{24.4}{480}\times\frac{147}{24.4}=0.015$
3	1690	245		
3-1/2	1675	260		
4	1670	265	$\frac{187}{18.75}$	$\frac{0.05}{1.0}\times\frac{18.75}{370}\times\frac{187}{18.75}=0.0253$
4-1/2	1660	275		

For 1st min. av. change $\frac{155}{2}=77.5$ F

For 2nd min. 1st 1/2 $155/2 = 77.5$

2nd 1/2 $\frac{155+215}{2}=185.0$

$2\overline{)262.5}=131.2$ F

For 2-1/2 min. take av. of 2 min. and 3 min. = 147 F

For 3rd min.

1st 1/3 = 77

2nd = 185

3rd $\frac{215+245}{2}=230$

$3\overline{)492}=164$

For 4th min. in same manner = 187

The graph of the values 0.00645, 0.015 and 0.0253 against the respective times gives a straight line to the origin, Fig. 3-V.

A calculation of the cooling time for the bob as a check on the derivation of the general gating equation can be made.

From the fundamental equation,

$$d\Theta=\frac{V(\Sigma C_p dt}{h_t A\, dt}$$

the general gating equation is derived as:

$$\Theta=\frac{R^2\times(\text{Av. temp. drop of bob})}{\left(\frac{AR}{V}\right)\left(\frac{k}{\Sigma C_p}\right)_a\left[\frac{kp}{k_a}\left(\frac{\text{Temp. diff.}}{L/R}\right)_{mold}\right]\left[1+\frac{A_G}{A_B}\times\frac{R}{L_G}\right]} \quad \text{(Eq. 17)}$$

Values are converted to the proper units and substituted for R, L and A. The density must be corrected for thermal expansion.

Linear coeff. of expansion ..... 10.77×10^{-6}

Vol. coeff. (linear $\times 3$) ..... 3.23×10^{-5}

For temp. rise of 1600 F, coef. is ..... .0517

Corrected density $\frac{524}{1.0517}$ ..... 500 lb/cu ft.

Then for a temperature drop from 1935 F to 1700 F there is obtained:

$$\Theta=\frac{\left(\frac{1.25}{12}\right)^2\times60\times0.015}{\frac{70}{(500)\ (0.10)}\times\frac{.1775}{70}\ (1.10)}$$

$\Theta = 2.5$ min. by calculation as compared with 2.5 min. by test.

It should be noted that the term (AR/V) is included in the temperature ratio.

The calculation of the cooling curve for the casting is made in the same manner as the bob.

A. Select the same points of temperature as were selected for the computation of the bob. Then k_p/k_a of the bob can be used by correcting for (AR/V).

B. Calculation of average temperature drop.

Data Time	Cast Temp.	Temp. Change	Casting Temp. Ratio	Temp. Ratio $\times\left(\frac{k_p}{k_a}\right)_{bob}\frac{\left(\frac{AR}{V}\right)_{cast}}{\left(\frac{AR}{V}\right)_{bob}}$
Pour	1935 F	...		
1/2	1760	175		
1	1720	215		
1 1/4	1700	235	$\frac{156}{dt}$	$\frac{156}{dt}\frac{dt}{480}\frac{.05}{1.0}\frac{2}{3}=0.0108$
1 1/2	1690	245		
2	1675	260		
2	1670	265	$\frac{191}{dt}$	$\frac{191}{dt}\frac{dt}{370}\frac{.05}{1.0}\frac{2}{3}=0.0171$
1/2	1660	275		

For 1st 1/2 min. av. change = 175/2 = 87.5

For 1st min.

1st 1/2 87

2nd 1/2 $\frac{175+215}{2}=\frac{195}{2\overline{)282}}=141$

For 1 1/4 min. take av. of 1 min. and 1 1/2 min. = 156

For 1 1/2 min.

1st 1/2 = 87

2nd 1/2 = 195

3rd 1/2 $\frac{215+245}{2}=\frac{230}{3\overline{)512}}=171$

For 2 min.

1st 1/2 87

2nd 1/2 195

3rd 1/2 230

4th 1/2 $\frac{245+260}{2}=\frac{253}{4\overline{)765}}=191$

For 2.5 min —in same manner = 206

Plot 0.0108 and 0.0171 vs. Θ, which is a straight line to origin, Fig. 3-V.

The calculation of the cooling factor for use in the general gating equation for No. 70 manganese bronze in plaster mold can now be made.

A. Refer to cooling curves as plotted from data on bob and casting, Fig. 3-V.

at $\Theta = 4.0$ min.

$$\text{Ratio of cooling rates}=\frac{0.0335}{0.0255}=1.315$$

B. The test casting was broken at several sections and fracture indicated that the casting was entirely sound. Experience would indicate that a slightly lower value of the cooling factor would still produce sound castings.

Discussion

The scope of this paper has been intentionally limited so that attention will be focused on the heat transfer and mathematical analysis. The ultimate purpose of the investigation is to provide information which will enable a more precise selection of dimensions for gates and feeders. This paper can be considered as a first step in that direction.

No rigid answer has been given relative to the effect of the time required to fill a mold, or the possibility of an initial temperature differential. The experimental technique used may help to provide the means whereby definite answers to these questions could be obtained. Extrapolation of data, presented herein, indicates that these factors are of a low magnitude of error and would not adversely affect practical application even for rather heavy castings.

The case in its simplest form states, that any riser and gate shall be of such size and shape so as to provide a continuous flow of heat from riser to casting until freezing is completed. If this condition can be initiated to a sufficient degree then a sound casting should result.

If any volume of liquid metal be considered it is apparent that, in order to cool, the heat present in the metal must be conducted away. The quantity of heat to be lost is the increment of sensible heat obtained in cooling from the temperature of the cast liquid state, to the temperature of the solid state at the freezing point. This heat is lost by conduction through the metal itself, across the mold-metal interface and into the mold. The direction of heat travel will be to follow a diminishing temperature gradient. The process is one in which heat is lost continuously and directionally. It is also well known that freezing progresses in a direction opposite to the heat flow.

By maintaining a thermal gradient from riser to casting and temperatures of sufficient magnitude along this gradient to permit metal flow, a condition exists which will permit feeding the freezing casting. This principle of progressive solidification is well known to foundry men [3, 4]. No effort is made to confirm this principle; that has been done. The question is, what are the optimum geometric conditions necessary to establish and maintain the progressive freezing?

Since the derivation has been largely mathematical it would appear that a number of variables have been slighted or omitted. Such properties of metals as fluidity, temperature range of solidification, tendency toward segregation, hydrostatic pressure of the riser and contraction in the liquid state have been considered in respect to their probable effect.

It would be advisable to investigate several alloys which have marked differences in their metallurgical character. Only in this way can concrete evidence be presented in respect to the above mentioned properties. However, it may be advisable to inject a thought or two concerning these variables, keeping in mind the fundamental conception that the problem is one of heat transfer.

Fluidity is a property related to the character of the alloy or to the temperature of the liquid. To improve fluidity, particular additions may be made to the alloy or the temperature of the liquid metal raised. The singular purpose of this property is to promote a completely and readily filled mold. A higher casting temperature will require a longer "cooling to freezing" period but neither that nor alloy composition should alter the basic reasoning of the derivation.

That the temperature range of solidification does not influence the results obtained by the method is borne out by the experimental data (see Appendix).

Segregation in an alloy is quite frequently related to the range of solidification and the solubility of constituents. Should a pronounced interdendritic network be formed in the upper portion of the freezing range, metal flow may be restricted. Such a condition might require a larger gate.

Hydrostatic pressure is an added means to promote flow. The amount of metal used in a riser is considerably more than is needed to compensate for the contraction of the casting. This excess metal is more directly associated with the requirement of a heat reservoir, rather than hydrostatic pressure.

The contraction encountered in the cooling and freezing of a cast metal must be provided for in some manner. Those alloys which possess a high degree of contraction will require more careful gating and risering than those of less degree. However, the principle of heat transfer and the method by which the transfer is maintained will be essentially the same as has been considered in this paper.

In Fig. 4 are shown two curves which relate the ratio of bob diametter to casting thickness as a function of the shape factor. If a given volume of metal, V, is cast as a sphere, the ratio R_b/R_c is a quantity whose value is assigned as x. If this volume, V, is now cast as a flat plate the section thickness will be less than the sphere but the ratio R_b/R_c will be greater than x. In other words a change in section shape of a casting will influence the ratio of bob diameter to section thickness.

It is recognized that further experimental data should be obtained to support the analysis presented. Experience with other molding materials, different sizes and shapes of castings should be worth consideration. It is hoped that further work will bear out the present contention and point the way to a more complete solution of the problem.

Summary

1. Based upon a fundamental heat transfer equation the derivation of a general gating equation has been made. Certain assumptions in the derivation have been supported by the use of experimental data.

2. A certain amount of successful experience has been accumulated by way of practical application of the method to castings made in plaster molds.

3. Certain factors or variables concerned with the metallurgical character of metals have been considered. These factors have not been fully investigated but it is felt that they will not greatly alter the heat transfer analysis.

4. Curves have been presented which indicate the relation of a shape factor to dimensions of the casting and feeder.

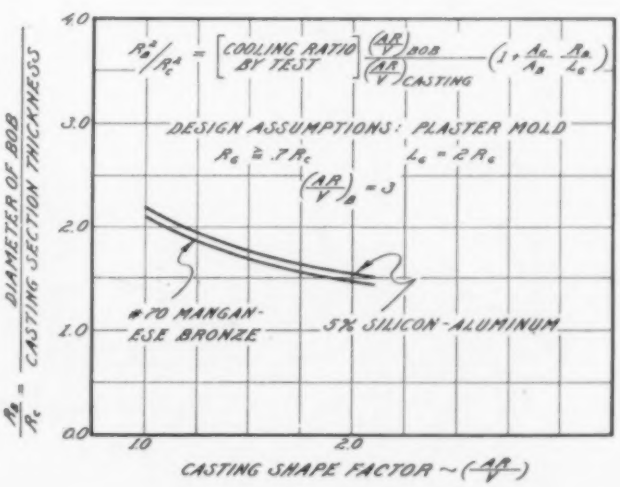


Fig. 4—Curves for size of bob derived from general gating equation.

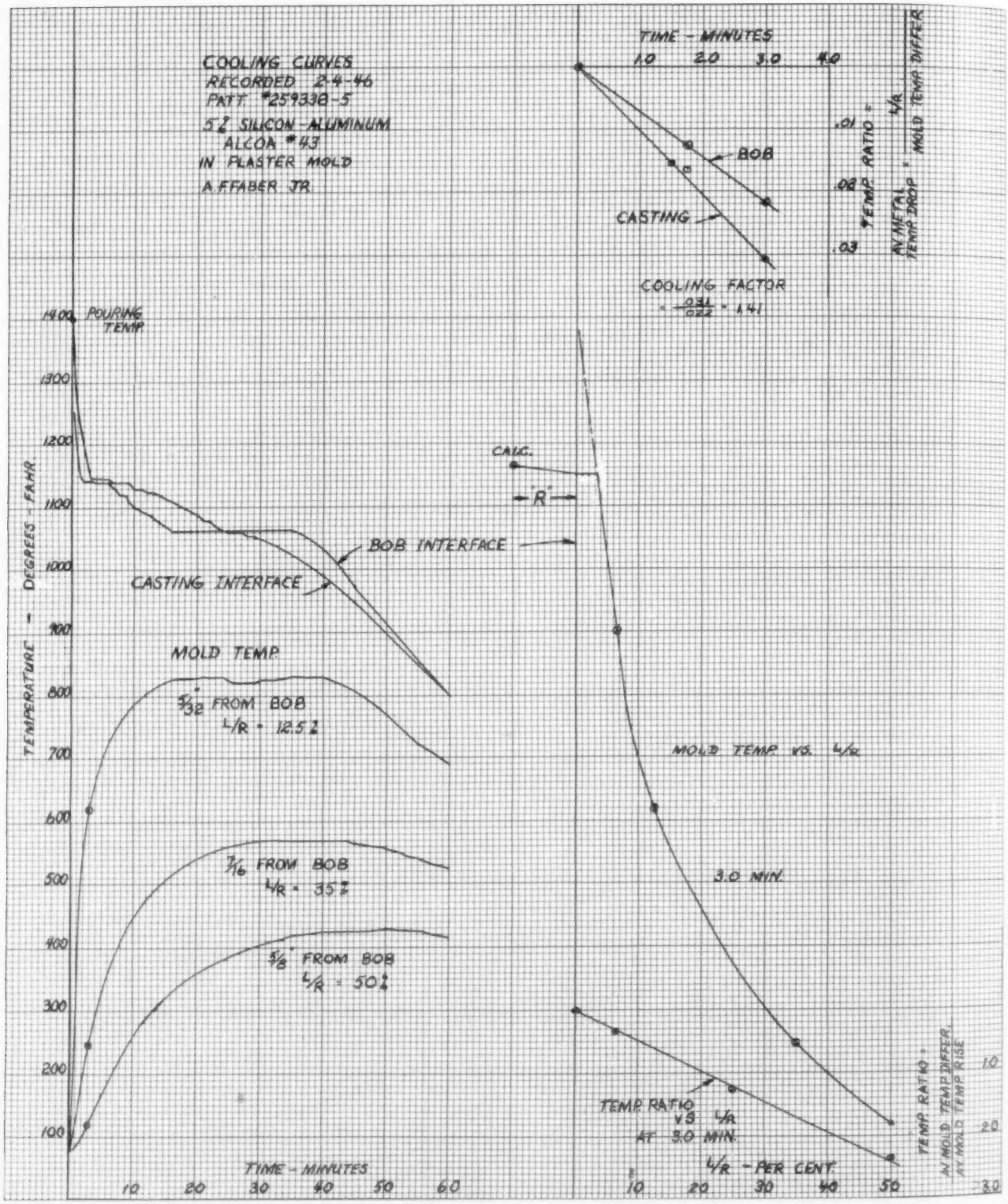


Fig. 5—Curves for 5 % Si—Aluminum Alloy

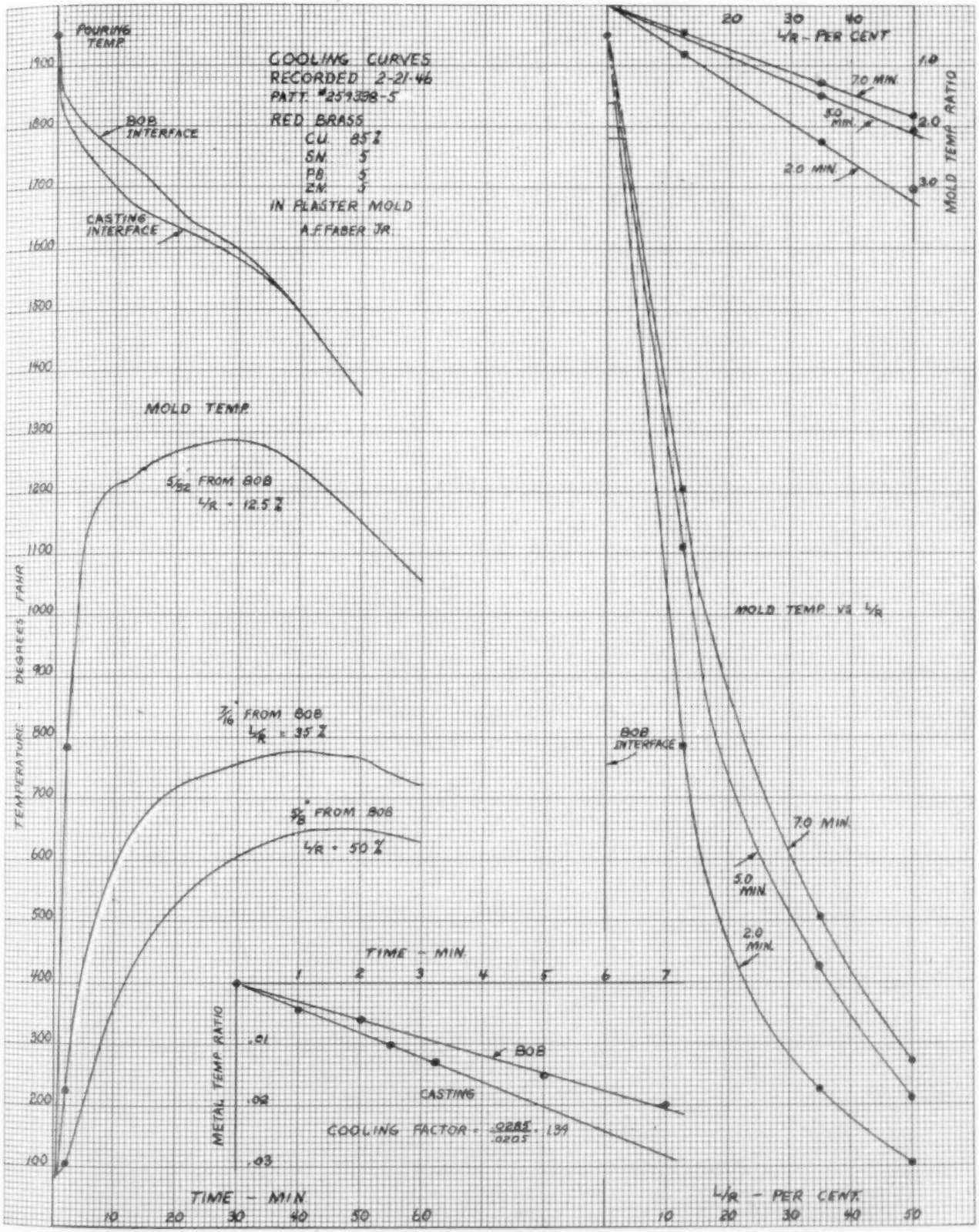


Fig. 6—Curves for Red Brass

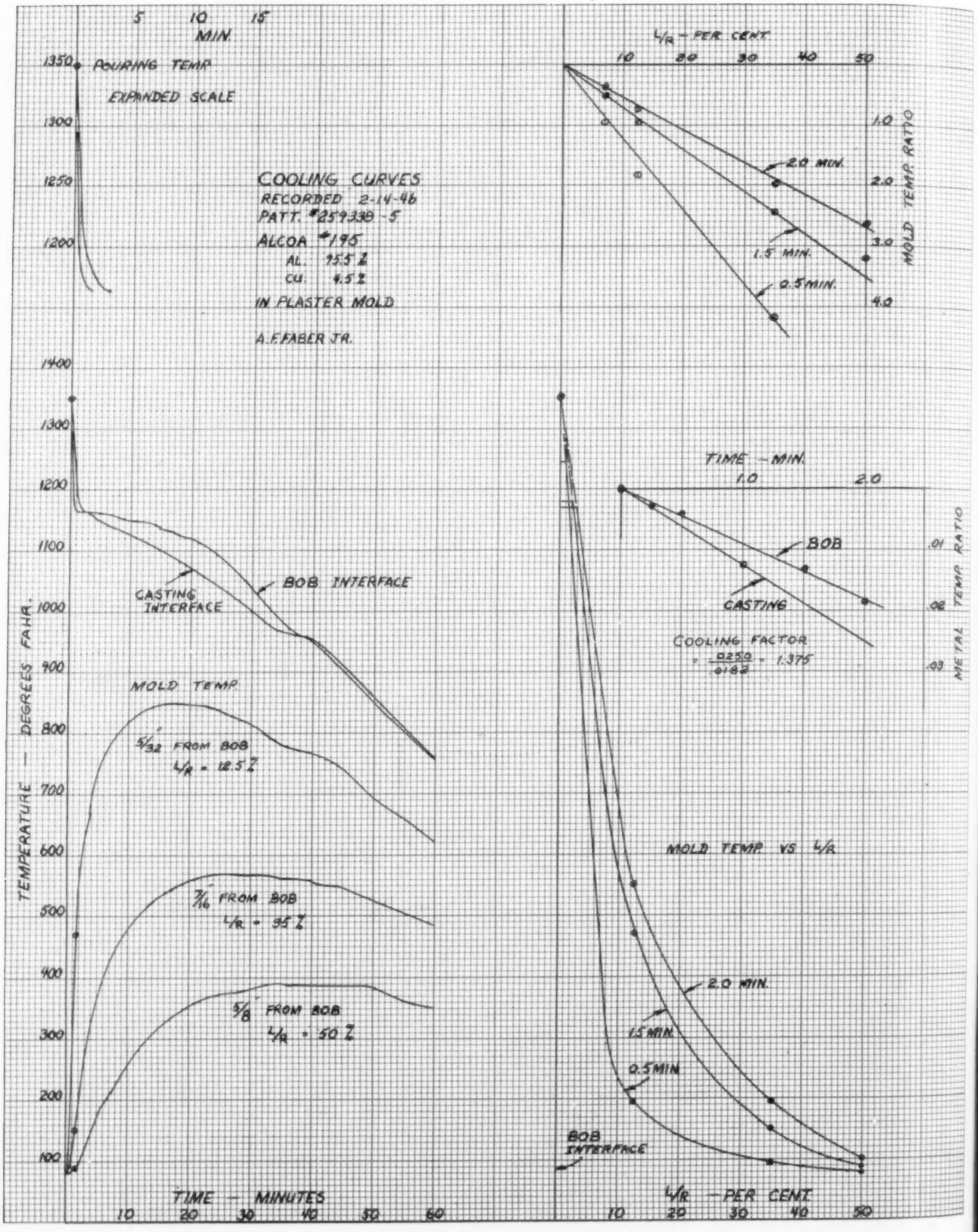


Fig. 7—Curves for No. 195 Aluminum Alloy

5. Further work is suggested to broaden the application of the equation and to support the contentions herein presented.

Appendix

Calculations similar to those previously performed for manganese bronze are indicated below for a 5 per cent silicon aluminum alloy. Figure 5 presents curves for the experimental data and the calculated values. Figures 6 and 7 are curves of a similar nature for two other alloys, calculations for which are omitted here.

A. Calculation on 5 per cent silicon-aluminum alloy:

Penetration depth $\Theta = 3.0$ min.
At $L/R = 0.50$, mold temp. = 120 F

Average mold temperature differential $\frac{1400+1150}{2} - 120 = 1155$ F

Average mold temperature rise

1st ¼ L/R $\frac{1400+620}{2} = 1010$

2nd ¼ $\frac{620+370}{2} = 495$

3rd ¼ $\frac{370+220}{2} = 295$

4th ¼ $\frac{220+120}{2} = \frac{170}{4)1970} = 492.5$

Temp. ratio $= \frac{1155}{493} = 2.34$

B. At L/R=0.25 and mold temperature 375 F

Average mold temperature differential $= \frac{1400+1150}{2} - 375 = 900$ F

Average temperature rise

1st ½ L/R $\frac{1400+620}{2} = 1010$

2nd ½ $\frac{620+375}{2} = \frac{497}{2)1407} = 703.5$

Temperature ratio $= \frac{900}{703.5} = 1.28$

C. From curve temperature ratio vs. L/R corrected value of temperature ratio for L/R=.50 is 2.4

$$\text{Constant} = \frac{0.50}{2.4} = 0.2085$$

D. Calculate L/R for 900 F mold temperature

Average mold temperature differential $= \frac{1400+1150}{2} - 900 = 375$ F

Average mold temperature rise $= \frac{1400+900}{2} = 1150$ F

Temperature ratio $= \frac{375}{1150} = 0.326$

L/R = 6.8 per cent
Assume $k_a = 120$

$$\Delta t = \frac{500}{.068} \times \frac{.1775}{120} = 10.85 \text{ F}$$

A. Calculation of cooling curve for 5 per cent silicon aluminum alloy. Calculation of average temperature drop—shrink bob.

Time	Bob Temp.	Temp. Drop	Metal Temp. Ratio	Temp. Ratio $\times \frac{k_p}{k_a}$
Pour	1400	...		
½	1300	100		
1	1250	150		
1½	1220	180		
2	1190	210		
2½	1170	230		
3	1150	250	$\frac{166}{11.3}$	$\frac{166}{11.3} \times \frac{11.3}{610} \times \frac{0.08}{1.0} = 0.02175$
3½	1145	255		

1st min. average temperature

1st ½ $100/2 = 50$

2nd ½ $\frac{100+150}{2} = 125$

$2)175 = 87.5$

2nd min. average temperature

$1st+2+3+4,\ 4)535 = 134$

3rd min. average temperature (in same manner) = 166

B. Assuming $k_a = 120$

Average metal temperature differential $= \frac{610}{0.08} \times \frac{0.1775}{120} = 11.3$

Mold temperature ratio $= \frac{166}{11.3}$

Plot 0.02175 vs. $\Theta = 3.0$

C. Using general gating equation to calculate Θ.

$$\Theta = \frac{60\ (.02175)\left(\frac{1.24}{.12}\right)^2}{\frac{120}{162.5 \times .260} \times \frac{.1775}{120}\ [1.10]}$$

$\Theta = 2.79$ min. vs. 3.0 by test.

A. Cooling curve of casting 5 per cent silicon aluminum alloy.

Calculation of average temperature drop.

Time	Casting Temp.	Temp. Drop	Temp. Ratio	Temp. Ratio $\times \frac{k_p}{k_a} \times \frac{\left(\frac{AR}{V}\right)_{\text{casting}}}{\left(\frac{AR}{V}\right)_{\text{bob}}}$
Pour	1400	...		
½	1220	180		
1	1180	220		
1½	1150	250	$\frac{175}{11.3}$	$\frac{175}{11.3} \times \frac{11.3}{610} \times \frac{.08}{1.0} \times \frac{2}{3} = 0.015$

For 1.5 min.

1st period $180/2 = 90$

2nd $\frac{180+220}{2} = 200$

3rd $\frac{220+250}{2} = 235$

$3)525 = 175$

From penetration depth recorded for bob and at same temperature of metal, $\frac{k_p}{k_a}$ is known and can be corrected for $\left(\frac{AR}{V}\right)$ of casting.

Plot 0.0153 vs. Θ on graph of cooling curve.

B. From graph of cooling curves at Θ=3.0

$$\text{Cooling factor} = \frac{0.0305}{0.0215} = 1.42$$

Cooling curves—5 per cent silicon aluminum cooling data.

Proceed with method of calculations as established. Apply penetration depth data of curves to cooling data of curves.

Then cooling ratio for bob at $\Theta = 3.0$ is 0.0212 and cooling ratio for casting at $\Theta = 1.75$ is 0.01615.
Plot curves as straight lines to origin.

Then, cooling factor for gating equation $= \frac{0.01615}{0.0124} = 1.30$

Calculation of cooling time for shrink bob from 1400 F to 1230 F.
Correct density of 170 lbs/ft³ for elevated temperature

Linear coefficient	0.00001234
Volume coefficient	0.00003702
For 1200 rise	0.0443

Correct density $\frac{170}{1.0443} = 162.5$

Then,

$$\Theta = \frac{60\ (.0124)\left(\frac{1.25}{12}\right)^2}{\frac{120}{162.5 \times .260} \times \frac{.1775}{120}} \quad [1.10]$$

Θ min. = 1.59 calc. vs. 1.75 by test.

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1. W. H. McAdams, *Heat Transmission*, McGraw-Hill, New York (1933).
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   e. Marks, Mechanical Engineers Handbook, 1930
3. H. A. Schwartz, "Solidification of Metals," *Transactions*, American Foundrymen's Association, vol. 53, p. 1 (1945).
4. C. W. Briggs, and R. A. Gezelius, "Studies on Solidification and Contraction in Steel Casting," *Transactions*, American Foundrymen's Association, vol. 41, pp. 385-448 (1933), vol. 42, pp. 449-476 (1934), vol. 43, pp. 274-302 (1935).

DISCUSSION

Chairman: H. F. TAYLOR, Massachusetts Institute of Technology, Cambridge, Mass.

Co-Chairman: W. J. KLAYER, Aluminum Industries Inc., Cincinnati

CHAIRMAN TAYLOR: While you are dealing in this case with plaster molds and can neglect the factor of fusion, that is not so easily done when one considers the pouring of metals in sand molds. Steels, for example, would immediately start to solidify. Some of the physics and mathematics in this calculation are based upon complete lack of consideration of initial fusion. If the first crystal begins to solidify shortly after pouring then perhaps your equation would not work.

MR. FABER: In the non-ferrous alloys, we have higher thermal conductivities which tend to equalize temperatures through the bob and through the casting. I should have pointed out on one of those aluminum alloys we calculated the temperature differential between the center of the feeder to the surface and it is only 12½ degrees. If this were the case in iron, that temperature differential would undoubtedly be greater. With the amount of superheat that you use in pouring steel, whether that is so little that you would get into the heat a fusion almost immediately, I do not know.

I would like to go a little further and say that suppose for the purpose of test you deliberately put on a lot of heat of fusion. In that way we can get the test data and, knowing that cooling ratio with that high heat of fusion, put it in the equation to get the general gating equation. You will be on the safe side anyway and maybe by further experience just take that curve as in Fig. 4 and slide it down a little. I do not think it is going to come down much but if you work it out for a high superheat, pouring hot castings, you know in pouring colder castings you are certainly going to be on the safe side. If the geometry of the cavity in the mold tells us whether we are going to get a good casting or not, everything else being equal, once we have established the feeder we can make a good casting time after time and the reason for it would be that the geometry did control. We are using this reservoir outside the casting to produce a good casting. The size of that we know is going to control the heat transfer that takes place. Maybe under actual conditions you would get into freezing a little sooner but for the purpose of the test and the purpose of giving you something that you can use, curves as in Fig. 4 will be fairly accurate.

DR. V. PASCHKIS[1]: On p. 463, paragraph C refers to Fig. 1, in which the straight lines represent the cooling of the bob as if it were just natural that straight lines obtain. The mathematics in the paper is based on this assumption. It would be important to know the basis for this assumption.

MR. FABER: In Eq. 1, supposing we just transpose the differentials of temperature, collect them on one side. Then the differential of time is equal to some constant times a ratio of those differentials. In that form of equation, time equal to a constant times the ratio of the differentials, that would indicate a straight line function.

N. JANCO[2]: How does the size of the bob take into account the amount of volumetric shrinkage of the casting during the solidification period?

In Fig. 4 you show that the shape factor varied over a range of practically speaking from one to two or from one to three. The curve was very flat, with the result that it appeared that the effective shape was not of reat moment upon the answer you got so far as ratio is concerned. The fact that the curve is so flat meant you could have a wide range in shape factor and still obtain very closely the same answer so far as the ratio is concerned, which is the vertical axis. That was rather puzzling because it did not seem to cover a very wide range.

You stated that for a particular casting of any type, a little bit more complicated than the one you chose there, how did you actually know what shape that was? You mentioned that it can be calculated very simply or by looking at it. I do not follow that very clearly. Then again I do not follow that it can come back to the fact that regardless of the shape factor, you get very closely the same answer on the vertical axis because of the flatness of the curve.

MR. FABER: I am glad you brought out the point about the relation between the volume and the actual volume shrinkage. You will find in static casting you have a different condition than what you have in a centrifugal casting in that we are using a reservoir to get a heat mass. Take the weight of the casting and multiply it by something around 3 or 5 per cent and add that amount to the top of the bob.

In talking about any bob, the height has to be equal to or just about equal to the diameter otherwise the shape factor of this bob and the size of the bob is really different because instead of looking at the diameter, you have to look at the thickness instead. Your shape factor would change if this height were too small. Start out by saying the height has to at least equal diameter and then add a certain amount to the height by the percentage of weight for volume shrinkage. That will put you on the safe side.

You have a little advantage in pouring metal in steel molds in that it is a lot easier to establish these cooling slopes and the ratio between the casting and the feeder. If you could take measurements and work out such a test for your iron molds you would find it much easier to establish those ratios that will produce a sound casting. That lets you make these feeders smaller.

If you were to apply the curve to different shapes, I do not think it would look so flat when you actually got done. In other words, if you took this particular casting and then took a flat casting and maybe something in between there, you would find that the size of that feeder was appreciably different. I do not know that I can express it very simply. It is just the way the mathematics came out. It does give you a flat curve. The slope that is required to get a good casting will influence somewhat.

Calculating shape factors, we would calculate the area and the volume and we can measure the section thickness to feed into. The ratio of those three numbers is our shape factor. You can look at them and guess. I say that some fellows would a lot rather do that. You will find after a little practice that you come pretty close. If you did calculate it, it would not make so much difference in the actual size of the feeder. Really the more important thing to watch is the size of that gate

[1] Columbia University, New York City
[2] The Centrifugal Casting Machine Co., Tulsa, Okla.

when you are working with the higher conductivity alloys.

CO-CHAIRMAN KLAYER: How would your method work if the casting was quite large and complicated?

MR. FABER: If the casting were larger, let us say ten times larger, I would still use this formula. The formula should hold true because the heat transfer analysis was set up on dimensionless ratios. I do not think that we could get beyond the limits of the equation itself. The volume of the shrink bob as we go up is going to change, of course.

CO-CHAIRMAN KLAYER: I specifically had in mind two or three more of these on one casting. Would you pour from four places or run around and put a shrink bob on each? Say it is a casting of four cylinders and no other connection between the parts. Not just a simple casting. I mean an engine cylinder block or something of that kind.

MR. FABER: In a more complicated casting of that type there is a definite point or definite limit as to how far from a riser you can feed metal into a casting. I do not know what that is. The formula does not take that into account. I would start out with just one feeder. Maybe your experience would tell you that you would have to put more than one. If you did have to put more than one still on that section thickness, I would use the same size of feeder. In other words, keep adding more of the same size of risers and feeders.

CO-CHAIRMAN KLAYER: I would assume in large aircraft castings, you would just add the riser where needed but would your formula hold and how could you start from your basic formula to tell approximately what size the shrink bobs would be? Would the formula hold?

MR. FABER: I think the formula would hold. We would still take the same shape factor and thickness and that would tell us the size of your feeders. How many you would have to put on, I cannot tell from the formula. This may be letting something out but I think such a thing has a chance of being calculated if the mathematics involved here could be established. I can see another step which may permit us to do that.

MATERIAL HANDLING IN A MALLEABLE FOUNDRY PROCESSING DEPARTMENT

By
N. J. Henke [1]
Saginaw, Michigan

THE PURPOSE OF THIS PAPER is to outline the general principles of good material handling in a processing department as well as to illustrate the economies and improvements that can be realized by the application of these principles.

The material source is a malleable foundry producing approximately 330 production and 150 service castings, varying in weight from 0.3 lb to 86 lb, at an output of approximately 550 tons per day. However, the methods that will be discussed are applicable in small as well as large processing departments.

High production molding practices demand the casting of several pieces simultaneously. Consequently, it is practical and economical to handle the castings by mixed groups in the foundry and through the anneal. However, it is necessary that the castings be sorted in the processing department and that every casting be handled individually several times before it is ready for shipment.

The operations in the processing department vary from sorting the rough castings after annealing to inspecting the finished castings prior to shipping. Since the work performed at each operation is not identical for each type of casting, it is necessary that material handling methods be flexible and diversified.

Belt Conveyor. Powered belt conveyors are effective for operations that involve constant supply of materials in large quantities over a definite line of flow. One such operation is that of sorting the castings after they have been annealed.

Figures 1 and 2 illustrate a sorting system of elevated endless belts arranged in a rectangular manner and paralleled by storage hoppers and disposal chutes. A system of this nature promotes rapid and economical movement of castings from the anneal into the processing department and permits effective sorting directly to the grinding operation without further handling.

Economic material handling is obtained by the use of transporting equipment that is capable of being used for a variety of operations. Such equipment should be designed to permit its effective use under changing conditions and varying rates of production.

The high lifting, electric power, platform truck, Fig. 3, and the dump type hopper, Fig. 4, are the nucleus of efficient and flexible material movement.

Their use enables the rapid movement of large quantities of castings between operations, provides storage ahead of the operations. In addition, there is a rapid turnover of equipment since the hoppers may be dumped into larger capacity stationary bins at each operation. This equipment also makes possible the supplying of materials to the operations by gravity since it is possible to load castings into a container up to nine feet high, as illustrated by Fig. 5.

Magnetic cranes are the best method of moving rough or finished castings to and from storage when inventories are large.

Although magnetic cranes have a high initial cost, this is adequately offset by the fact that they enable piling the castings to great height in bins, thus conserving expensive floor space. They provide flexible and effective movement of castings away from their last operation, thus eliminating the necessity of a great many small capacity disposal containers (tote boxes or dump hoppers). In addition, they are capable of raising large quantities of castings with ease to levels that permit gravity supply for additional handling.

Figure 6 illustrates a 7½-ton bridge-type magnetic crane over a group of finished casting storage bins. This crane is used for moving castings from the straightening presses, Fig. 5, to the storage bins and from the bins to the shipping dock for loading. The loading system will be discussed later in the paper.

Manual Movement by Correlating Operations

The most economical method of moving castings through their processing operations is that of correlating two or more operations in an "in line" manner.

Figure 7 illustrates the correlation operations of an "in line" manner. The processes are Brinell testing and magnetic particle inspection of castings. This system is designed for processing castings varying in nature from valve rocker arms (0.3 lb) to 4¼ in. pistons (10.0 lb) through the following operations:

1. Spot grind Brinell pad.
2. Brinell test (dial indicator type machine).
3. Magnetize each casting.
4. Apply magnetic powder.
5. Visually inspect for cracks and other defects.

Many different types of high production castings

[1] Supt. of Cleaning and Inspection, Saginaw Malleable Iron Division, General Motors Corporation, Saginaw, Michigan.



Fig. 1—Sorting castings into storage hoppers and disposal chutes paralleling endless belt conveyor system.

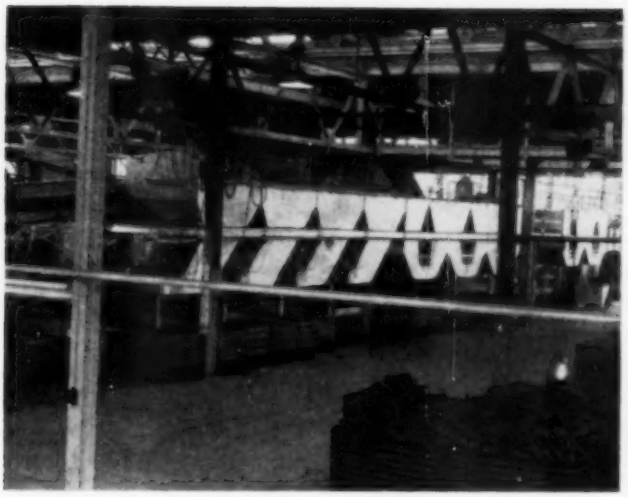


Fig. 2—Hoppers receive castings for grinding operation from the paralleling elevated endless belt conveyor.

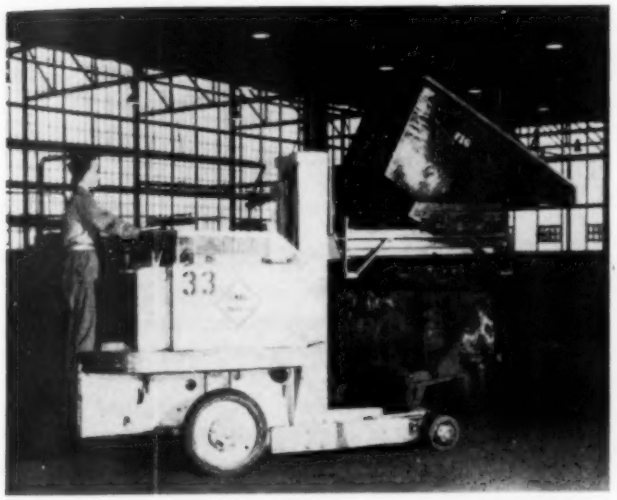


Fig. 3—High-lift electric platform truck and dump-type hopper form flexible unit for material movement.

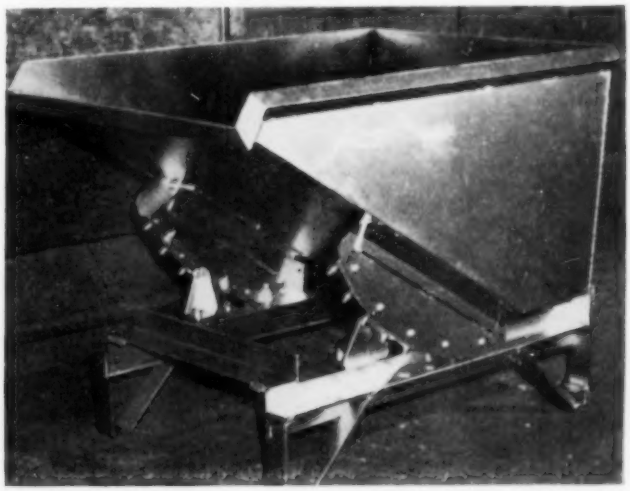
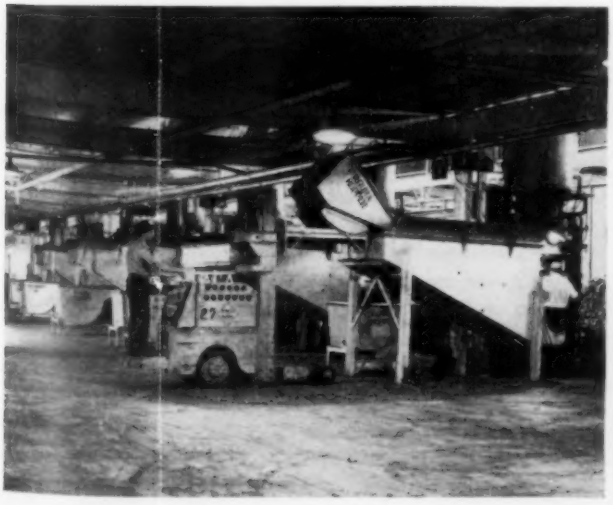


Fig. 4—Dump-type hopper used with high-lift platform truck for moving between and ahead of operations.

Fig. 5—Transferring a load of castings to large gravity-feed hopper supplying a straightening operation.

Fig. 6—Magnetic crane moves castings from the straightening operation to storage bins and shipping dock.



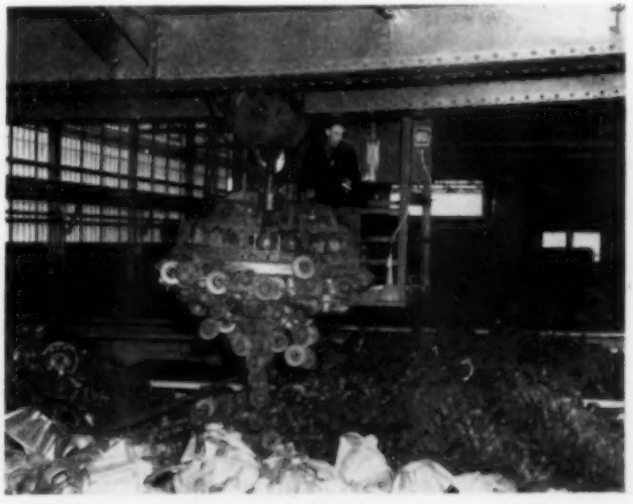




Fig. 7—"In line" movement with double-sided storage hoppers for Brinell and magnetic particle testing.

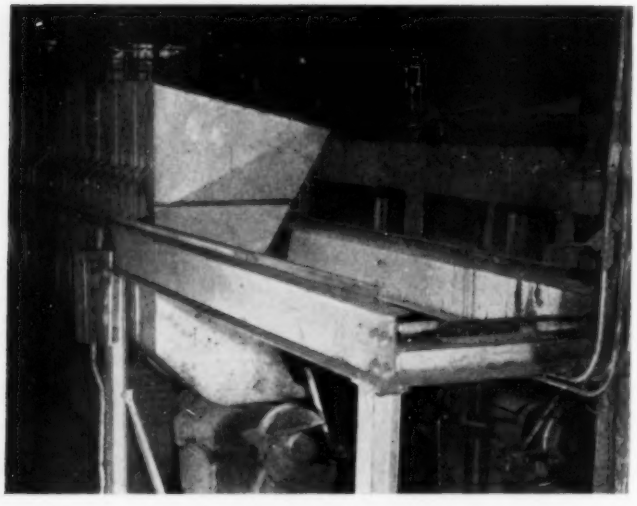


Fig. 8—"In process" 22-hopper storage system provides flexibility for six-station snag grinding operation.

Fig. 9—Castings of various types are stored in stationary sectioned bins ready for the final visual inspection.





Fig. 10—This method of counting castings for shipment is slow, inefficient, and fatiguing for the operators.

are run through these processes, thus necessitating the efficient flexible performance of each operation. The flexibility is derived by supplying the castings to the operator in double-sided storage hoppers. This enables a different type of casting to be run at each operation without hampering any of the other operations. The efficiency of the operations is derived by good work-place layout.

Processing Operations Vary

It is impossible to recommend definite types of equipment or operations for the application of this method of material movement because processing operations vary greatly in different foundries. However, the following general points provide a guide for the development of an "in line" movement.

1. Select only operations that are, or may be altered to be, of comparable rates of production.
2. Select castings that may be handled efficiently by hand.
3. If several types of castings are to be subjected to the same operation, provide double-sided storage at each operation.
4. Design equipment and arrange machines in a manner that permits the performance of each operation and the transfer of each casting to the next operation while the operator remains in a fixed location.

It would be difficult to devise a production program with which a malleable foundry could produce each day only those castings to be shipped that day. Such a program would require an impractical number of molding stations or an excessive number of pattern changes each day. Consequently, it is imperative that once a pattern has been placed in production, a predetermined quantity of castings be produced to cover known or forecast customer requirements.

However, once the castings have been made, it is a problem of the processing department to store them as finished or to hold them in abeyance of processing.

No hard and fast rule can be applied in determining the size and nature of "in process" inventories. It can be said, however, that these inventories should



Fig. 11—Improved method of counting castings for shipment. Operators may stand erect while handling.

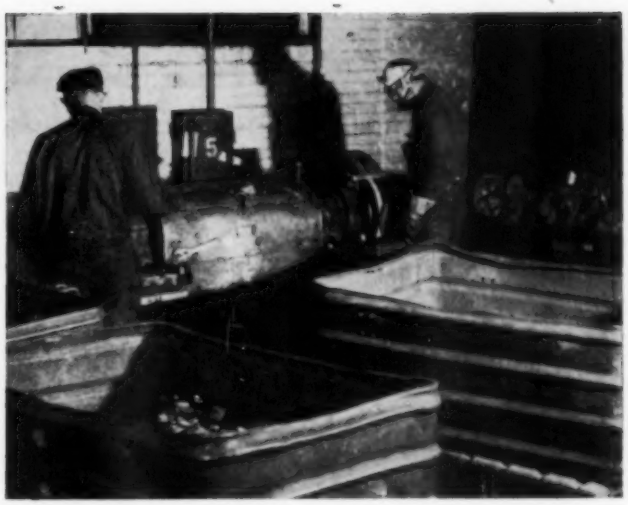


Fig. 12—Snag grinding from tote box to tote box, requiring the operator to move considerable distances.

be controlled sufficiently to prevent their size exceeding the storage and handling capacities of the processing department.

Storing castings on the floor in piles is undesirable since it involves expensive rehandling to pick them up and it contributes to poor housekeeping and bad safety practices. Also, castings which have been sorted may become mixed and require resorting.

Storage Equipment

The ideal theme of "in process" storage is to provide storage equipment for each operation that permits changing the operation from job to job at any time with a minimum of delay, either by moving the desired castings to the operator or by moving the operator to the desired castings.

Moving the castings to the operator is illustrated in Fig. 8 by a snag grinding system of six grinding stations and 22 movable supply hoppers that may be moved as desired by manually pushing from station to station.

Figure 9 illustrates the principle of storing quantities of castings of various types in sectioned hoppers and moving the operators as desired from one bin to another. The operation illustrated is visual final inspection before shipping.

Fatigue. The most elaborate equipment is of little value unless adequate consideration is given to the human element of material handling. As previously stated, each casting in the processing department is handled individually several times before it is finished and shipped. Although the movement from one operation to another may be mechanical, the handling in the performance of the operation is physical. Consequently, there may exist the element of operator fatigue.

The elimination or reduction of fatigue in many cases may require the design of special intricate mechanical equipment. However, most operations may be improved readily by the application of a few motion time analysis principles and the law of gravity.

A comparison of the two methods of counting cast-



Fig. 13—Snag grinding operation improved by raising operators and machines above disposal containers. The castings are supplied from a stationary hopper.

Fig. 14—Straightening operation illustrating crowded working space and haphazard material arrangement.





Fig. 15—Straightening operation improved to give ample unobstructed working space and material supply.

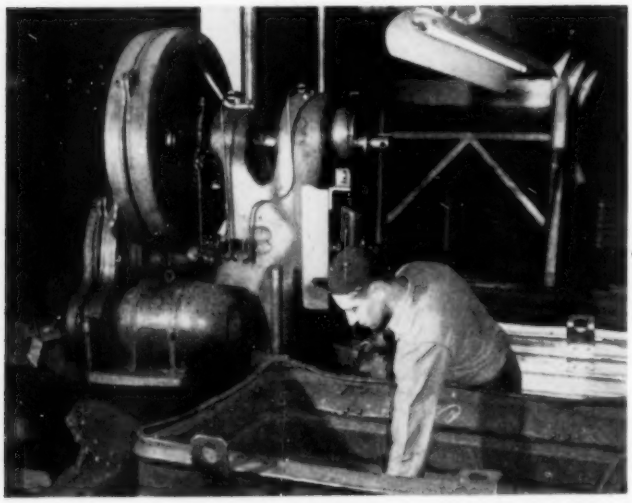
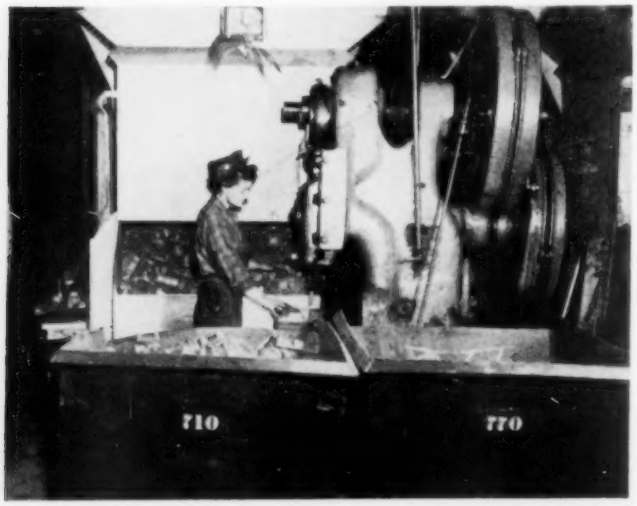


Fig. 16—This shearing operation set-up is undesirable because operator must search in box for castings.

Fig. 17—Improved set-up permits operator to concentrate on casting being sheared while grasping another.



ings for shipment, illustrated in Figs. 10 and 11, demonstrates the improvement possible with the installation of simple equipment.

The method in Fig. 10 required the employees to work in a bent-over position for long periods of time and to handle the full weight of each casting through long distances to transfer it from one box to the other. Having counted the castings, the box would then be weighed and placed in a freight car to be emptied by the same method that it was filled.

Gravity Utilized

The improved method, Fig. 11, permits the employee to stand in a normal, erect position and to count the castings as they slide them from the stationary hopper into the dump type gondola in a downward motion without having to handle the full weight of the casting. The gondola, when filled, need only be weighed and dumped into a freight car.

Figure 12 and Fig. 13 demonstrate the same principles applied to the operation of snag grinding. Here fatigue has been eliminated by raising the operator above point of disposal and by supplying castings to him by gravity at a location about which he need move only short distances in performing his operation.

Working Conditions. Working conditions, although generally associated with lighting, ventilation, etc., are an important phase of the human element in material handling. Good housekeeping is one element of working conditions that may be derived from effective material handling. It will be noted in Fig. 14 that the press operator is confined to a very small area by the two tote boxes, the location of which may vary as the boxes are changed. The hoisting eyes on the tops of the boxes are obstacles that he must avoid every cycle of his operation. Also, if the operator should leave the machine it would be necessary for him to squirm through a narrow opening or climb over one of the boxes.

These undesirable conditions are eliminated by supplying the castings to the operator in a stationary hopper, Fig. 15, and by providing a pit at the right of the press into which the castings may be disposed.

Safety is another element to be considered in developing material handling methods for an operation. A good general rule is to supply the material to the operator in such a manner that he may grasp and transport the casting to the operation without eye-directed movements. This permits the operator to concentrate his attention on the area of the operation where a hazard may exist. Figure 16 and Fig. 17 illustrate shearing operations, and a comparison of the two systems demonstrates principles mentioned.

Conclusion

Many more types and examples of material handling could be discussed. Since each operation requires individual treatment, only an outline of the fundamental principles of good material handling has been presented. However, foundrymen should realize that without adequate and efficient handling methods in the processing department, the most effective practices of economy in the foundry are useless.

DISCUSSION

Chairman: R. J. ANDERSON, Belle City Malleable Iron Co., Racine, Wis.

Co-Chairman: J. A. DURR, Albion Malleable Iron Co., Albion, Mich.

J. D. FRASER[1]: Am I right in understanding that your castings are processed in a mixed condition from the shakeout to the finishing department?

MR. HENKE: That is true, from the shakeout to the hard iron and through the annealing process in the mixed stage.

MR. FRASER: On what basis are your molders paid?

MR. HENKE: They are paid on an incentive basis.

MR. FRASER: How do you get a mold count?

MR. HENKE: There is an electric-eye count and a hand-meter count.

MR. FRASER: Do you have a hard iron inspection?

MR. HENKE: Yes, we do. It is done in a productive manner by the people who do the job packing the castings in the pots for annealing. Then we have an overseer on each shift in the hard iron room and in the foundry who are in constant contact with the scrap, that may enter the hard iron room or the annealing room and they report back to the foundry.

MR. FRASER: You say that your packers do the inspecting. With your castings mixed, does this not call for a great deal of versatility?

MR. HENKE: In the hard iron room we sort to size only. In other words, from the foundry the castings are brought to the hard iron room by bucket type conveyor and dumped on a long knockout. They are sorted as to size, large, medium, and small. Each operator will handle approximately eight to ten different types of castings, and they are the jobs that the packers inspect for scrap. However, from day to day the castings may vary, and it is true a great deal of versatility is required.

R. A. LODER[2]: In inspecting the castings by magnetic particle inspection, do you find it necessary to demagnetize the castings?

MR. HENKE: We do not find it necessary to demagnetize the castings 100 per cent because they receive a great deal of impact due to dropping, and are handled three times after which they are finally handled with a magnetic crane.

MR. LODER: How hot are the castings when they come through the packing room?

MR. HENKE: They are down to black heat.

MEMBER: What happens if there is something wrong with the castings and they have to be processed again?

MR. HENKE: It is a matter of taking them out of the finished bin and running them back through the operation.

MEMBER: Do you put them back in the bin by means of the overhead crane?

MR. HENKE: Yes. We take them out and return them with a magnetic crane.

E. M. STRICK[2]: How do you get the castings into trucks or cars for shipment?

MR. HENKE: The electric platform truck (Fig. 3) is used to transport these dump type hoppers, and after the loaders, as we call them, have filled one of these gondolas (Fig. 4) that electric truck picks up the gondola, puts it on a scale, weighs it, runs it into a box car and dumps it.

MR. STRICK: How do you get your count?

MR. HENKE: As the men who handle the castings throw the castings from the stationary hopper into the dump type gondola they count them. A bonded checker simultaneously makes a count. In other words, the method of dumping the castings into the car helps in providing the count.

MR. STRICK: Do you pick up these castings from the storage with the magnetic crane and put them in gondolas for shipment?

MR. HENKE: No. We take them from the storage bins with the magnetic crane and place them in the statonary hopper.

MEMBER: How do you load your tumbling mills?

MR. HENKE: We load them in part with a dump type hopper and the electric truck, merely by loading the casting hopper full of castings and running it up to the mill and dumping the hopper into the mill. That is on some type of castings. Large type castings are loaded physically by hand.

We have a gravity fall feed into the wheelabrator. We have a conveyor to transport specially designed boxes to an overhead position, dumping it into a hopper, and from the hopper into a wheelabrator and into the dump type hopper.

MEMBER: Do you have a special arrangement for handling bags?

MR. HENKE: A very small percentage of our shipment is done in bags. It is done either loose or in gons. We have a series of hoppers with ports in the center and raised off the floor approximately the depth of a bag and we hook the bag underneath and count the castings into the bag, then transport the bag by truck.

MEMBER: Do you have several of them on one platform?

MR. HENKE: Yes, we have a series of them.

MR. STRICK: Do you count those castings manually, or do you have an electric eye count?

MR. HENKE: For shipment we do all of our counting manually. The reason for weighing is to establish the gross weights for carload capacities and for checking back of unit weight against the checker count. In other words, we divide the total weight by unit weight. If there is a variation of one piece or more we recount it.

CO-CHAIRMAN DURR: Do you find it necessary to spot grind for hardness by Brinell?

MR. HENKE: Yes, we do. We find that we do not get an accurate Brinell without spot surface grinding because of the variation in skin hardness, and the fact that there are small lumps on the surface of the castings which deflect the reading.

MEMBER: Do you use the Brinell hardness test as a basis for rejection of castings? How close a limit do you use?

MR. HENKE: Yes, we do. We use a 5-point range.

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[1] National Malleable & Steel Castings Co., Cicero, Ill.
[2] Erie Malleable Iron Co., Erie, Pa.

CASTINGS INDUSTRY APPLICATIONS OF MAGNETIC PARTICLE INSPECTION

By
W. E. Thomas
Vice President
Magnaflux Corp.
Chicago

THE MAGNETIC PARTICLE INSPECTION METHOD has had over fifteen years of development and use during which time it has passed from an interesting laboratory novelty to a well recognized, highly useful and dependable method of inspection. During the war years its use was greatly expanded, although this is equally true of every other useful testing and inspection method.

War vs. Peacetime Inspection. The entire matter of inspection of materials received a tremendous emphasis during the war years. Perhaps the largest factor bringing about this emphasis was the great expansion of the aircraft industry. This industry long ago had, of necessity, set standards of quality that made rigid inspection of all kinds a prime requisite in the production of all structures and parts. However, this emphasis was also applied to all items produced for the Army and Navy—to Navy hulls, propulsion machinery, and ordnance—to Army trucks, tanks, and ordnance.

High Quality Standards

In the application of mass production methods to the construction of all of this war equipment, many large and small plants became acquainted with the highest quality standards and the most searching inspection methods. Many of these plants were previously engaged in other fields in which precision gages, magnetic inspection, or X-ray had never been necessities. Consequently these plants and the executives and engineers who operated them became more inspection-conscious than they were before.

In our present-day economy, with a demand for products in general greater than most manufacturers can meet and materials difficult and sometimes impossible to obtain, the temptation is to use what comes to hand without looking too closely at quality. Inspection in many plants has, therefore, swung to the opposite end of the scale from where it was during the war.

There are signs that in the near future, however, marketing conditions will again change, and high quality, to win and hold customers in a competitive field, will return as an important requirement, and the problem of how far to carry inspection and what methods are justified will again come up for consideration.

The inspection department of a plant, unless headed by a man who fully comprehends the broader relationship of inspection to the whole business of producing goods, is quite likely to look with favor on new tools and processes which will build up the importance of the inspection function. But he may miss some of the implications of added inspection costs.

It seems obvious that the extent to which inspection of any kind may properly be applied in the production of any article or material is a matter of a proper point of view and a proper understanding of what inspection may be expected to accomplish.

All eyes are focused on this ultimate use to see whether the design, the materials, and the construction are satisfactory in the final analysis. How the product performs in service is the criterion of judgment of the effectiveness of design and construction.

Purpose and Scope of Inspection. Inspection, therefore, should insure that the product be as good as it need be to perform its function satisfactorily for a long period of time. In general, however, there is no particular need that the materials, parts, or product as a whole be held to standards of quality any higher than required for such a performance. It is necessary, therefore, to set up inspection procedures, as well as standards of acceptance and rejection for the inspector to follow, which are based on a thorough understanding of what may be and what cannot be tolerated in the part or finished product.

Whether or not a casting is defective and merits rejection is, therefore, not necessarily a question alone of the presence or absence of such things as cracks or shrinkage or other abnormal conditions. The question rather is whether conditions which are present will actually affect the usefulness of the particular part.

Defect Classification

We should rather consider that such conditions do not constitute defects in the particular part unless they do affect the usefulness of that part. They may be blemishes, departures from perfection, but they may not be defects. Failure to appreciate this distinction has often led, in the application of such searching inspection as the magnetic particle method provides, to the rejection of much material which was perfectly satisfactory for its intended use even though it contained conditions which for other purposes would constitute defects.

From this point of view then, any inspection process should be justified by the particular results which it can produce to the end that the product be as good as need be. The product need not necessarily be better but certainly may be no worse than required by its particular function and service. It is the responsibility of

the engineer or executive who decides on an inspection program to consider and judge his inspection procedures from some such point of view.

Most foundrymen can cite one or more instances in their experience with magnetic particle inspection during the war where such principles were violated. Most of these could be set down to lack of experience and lack of knowledge, both on the part of the writer of the specifications and of the inspector who applied them. Yet in the majority of cases it is difficult to blame these people because in the urgency of war production there was a lack of time in which to acquire this experience and knowledge.

The present period allows time to appraise all methods and procedures, and inspection methods which will survive such scrutiny must be economically justified in every application.

The magnetic particle method of inspection is no longer new or novel. It has been in use in hundreds of plants and in an extremely large variety of applications and the principles on which operation of the method depends are generally understood. However, the fundamentals of the method will be reviewed for the benefit of those not closely connected with its application so as to clarify the terms used and the procedures involved.

Magnetic Particle Inspection Methods

First, the method is a magnetic method and is, therefore, applicable only to the detection of flaws in materials which can be magnetized. This includes, of course, all iron and steel and many iron alloys, but does not include austenitic alloys of various compositions and naturally does not include non-ferrous materials such as aluminum, bronze, etc.

The method depends for its operation on the fact that if a crack or crack-like discontinuity in a piece of magnetized material is so located as to be transverse to the direction of the magnetic field in the material, that field is distorted and the flux lines are crowded or deflected around the ends of such a magnetic obstruction.

If the obstruction lies near enough to the surface of the material, Fig. 1, some of these flux lines will be crowded outside the material itself, and a leakage field is produced at the surface at a point over the discontinuity. The nearer the discontinuity is to the surface, Fig. 2, the stronger is this leakage field, and if the discontinuity actually breaks the surface as, for instance, a surface crack—the leakage field is quite strong and highly localized.

If a powder—either dry or suspended in a liquid—consisting of fine particles of magnetic material, be applied over such a surface in the vicinity of such a leakage field, some of the powder will be attracted and held by the leakage field and thus set up a magnetically-held pattern outlining the discontinuity.

The greater the obstruction in the magnetic path, the stronger the leakage fields. Sharp, deep cracks at right angles to the surface give the strongest patterns and large discontinuities below the surface, having a principal dimension at 90 degrees to the surface, are more favorable for the production of strong indications, Fig. 3. Small defects, or defects of unfavorable shape, must be close to the surface to be found at all.

Surface scratches or tool marks do not produce patterns except at very high levels of magnetization.

Two points are fundamental and at once apparent:

1. Given a part containing discontinuities, it is necessary, in order to produce good leakage fields, to so magnetize the part that the resulting field will intercept the discontinuities, and

2. The strength of the leakage field, and hence the strength of the powder pattern produced will vary with the intensity of the magnetization set up in the part.

Magnetizing methods, therefore, must be understood since use of the wrong method may make the inspection entirely unproductive, and it is also easy to see that any variation in technique that affects the strength of leakage fields produced will greatly affect the nature of the results obtained.

Methods of Magnetization

Magnetism of proper strength and direction for the part to be inspected can be induced in several ways. In the early days, portable electro-magnets were widely used as a source of magnetizing force. In the light of subsequent experience, this method has been shown to be least effective, though still used under certain circumstances.

Coils into which parts are placed, or coils wound with flexible cables to suit the size and shape of the part are extensively used in many applications, but the direction of such fields is not easily controlled in odd shaped parts. Also, there is an excessive amount of general leakage field because the magnetizing force is external and the flux path is completed through the air outside the material in which the defect is sought.

A much more effective method of magnetizing, when applicable, is to pass current directly through the entire part or structure or locally, as in fillets, by means of prod contacts applied to the surface, in a direction to set up fields favorable for the creation of maximum leakage fields at defects, Fig. 4. By this procedure the external flux lines are kept to a minimum, as the field

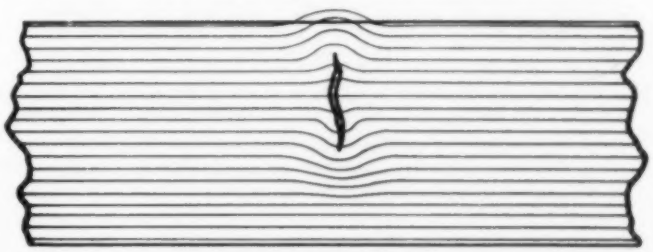


Fig. 1—Distortion of flux lines. Leakage field produced at surface at point over the subsurface discontinuity.

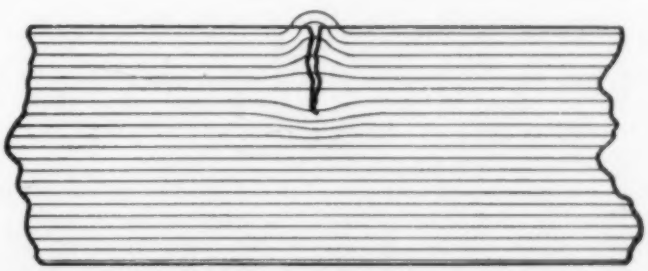


Fig. 2—Surface discontinuity causes distortion of the flux lines and produces highly localized leakage field.

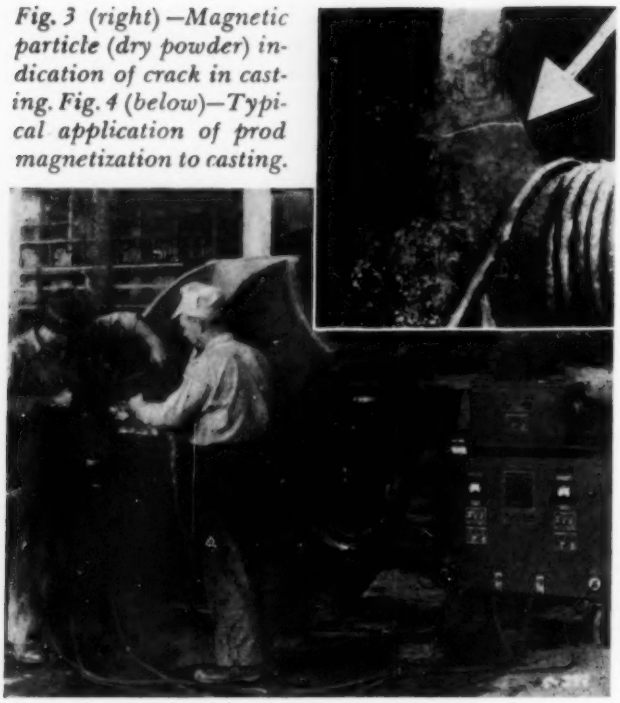


Fig. 3 (right)—Magnetic particle (dry powder) indication of crack in casting. Fig. 4 (below)—Typical application of prod magnetization to casting.

set up by the current flowing through the metal is to a large extent contained within the metal itself. The term circular magnetization is frequently applied to this method, though in irregular shapes the path of the field is certainly not circular.

Since the direction of the field generated is always at 90 degrees to the direction of the current, it is quite possible in most cases to set up a field by this method in a desired direction by properly locating contacts.

The possibilities of varying the sensitivity of the method are numerous and complex. Obviously the stronger the magnetizing current, the stronger the field, and because of the magnetic behavior of materials, stronger fields always exist while the magnetizing current is flowing than after it is cut off. The residual field which a part retains is always only a fraction of that present while current is flowing. The residual field is also a function of the composition and hardness of the material, as well as of the amount of magnetizing current applied.

Continuous Method

For maximum sensitivity, therefore, it is evident that it is better to apply the magnetic powder simultaneously with the flow of the magnetizing current to take advantage of the stronger field then existing. This is the so-called continuous method.

Sensitivity may also be affected by the choice of magnetizing current—whether alternating or direct. Alternating current, because of skin effect, magnetizes the surface layers of material more strongly than those deeper in the part. Direct current on the other hand, penetrates more deeply into the cross-section.

For maximum sensitivity for the location of discontinuities lying below the surface, therefore, the continuous direct current method is the best. However, if interest lies only in surface cracks or discontinuities very close to the surface, alternating current is usually equally and sometimes more effective.

The term direct current is here applied to several types of single direction current. These are:

1. Non-fluctuating direct current.
2. Direct current with an initial surge of high current, quickly dropping down to a lower steady value.
3. Full wave rectified AC which is a slightly pulsating direct current.
4. Half wave rectified AC which is a form of intermittent direct current with no current flowing between the pulses of current which occur at the A.C. line frequency.

Direct current with surge generates an internal magnetic field in the work of a higher level than is produced if the metal is magnetized with the current rising to a steady level without the initial high surge.

The illustration shows by means of a magnetizing curve how this occurs, Fig. 5. The advantage lies not alone in the fact that the magnetic field inside the part is stronger, since this result could be accomplished by merely increasing the amount of steady current employed. The point is that the amount of external field due to the flow of current and consequently the amount of interference by this external field with the formation of powder patterns over defects, is less for the same magnetic level if the surge principle is employed.

A much more recent development extends the benefit of this principle to the inspection of all types of steel including soft materials on which the surge principle is not particularly effective. This development is the use of half-wave rectified single phase alternating current instead of direct current from any source.

Average Current

This half-wave current, Fig. 6, consists of separate pulses of direct current with intervals during which no current at all is flowing. Each pulse lasts for one-half cycle and the peak current reached is the same as the peak of the single-phase alternating current which is being rectified. The average current, however, which is read on the meter is only about a third of this peak.

Since power input and heating losses are more nearly a function of this average current, the system presents an advantage over direct current, alternating current, or full-wave-rectified alternating current in respect to size and cost of equipment necessary to produce comparable inspection results.

However, the magnetizing effect is determined by the peak value of the current and not by the average current or meter reading, and the result amounts to a high surge with each half-wave impulse. Thus with an average current of 400 amperes, there is a magnetizing effect equivalent to 1270 amperes. With this, results are comparable with, and in many applications considerably better as regards sensitivity, than those obtained with straight DC with surge, at 400 amperes steady current.

In addition, considerable advantage arises from the fact that the intermittent nature of the half-wave rectified current improves mobility of the particles and, therefore, sensitivity of the inspection. Because of the numerous advantages of this technique this method is finding a wide application not only for casting inspection, but for many other uses where maximum sensitivity is desired or specified.

There is, however, a definite trend away from the

doctrine, except where required, which insisted that the highest possible sensitivity should always be employed and that it was the business of the inspector to decide as to the significance of the conditions revealed.

Even in the casting field it has been found that the great sensitivity of the methods being discussed is not always an advantage. An excellent example is taken from the welding industry.

Many welds are designed with root openings or unfused root faces and the high sensitivity methods bring out indications of such root openings masking the presence of cracks lying near the surface. The illustration shows an instance of this sort.

A remedy for such confusing results was offered by James W. Owens,[1] who suggested that alternating current might be used to supplement direct current for the purpose of gaining additional information regarding the nature and depth of the condition producing such indications, Fig. 7. Equipment is now available to provide at the operator's option either AC or DC. Figure 8 illustration shows a portable unit from which both types of magnetizing current are available.

Another factor affecting sensitivity is the nature and shape of particles composing the magnetic powder or suspension. Experience has shown that the coarser elongated particles of the dry powders are much more effective in creating readable patterns indicating the presence of deep-seated defects than are the very fine particles generally used in suspension in a liquid for the wet method.

It would be possible to go further and discuss at great length the merits and limitations of various inspection techniques. However, in this paper, interest covers the entire subject of magnetic particle inspection and what its value may be in the foundry.

Another trend which is important to the future application of this method is in the direction of high speed inspections, and semi-automatic to automatic inspection equipment. Prior to the war, most installations of magnetic particle testing equipment consisted of a single unit which was expected to be as flexible as possible so that all sizes and shapes of parts might be inspected on a single machine.

Special Inspection Units

However, when large numbers of the same part are to be inspected, one machine may be kept busy continuously on this single job. A general purpose unit may not be well adapted to carry out such a particular inspection operation, and a special machine to do that particular job effectively would then be justified.

Magnetic particle inspection can, therefore, be put into the production line as a process complete in itself. This may involve such steps as cleaning before inspection, magnetizing, applying the magnetic particles, inspection, demagnetizing, and again cleaning before passing on to the next process, all on an automatic or semi-automatic basis. Such developments open fields of application for which the method had formerly been held useless because of lack of speed and consequent high cost.

There are other encouraging and healthy trends today in the handling of these inspection tools which are the result of greater experience and better understanding of the methods. There is a growing realization

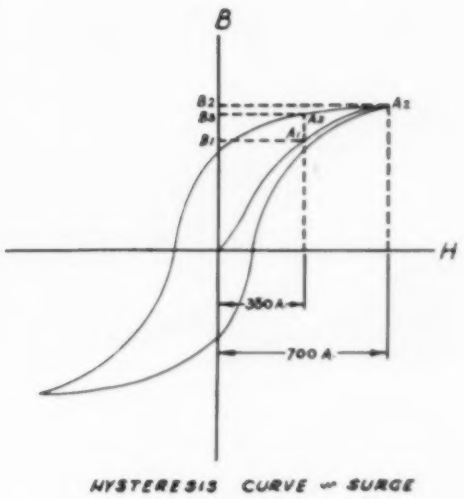


Fig. 5—Magnetization curve; initial surge advantage.

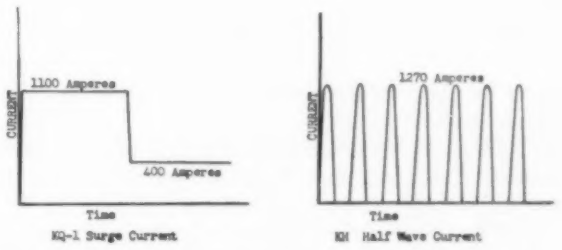


Fig. 6—Current output curves showing (left) direct current with surge, and (right) half-wave current.

that because of the wide variations in technique which are not only possible, but likely, to be used by various operators, results satisfactory from the point of view of each party interested in the inspection can be obtained only if the same techniques are used.

There has been great activity in the past two or three years in the direction of setting up specifications or recommended practices to guide the use of this inspection method. One such specification recently adopted as tentative by the ASTM relates to the use of the magnetic particle method in the inspection of castings. In addition to specifications such as these, many companies and government agencies have issued new or revised manuals setting forth the approved procedures for inspections involving their products or products in which they are interested.

Contract Specifications

The requirement for agreement between the customer and the supplier prior to a contract calling for magnetic particle inspection, which appears in the following excerpt from ASTM Specification A272-44T, is an excellent step:

"When in accordance with the requirements of the inquiry, contract, order, or specifications, castings are furnished subject to magnetic particle testing and inspection, the manufacturer and the purchaser shall be in agreement concerning the following:

1. A statement in the inquiry or specifications that the castings are to be subjected to magnetic testing.
2. The locations on the castings which are to be sub-

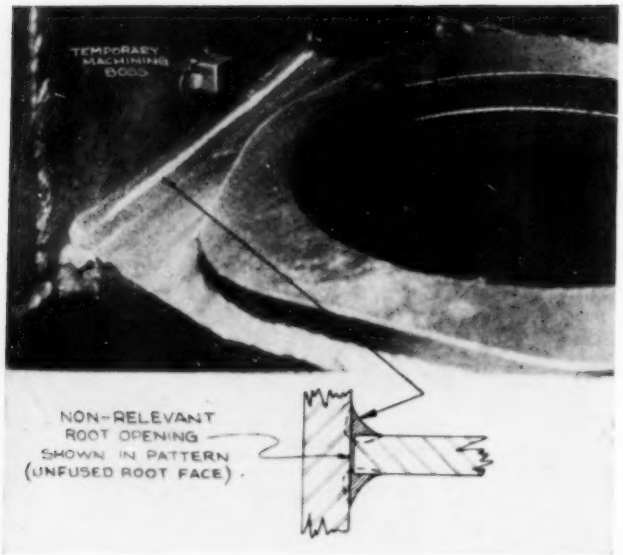


Fig. 7—Magnetic powder indication (DC) of non-relevant root opening. No pattern obtained with AC.

jected to magnetic particle testing shall be clearly defined or described.

3. The type, number, size, location, and orientation of cracks, hot tears, shrinkage cavities, or other defects that are to be considered injurious.

4. The stage of manufacture at which the test shall be conducted, surface preparation of the castings, the method of magnetization, magnetic flux density or magnetic force, field orientation, method of application of magnetic particles, demagnetization requirements, etc."

It is futile for the purchaser to specify magnetic particle inspection in a contract unless he defines how the inspection shall be made and what shall be considered the basis for acceptable or unacceptable material. Likewise, it is foolish for a foundry to accept such a contract unless these definitions are included. Merely to specify magnetic inspection for a part has no practical meaning, and would be the same as specifying a tensile test without setting the limits of strength required.

Yet specifications of this type—of which there are many—are of little use in any specific case. They do little more than discuss in general terms the various methods which may be used, the various types of current, equipment, and powders or pastes.

In the case of every individual foundry and every individual casting it is essential that the inspector or metallurgist, or whoever has supervision over the inspection, be capable of setting up the exact procedure to be used. If correlation with customer requirements is necessary, he must be capable of discussing the inspectin problem with the customer to arrive at a mutually acceptable method and standard of acceptance.

In the past, too much reliance has been placed on the ability and judgment of operators or inspectors who too often have been unequal to the task expected of them. General standards of acceptance and rejection applicable to large varieties of parts are impracticable.

Many plants, however, have found it of tremendous value to undertake and complete the often large task involved in setting up individual standards of procedure for magnetic particle inspection for each individual part as well as defining precisely what indications are acceptable and what are cause for rejection.

A final point of importance in the continuing development of magnetic particle inspection is the general recognition of the need for better interpretation and evaluation of the results obtained.

The concept that a defect is only a defect when it affects the usability of the particular part in which it occurs is one that has been slow in gaining general recognition. This is true as far as interpreting the results of magnetic particle inspection is concerned, and yet this concept seems so fundamental that it is difficult to understand why it was not recognized long ago.

With such an understanding of its proper application, the magnetic particle method can be expected to serve industry more effectively and maintain a permanent place as a method of test with a well established field of application.

In the foundry, magnetic particle inspection is used to examine castings for hot tears, shrinks, shrinkage porosity, thermal cracks, blowholes, cold shuts, and slag and sand inclusions. Unfused chaplets and chills also may be located.

The purposes and stages of production for which the method is used in the foundry may be broadly stated:

1. As an aid in developing molding and pouring technique.
2. As an aid in developing cleaning and foundry processing practices.
3. As an aid in production control.
4. As an aid in repair of defects.
5. As an aid in final inspection for defects.

These five foundry uses for magnetic particle inspection are discussed in the following:

1. As an aid in developing molding and pouring technique. The determination of the general quality of a casting of new design is rapidly made by magnetic particle inspection. Location and size of gates, risers, and chills, proper preparation of sand and cores, proper temperature of metal poured, all may affect casting quality and cause cracks, hot tears, shrinkage, etc. If detected in the first of a run of castings, these variables may be changed to eliminate defects before the pattern is put into production.

Co-operation Reduces Cost

In addition, important increase in yield is possible. In one foundry, sound castings being produced with a certain number and size of risers were poured with fewer and smaller risers until this method of inspection detected shrinkage. The risers were then increased in size slightly to give sufficient metal. The pounds poured compared with the weight of cleaned castings was decreased considerably, showing an important cost reduction through intelligent co-operation between the production superintendent and the chief inspector.

Thompson Vann and Dunlap McCauley[2] have stated that magnetic particle inspection is one of the more popular methods of inspection and perhaps the most effective in its application to a production line in a ferrous foundry. They say that their first use of magnetic particle inspection was to detect defects in cast steel tank bogie wheels. It was soon evident to them that casting procedures could be studied quickly and efficiently by this method, and adjustments made promptly.

Vann and McCauley state: "Magnetic particle inspection has proven a valuable tool in the authors' plant. All special products are either 100 per cent magnetically inspected or spot checked at frequent intervals. Magnetic particle, radiograph, macroetch, and pressure tests are all used in routine inspection . . .

"Cast steel tubing was produced and fabricated for stern tubes for cargo ships.

"Several defects due to incorrect casting procedure were found with the aid of magnetic inspection. Pouring temperatures and times were found to be of major importance in controlling the quality of these tubes.

"Magnetic inspection was used in the development of centrifugally cast fly wheels for tank motors. . . . In the casting, shrinkage running radially from the center towards the outside diameter was experienced. This was eliminated by changing the thickness to obtain directional solidification.

"Tiller brake quadrants for LSM craft, weighing about 900 pounds, are representative of static castings. Magnetic inspection was used in a study of the gates and risers and their effect on casting soundness."

2. As an aid in developing cleaning and foundry processing practices. In addition to molding and pouring a casting the foundry must shake out, clean, and frequently perform operations such as tumbling, sandblasting, grinding, chipping, annealing, welding, and other operations to condition properly the raw casting for acceptance and use by the customer.

Conditioning Operations Important

Many times a sound casting *as poured* is found defective at time of shipment due to improper performance for some of these conditioning operations.

Magnetic particle inspection is often used with much profit in preventing such losses.

As an example, inspection was made of some lots of several hundred castings passing through the operations of (1) gate and core removal, (2) sand blast, (3) grind rough edges, (4) anneal, (5) chip and polish. Twenty-seven per cent of the finished castings had been rejected by the customer because of cracks which were large and detrimental to the use of the casting.

These lots of castings were inspected after each individual operation. Cracks were found in a large percentage after the grinding operation, though these cracks were small—much smaller than those causing rejection by the customer. Due to rough handling and shipping these small cracks grew to appreciable size.

The important thing was that the source of that particular trouble was rapidly found. The grinding operation was corrected so as not to produce the small stress raisers and rejection dropped 95 per cent.

3. As an aid in production control. When production procedures have been proven practical it is still necessary to control that production to determine that those procedures are not changed to the detriment of the casting. Production men are interested in periodic inspection to insure that control.

This is not the place to go into detail on the statistical control methods but every foundryman should learn about them. They reduce drastically the amount of inspection required yet provide commercially accurate reliability for the entire production.

For example, it might be decided that five per cent defectives could be expected in a certain type of casting and the cost of such borne by the foundry. A proper number of random samples would be chosen from the production each day and inspected and a chart kept of the actual per cent defective. If the quality remained in control, that is, within the established limits, the control chart would show the uniformly high quality of the product. If it went out of control, 100 per cent inspection could be immediately started, the trouble found, and corrective measures taken.

Magnetic particle inspection is efficient for this use because of its flexibility and speed.

4. As an aid in the repair of defects. Magnetic particle inspection is valuable not only in the location of the defect but is at its best in controlling the repair procedure. The following routine is standard procedure in many foundries.

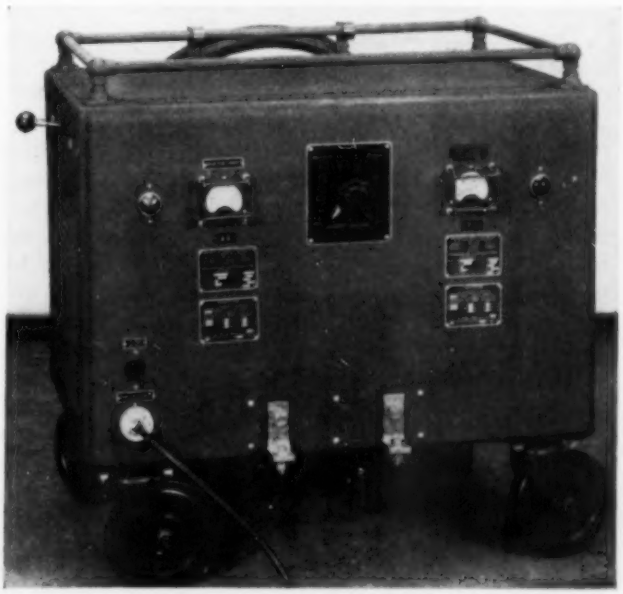
1. The defective area is chipped out with an air hammer or melted out by flame gouging.
2. The chipping is checked by the prod method to determine when the crack has been completely removed.
3. The repair is made by arc welding.
4. The weld is ground smooth.
5. The welded area is inspected with prods to check the soundness of the weld.

In Paul Ffield's[3] paper this subject is commented upon as follows:

"For sound repair welds it is necessary to remove all traces of cracks or other significant defects. Frequent magnetic powder check during excavation will insure this with a minimum loss of time and with the removal of a minimum amount of metal. Experience shows that it is more reliable than acid etching when excavating cracks, and, with an experienced operator, just as reliable when excavating shrinkage or sand."

5. As an aid in the final inspection for defects. It is not considered that it is within the scope of this paper to go into detail on this subject. Much work has been done and several excellent papers written on this gen-

Fig. 8—Portable magnetic particle testing unit providing AC and DC magnetization at operator's option.



eral application—its advantages, its limitations, the various methods, the necessity for experience and training of inspectors.

However, in the opinion of the author, two phases of the use of magnetic particle inspection which have previously been mentioned in the excellent papers quoted before bear repeating. Their importance for inclusion here is due to their effects upon the reduced cost of inspection as a whole.

The first is the use of production types of equipment mentioned earlier in this paper. In the inspection of small and medium sized castings produced in quantity, it is frequently true that the cost of handling the pieces, the magnetizing and applying of the particles takes appreciably more time than the inspection. Frequently these operations can be mechanized, and greater effort to do so will be well rewarded in lower costs.

Special Purpose Units

Several years ago a specially designed unit was made for the aircraft engine manufacturers for the purpose of inspecting cylinder barrels. Although most of them were made from forgings, the Ford Motor Co. was successful in making them from steel castings, and this type of unit was of considerable assistance in the determination of the proper amount of extra stock on the outside surface and the extra length necessary to pour a sound casting.

The unit has also been used for other cylindrical shaped castings in production. It magnetizes in both directions, and two men can completely inspect for defects in all directions at the average rate of around 100 pieces per hr.

Crankshafts which are frequently cast are large and heavy for manual handling and are made in quantities. A large magnetic particle inspection unit was built to perform all operations except the visual examination automatically. The shaft is placed on the conveyor chains and carried along by an intermittent hydraulic drive. Its first stop is under the hood on the left, where it is magnetized either in a coil or by passing current through the shaft, and at the same time the wet bath is applied to the casting.

Demagnetizing and Washing

The next stop is at the inspector's station where the shaft is examined. The carriers on the conveyor are rollers so that the inspector can rotate the shaft easily. The third stop carries the shaft into the hood at the right and it is there demagnetized by reducing and lowering the AC current flowing though the demagnetizing coil. The shaft then passes out of the demagnetizing coil through a spray to wash off the inspection bath and stops at the end of the unit for removal by overhead hoist.

The speed is adjustable, depending upon the inspection time necessary, but averages 60 per hr. for large truck engine shafts.

These two examples demonstrate the possibility of increasing the speed of inspection and greatly decreasing the amount of labor required. Also important is the fact that inspection keeps up with production enabling the production of defective parts to be stopped before a large backlog has been made.

The second point is that of the correlation of magnetic particle inspection with radiography so well expressed by Ffield as follows:

"A useful way of using magnetic powder inspection is to make it complement radiographic inspection. For example, if in the radiographic inspection of a series of identical castings it is found that some are defective in the same area, a standard often can be created for this location. Areas which have been radiographed are also inspected by magnetic powder.

"The powder pattern is compared with the radiographic image and the relationship between each method of test established as a standard for the area under consideration. Subsequent castings then may be magnetically inspected instead of radiographed in this area, at a considerable saving in time. An advantage of this form of inspection is that it enables the operator to gain experience in the evaluation of magnetic powder indications.

"The foregoing method is particularly useful when shrinkage must be located and evaluated. If shrinkage is present in a specified area, it usually will recur in exact locations and assume the same general contour in each succeeding casting, thus establishing the shape and location of the magnetic powder pattern.

"The method is useful, too, for other defects. For example, the design of a casting may be such that a certain area is prone to trap sand, the presence or freedom from which sometimes may be rapidly established by magnetic powder inpection after the reliability of the technique has been established for that area with the aid of radiography.

"In the case of castings which are being radiographically inspected, the foundry can save considerable time and unnecessary work if the surface defects are located by an 'all-over' magnetic powder inspection. The time consumed for such a test would be only a few minutes from start to finish, and the inspecting personnel is assured that most of the surface defects and many subsurface discontinuities have been located."

The proper use of the magnetic particle method along with other non-destructive test methods now used may well have an important effect on the design of metallic parts in machines in the future. Under the development of present day techniques the old factor of safety method of design is gradually giving way to more confident and effective utilization of a higher proportion of the strength of structural materials.

As is always the case, an improvement in tools is reflected in an improved product, but to derive full benefit from an improved tool requires skill and experience, which can only be obtained by a period of training and experiment. The past ten years has been such a period for the magnetic particle inspection method, and it is expected to be applied to future problems more intelligently than has often been the case in the past.

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DISCUSSION

Chairman: H. YOUNGKRANTZ, Apex Smelting Co., Chicago
Co-Chairman: H. C. STONE, Belle City Malleable Iron Co., Racine, Wis.

CHAIRMAN STONE: How does Magnaflux compare with X-ray for the determination of cracks. That is, are both equipments suitable for this purpose?

MR. THOMAS: Magnaflux is at its best in locating surface cracks. It is fast, effective and economical. For this purpose it is greatly superior to X-ray.

E. L. LAGRELIUS[1]: X-ray should be used only for sub-surface discontinuities. If you subject the castings to a good visual inspection, you will find the larger surface cracks. Occasionally you run into machining difficulties due to cracks in castings. These cracks would show up on radiographic film, and perhaps a little better or a little worse than in magnetic particle testing, but it is still a matter of economics. I cannot see using film when there is another tool that will disclose the defect more rapidly and cheaper.

CHAIRMAN STONE: In the production of malleable iron it is necessary for us to determine the presence of two types of cracks; first, the hard crack which occurs in the unannealed casting, and the other, a hot tear which occurs during solidification of the casting. Sometimes these hot tears are very small and are often termed checks. This is a very tight crack that visually cannot be seen, but after annealing and oxidation of the crack it is apparent to the eye.

We have attempted to determine this type of crack by X-ray, but we do not feel that there is any positive method of detecting this type of crack unless the line of X-ray happens to be in the exact line of the crack.

We have tried to use Magnaflux for this purpose and one of the problems that we have encountered is the false readings at surface irregularities; such as that caused in the junction of the core and the green sand surface. Any slight irregularity seems to give an indication and in the particular job that we were examining, this was the surface at which the hot tear occurred.

MR. THOMAS: Was it mechanical holding of particles due to roughness or irregularity of the surface? They should only be held magnetically. Little surface blemishes should not hold the powder particles but extreme surface roughness will hold them.

[1] American Steel Foundries, East Chicago, Ind.

CHAIRMAN STONE: That section size changed sharply at that point.

MR. THOMAS: Generally, then the magnetizing force was too great, because the magnetic field will leak across a sharp fillet or a sharp corner without a fillet. The leakage field in that corner may tend to hold the particles unless the magnetizing force is reduced to a low value.

CHAIRMAN STONE: Is there a technique that would offset that surface irregularity?

MR. THOMAS: Yes, if that was a powder indication due to a sharp corner. Proper current control will avoid it.

MR. LAGRELIUS: We have the same condition in steel castings that are magnetic particle tested, but we got around it by flowability of the powder and also reducing the current. We have the same condition in effect on thin sections of a sharp corner, where we have a certain amount of adhering sand, but it all deals with the flowability of your powder.

MR. THOMAS: That is right. If it will go into and collect in that area, then it is going to stick there. The excess can be removed by blowing it away.

MR. LAGRELIUS: You can use various percentages of red and gray powder to get a certain amount of flowability of powder, or you can reduce the current to minimize the stickiness. Is there any method whereby we can measure magnetic flux so that we can always have the same magnetic flux density for every casting?

MR. THOMAS: Unfortunately, no.

MR. LAGRELIUS: What is this General Electric meter?

MR. THOMAS: The General Electric meter works on uniform sections, but does not give reliable results on non-uniform sections, and that is where you need an accurate answer.

MR. LAGRELIUS: That would solve one of Mr. Stone's big problems. If he could have some method of ascertaining the magnetic flux in that area until he reached the point where he was only showing up the tight crack, as he calls it, then he could maintain that on all castings checked.

MR. THOMAS: On small castings, and where you have a change of section which causes an accumulation of powder, it should be easy to cut the current down in that area to the point where you do not get it. Or perhaps you could tip the casting so that the powder tends to fall away from that area instead of tending to pack down into it. It is more of a handling problem. I do not want to over-simplify it, but it seems that way to me.

PREPARATION OF FOUNDRY SANDS FOR MARKET

By
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SAND DEPOSITS are the disintegration products of various rocks and have been formed in a number of ways. They may be residual or transported greater or lesser distances by water, wind or ice. The composition, shape, size and uniformity of the sand grains within a given deposit are dependent on the origin and history of the deposit and these properties can be changed only within certain limits.

In developing a sand deposit, test holes and/or pits are sunk in some ordered manner, usually at the corners of a square grid. These test holes are plotted on a map of suitable scale as are test data obtained from laboratory analyses of the borings. From this map, cross sections through the deposit can be drawn, showing the location and the arrangement of the various types of sands and the overburden.

From the map and cross sections the amount of overburden and quantity of the grades of sand can be computed and, if the deposit warrants development, a suitable mining and processing method can be formulated.

Bonded and Bank Sands

The foundry industry's sand requirements can be classified as follows: bonded sands or those containing more than 5 per cent clay; semibonded (sometimes referred to as bank sands) containing up to 5 per cent clay; and washed sands.

Bonded and semi-bonded or bank sands are characterized by sand grains naturally bonded with clay. Many varieties are mined but the predominant components are silica grains and one or more clay minerals. These grades are usually found in the younger geologic formations. Those suitable for foundry use must contain the proper qualities of grain size, refractoriness, bond, permeability and durability.

Bonded and bank sands of foundry quality are not as widespread as the geologic origin would indicate. The preparation of bonded sands for market varies according to the geology of the deposit, the amount of deleterious material present, and the type or grade of sand desired, and can best be discussed by describing typical examples such as the Albany, N. Y., and southern New Jersey districts.

The Albany sands are located in a narrow belt along the Hudson River extending from the Adirondacks southward about one hundred miles into Dutchess County and are of glacial origin. The glaciers coming from the north eroded the areas they passed over and, by constant attrition, ground most of the rock fragments to sand, eventually mixing them with shales and clay gathered along their course.

As the glaciers melted, the water deposited the sediment over a large area. Subsequently, these deposits were subjected to weathering which resulted in the decomposition of the shale fragments to form the bond and alterations of the iron minerals to the hydrated form, giving the brown color to the sands. [1]

The amount of overburden or topsoil ranges from six to 18 in. and the sand deposits vary from 12 in. to four feet. As is typical of glacial deposits, the material is not uniform and only scattered deposits are suitable for foundry use.

The deposits that are suitable for foundry use are developed by stripping the overburden from a narrow section about six feet in width and the length of the particular deposit. These strips are usually cut parallel to a property line and, after mining of the sand, the overburden from the next section is removed and spread over the mined area and the field is left in suitable condition for farming.

After stripping, the sand is carefully examined and sampled. From laboratory test data, the sand is classified as to grade or grades. The sand is then mined, as shown in Fig. 1, under the supervision of an experienced foreman, loaded on trucks and hauled to the railroad siding.

Here, as Fig. 2 shows, it is put through a rotary screen which is mounted on a truck chassis and pow-

Fig. 1—Mining and loading Albany district sand.



ered by a gasoline motor. The screen discharges to an inclined belt which throws the sand into the railroad car or truck.

Albany sands are designated by number and range from 000, the finest, to 4, the coarsest. A tabulation of the number and equivalent average fineness is shown in Table 1. With such a large variety of sands more data, such as clay content, strength and permeability are required to specify a sand.

Although one deposit may contain several grades, many deposits must be developed so that any grade can be mined when required. Since these deposits are scattered over a large area, portable mining and processing equipment is the most practical.

The sands of southern New Jersey originated from the erosion of older formations—some are marine deposits while others are terrestrial.[2] The deposits are stratified and a formation can readily be traced. This

TABLE 1—DESIGNATION OF ALBANY SANDS BASED ON APPROXIMATE A.F.A. AVERAGE FINENESS NUMBERS

Grade	Mild	Medium	Strong	Extra Strong
000	.....	260	270	280
00	205	225	245	260
0	185	205	225	245
01	160	185	190	215
1	140	160	170	190
1½	120	140	150	170
2	100	115	125	150
2½	75	90	105	120
3	65	75	90	100
4	55	65	75	.....

does not mean, however, that the sand deposits are uniform, as numerous test holes must be sunk to find a deposit of suitable grade.

The overburden ranges from three to six feet and the deposits of sand range from eight to 60 feet in thickness, several grades being found in the same section. At Millville, N. J., the overburden is removed by scraper wagons and bulldozers. High banks of sand are mined with a one-yard shovel handling a 1¼ yard bucket. In thinner banks, or where greater selection is required, a ⅜ yard truck shovel is used.

The material is hauled by trucks from the pit to the processing plant where it is passed through a 10 x 25 ft rotary screen and discharged to a 450 ft belt conveyor, 300 ft of which is structurally supported 35 ft from the ground. The sand is then tripped off the conveyor into the proper storage pile. A 60-ft portable conveyor, Fig. 3, is used so that piles can be stored to either side of the main conveyor. Nine basic sands are mined in as many pits and are stored under or to the side of the conveyor.

From the drilling record and close observation by the foreman, lenses and pockets of off-grade material are deleted and separately removed and used for fill or dumped with the stripping. Samples are taken to the laboratory for analysis during the daily production and if the material is not up to specification it is removed from storage.

Despite such precautions, material dug directly from the bank is apt to vary as to strength and grain size.



Fig. 3—General view of screening plant and storage.

The storage piles effectively blend these variations, and when the material is loaded from these piles it is a homogeneous mixture whose characteristics have been predetermined by laboratory analyses.

One of the nine basic sands which range in average fineness from 30 to 110 and in clay content from 2 to 25 per cent will meet the specifications for many uses. In addition, by blending one or more of these basic sands with or without clay, a sand can be prepared which will meet practically any specification.

The blending of sands is accomplished by using 3-compartment, 110-ton bins from which batches are accurately weighed and fed to a 15-cu ft sand muller. This discharges onto a 60-ft portable conveyor which feeds the sand into cars or trucks (Fig. 4).

Washed Sands

By careful prospecting and mining, followed by screening and accurate blending, high grade molding sands are produced. However, the importance of maintaining experienced field foremen and constant laboratory analysis cannot be over-emphasized.

Washed sands derive their name from the process of preparation. The deposits suitable to production are those sufficiently large to warrant the expenditure for a plant. Such deposits must contain the proper sizes and composition of grain, and must be low enough in clay content to be readily washed clean.

Assuming that the deposit is satisfactory in all respects, the main problem relative to production is the design and layout of a plant which will produce grades that are currently required and yet be flexible

Fig. 2—Screening and loading Albany district sand.



to changing markets. The general practice is to mine hydraulically and classify the sand. This may be supplemented with screening in some instances. A typical example is the operation at Manumuskin, N. J.

This deposit averages 50 ft in depth, 40 ft of which is below water level, and contains over 98 per cent silica grains, approximately one per cent clay, and a small percentage of heavy minerals. The 2½ to 3 ft of overburden is removed by a Diesel engine powered tractor with a 5 cu yd scraper wagon.

Mining and Classifying

The mining is accomplished by a 6-in. centrifugal pump powered by a 100 hp electric motor and having an 8-in. discharge. The slurry, averaging 15 per cent solids by weight, is pumped about 1,000 ft to the plant at the rate of 1,200 to 1,500 gallons per min. or an average of 50 tons of sand per hr. It is then discharged into a storage box from which it flows into a revolving screen which scalps off the plus six mesh material which is discharged via a steel pipe from the plant.

The sand and water pass into a settling box of proper dimensions to settle plus 140 mesh grain. The overflow from this tank is passed back to a reservoir and the sand is drawn from the bottom and pumped to an overhead tank which feeds four classifiers. The classifiers, Fig. 5, use a water current of adjustable speed to produce a sand of the particular grade of fineness desired.

By regulating the feed and the classifiers, production of each grade is kept uniform. The first classifier produces a sand with an average fineness of 27, the second 35, the third 45, and the fourth 68. The discharge from the classifiers is pumped to dewatering towers and ground stored for drainage to about 3½ per cent moisture before shipment as damp sand. This operation is illustrated by Fig. 6.

Fig. 4—View of the sand muller and loading conveyor.



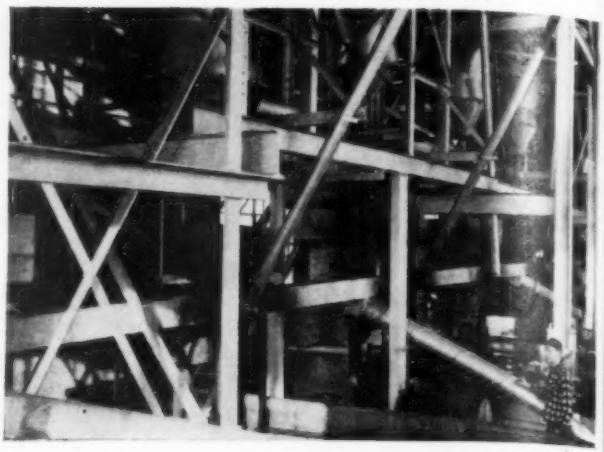


Fig. 5—View of sand washing and classifying plant.



Fig. 6—View of wash and dry plants and storage piles.

The discharge from the first classifier is sent to the dry plant where it passes through a 6 x 33 ft oil-fired drier which revolves at 7 rpm. The dry sand is discharged onto a screw conveyor to a bucket elevator which carries it up to a combination of double deck vibrating screens. These divide the sand into the following four sandblast sizes which are stored in four 800-ton concrete silos:

6 to 10 mesh or No. 3 sandblast sand
10 to 14 mesh or No. 2 sandblast sand
14 to 24 mesh or No. 1 sandblast sand
Minus 24 mesh or No. 0 sandblast sand

Any grade of sand produced by the wash plant can be passed through the drier and conveyed directly into cars, thus eliminating the necessity of dry storage of these grades.

The important factors in the production of uniform washed sands have been described and may be summarized as:

1. Thorough removal of overburden.
2. Control of feed to the classifiers.
3. Adjustment of classifiers.
4. Extensive use of screen analyses and other laboratory tests.

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FOUNDRY SAND LABORATORY

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DURING THE LAST DECADE the foundry industry has become more and more laboratory conscious. Because of this, smaller foundries are contemplating the installation of foundry sand laboratories for control and research purposes. It is the intent of this paper to outline briefly the organization of such a laboratory.

Strictly speaking, the foundry sand laboratory does not include the chemical laboratory, metallurgical laboratory or x-ray laboratory. These departments, which deal directly with semi-finished manufactured goods may be considered as "engineering inspection stations" for the foundry products.

For example, the chemical laboratory performs routine analysis on drillings or other types of samples secured from incoming ingot, remelted metal, and the outgoing casting; the metallurgical laboratory analyzes and controls grain size, alloying elements, type of heat treatment and other physical aspects of the final foundry castings; the x-ray laboratory devotes its time to the inspection of outgoing castings and the development of gating and risering techniques.

The foundry sand laboratory, on the other hand, usually works with expendable materials that are used during fabrication of the semifinished product. This laboratory controls and experiments with the sands, binders, mixtures and their treatments; the best method of baking, ramming and treating the cores for the castings; the use and application of different types of washes; the venting of the molds and other functions.

The foundry sand laboratory usually does not deal directly with the finished product. It only controls the modes and practices of manufacturing the product.

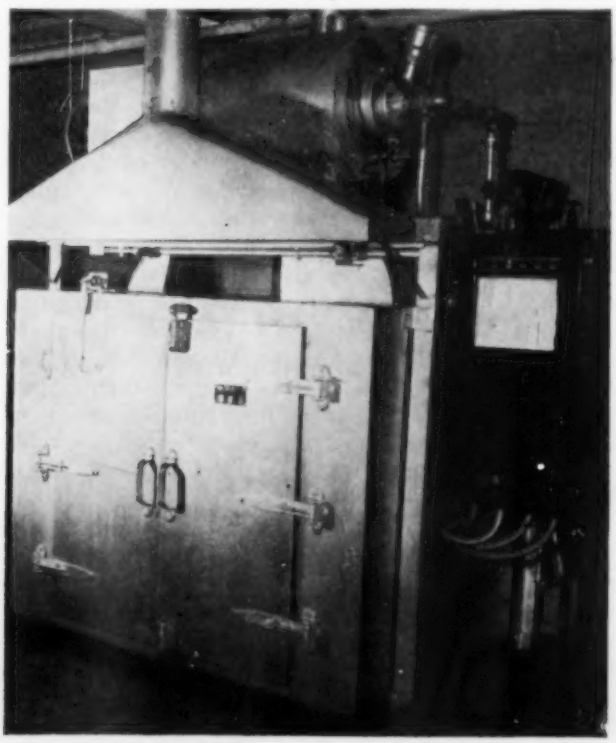
Why is a foundry sand laboratory necessary if its functions are only indirectly connected with the final casting? There are perhaps eleven significant points which must be considered:

1. *To control the uniformity of the product.* The reputation of any organization is based not only upon what it produces but also how uniform is its product. Uniformity together with inherently high quality can be obtained only through vigilant control. A few non-uniform castings in the hands of such meticulous cou-

Laboratory mixer used for experimental mixtures, a replica of the 3000-lb mixers employed in foundries.

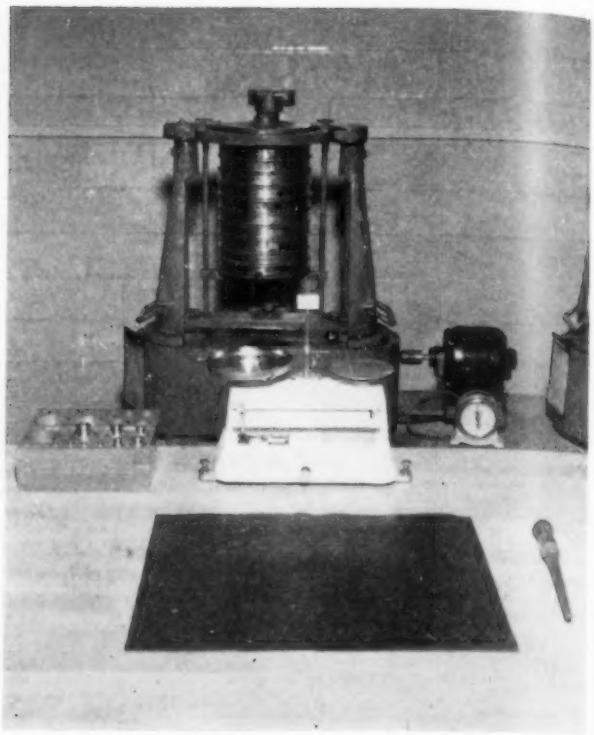


A gas-fired oven, equipped with automatic temperature recording apparatus, used to bake experimental cores.





Apparatus used in determination of moisture contents of sand mixtures by calcium-carbide "bomb" method.



Percentages of sand remaining on the mesh screens are determined by mechanical shaking in this machine.

sumers as the automotive or aircraft industries may easily spell doom for the small foundry.

2. *Control of manufacturing process.* In addition to controlling raw materials to achieve uniformity of a product it is necessary to control the method by which the product is made. Has there ever been a foundry in which scrap castings continue to recur despite the fact that "we have not changed a single thing?" This familiar phrase probably will continue to be quoted ad infinitum, but a foundry sand laboratory will aid in alleviating this condition.

3. *To improve products and practices.* Improvement means not only higher quality but a better method of producing the final product. The foundry sand laboratory has access to the newest methods of manufacture (through current technical literature) and should have the time to experiment with them.

4. *To control the expendable molding materials.* Although the chemical and metallurgical laboratories control the incoming materials which finally find their way into the finished casting (i.e., the metal itself), the foundry sand laboratory accepts or rejects such materials as sand, core oil, cereal binder, bentonite and other clays, pastes, silica flour, core washes, graphite, talc, plumbago, inhibitors, caulking compounds, partings, ladle washes, seacoal, blacking, etc.

All of these ingredients are used in manufacturing castings, yet not one of them leaves the foundry. Chemical analyses may be performed on these materials by the chemical laboratory but the foundry sand laboratory generally performs experimental and production tests with them.

5. *To investigate new materials.* The foundry sand laboratory, having access to the latest engineering data and technical publications, is in an excellent position to investigate new types of foundry materials. Samples of new materials should be forwarded to the sand laboratory for their examination before such products are placed in production.

6. *To aid management in solving certain production problems.* The foundry sand laboratory should be so well acquainted with its work that such typical problems as the following can readily be answered: Can the core baking cycle be speeded up in order to increase production without deleterious effect? Has the recent epidemic of hot cracks been caused by a non-collapsible sand mixture? Was the last shipment of silica flour of uniform quality?

What core sand mixture shall we use on the new lot of complicated castings just ordered? Shall we adjust the moisture content of our molding sand now that we are in a humid season? All of these questions and many others should be discussed with the foundry sand laboratory with the view in mind of aiding management in solving their problems.

7. *To perform engineering calculations relating to foundry practices.* The personnel of the foundry sand laboratory should be well acquainted with simple calculations so that the core room foreman having, for instance, "only 932 lb. of bank sand left" will be able to go to the laboratory and find out the exact mixture. This saves him the trouble of converting 10 pt. of oil per 1000 lb. of sand to X pints per 932 lb. of sand. Such terms as "Baume degrees" with reference to core washes, saponification number of core oils, special inhibitors such as ammonium-acid-fluoride, and other scientific terms will be readily explained in a laboratory.

8. *To maintain records of past performances.* Most

foundrymen believe that troubles occur cyclically and for this reason, if for no other, the records of past performances are extremely important. The sand mixtures "used last June when we were not getting cracks in our castings" can be important data next June when the castings crack at knockout. What kind of binder were we using two years ago when we could not get enough cereal? Have we raised the temperature of the core baking ovens within the last year? When did we lower the moisture content of the molding sand? All of these records may prove useful sometime and save the foundry thousands of dollars.

9. *To correlate data.* The wealth of figures conveniently inserted on data sheets are meaningless to foundry production unless these data are properly interpreted and correlated. The foundry practice unit will be able to aid production in correlating these data, in improving practices and writing procedures. Since most of the results obtained by foundry testing instruments are particularly valuable for comparison purposes, interpretation and correlation of these results is most important. A sand with a permeability number of 40 may be ideal in one foundry and useless in the next. The foundry laboratory will aid in the interpretation and proper use of such information.

10. *To perform routine analysis.* This is one of the main functions of the sand laboratory. Many a laboratory is merely a routine data assembly line where figures are accumulated and placed on sheets of paper and filed. All results must be analyzed to advise management exactly of conditions in the foundry at all times.

11. *To aid organization prestige and provide psychological control.* A foundry sand laboratory inherently increases the prestige of an organization in the eyes of management, the workers had competitors. The mere presence of a laboratory in a foundry will keep the workers alert.

These eleven reasons should provide present foundry management and potential foundry organizers with ample justification for the installation of a laboratory. They briefly outline the functions of a sand laboratory and how it may be used to aid in supervising foundry operations.

Every foundry can benefit from a laboratory. However, there are five specific instances when a laboratory is especially necessary or advisable.

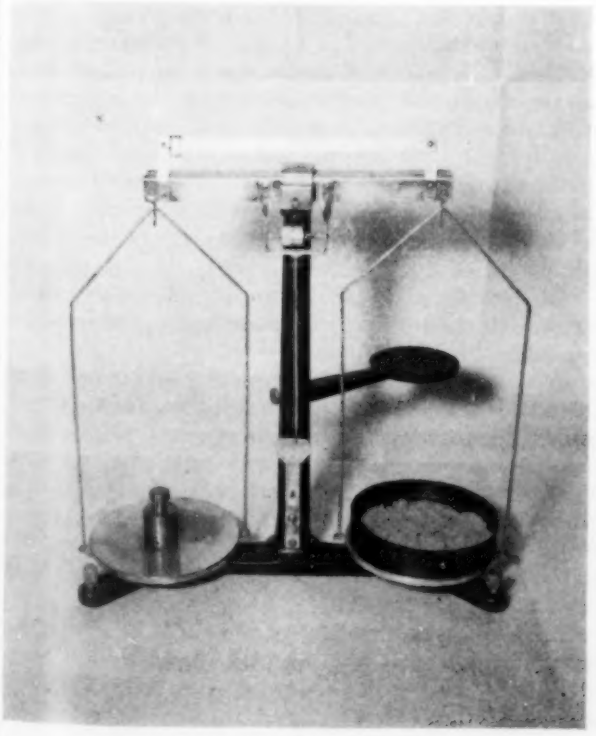
1. *Management is cognizant of poor control with attendant scrap castings and loss of money.* If management realizes the importance of a laboratory, and is fully cognizant of the monetary gain afforded by routine control, a laboratory should be started at once.

2. *Competition is too keen.* The foundry without a laboratory is at a great disadvantage in utilizing new products as soon as they become available. Even though induced to try a new product, an unbiased opinion can be gained only from within the organization and the sand laboratory is the place to get it.

3. *A new financial appropriation with attendant expansion is near at hand.* Although the latest in mechanization is always foremost in the manufacturer's mind, a new plant should incorporate the latest design for a foundry sand laboratory as well.

4. *Influx of new business.* The sudden influx of new business necessitating radical changes in foundry practices should call for the installation of a foundry sand laboratory. If the laboratory technician is able to work with the production management in setting up and designing methods for making the new products, he

Balance used to weigh sand for Dietert moisture test. Pan on the right has 100 holes per sq. in. on bottom.

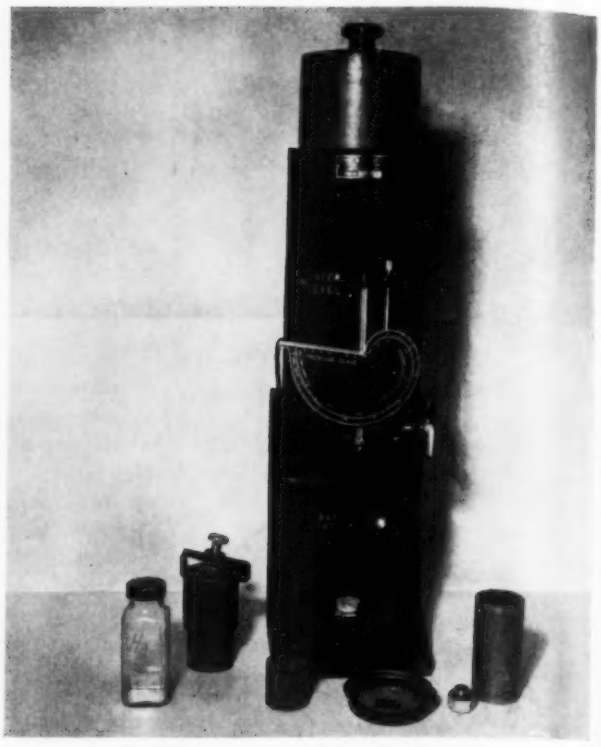


Tensile specimen rammer used to form briquettes in conducting the tensile strength tests on sand mixtures.





Clay analysis tester used in determining if percentages of clay in raw, crude sands meet plant specifications.



The permeability meter is a device used for measuring the amount of "pore space" between the sand grains.

will be in a much better position to aid management in solving future production problems.

5. *Management does not have time to experiment.* Because of other duties and pressure to get castings to the consumer, much recent technical information is unused. Management should realize this and take immediate steps to use the information by installing a foundry sand laboratory. This unit must have time outside of its routine duties to perform the necessary assimilation, interpretation and experimentation with all of the latest applicable technical data.

The question may arise as to how to start a laboratory in a foundry. In the first place, the originator of the idea must feel the necessity for it himself sincerely and should not just be trying to get another department under his control (with attendant extra prestige).

If the reasons for a laboratory given in the first part of this paper do not essentially fit into his case, the whole subject should be dropped and the foundry will continue to produce castings in the same manner it has during the past years. However, should more efficiency be the goal of the executives, it should be a fairly easy matter to explain to them how they can make still more money through the installation of a foundry sand laboratory.

Use can be made of the points brought out in the first part of this paper, with the explanation, of course, that less scrap will be produced, that new methods and materials will be discovered to make a higher quality casting and that the laboratory will make a valuable addition to the company as a whole. The mere mention of these points may not be enough and the organizer of the laboratory may need to keep insisting, reminding and selling his objectives before anything will be done.

For a man to run the laboratory, look first within your own organization. Although the laboratory may be non-productive in the accounting sense of the word, it will pay management back many times if it is correctly organized and operated.

Preferably, the supervisor of the laboratory should be a graduate engineer with foundry experience. He should be able to interpret results and be able to put them into good oral and written form.

He should be assisted by a practical young technician, perhaps drawn from the core room or melting floor, who is not bored by routine work and who is interested in foundry practice. A young girl sometimes meets these latter qualifications admirably but it is probably better to secure an energetic, imaginative young man (22-35) who can get along well with management and labor and who is able to learn to operate and maintain scientific instruments.

The foundry sand laboratory should not be isolated in some office building away from the foundry itself. On the other hand, it should not be placed in surroundings not conducive to concentration. The supervisor of the laboratory and his technician must have access to every part of the foundry and be able to appear at the site of trouble at a moment's notice. The laboratory will have to be equipped with hot and cold water, gas, compressed air, and electricity. Utilities should be kept in mind when planning the site for the laboratory. Air conditioning makes the laboratory a better place in which to work but usually it is not required.

The type of laboratory equipment needed depends upon the type of foundry which is being serviced. However, certain pieces of apparatus are essential regardless of the metal cast in the foundry. Listed below are the

various pieces of equipment which should be in every sand laboratory if it is to function properly.

1. *A core baking oven.* If any cores at all are being made in the foundry, this piece of apparatus is absolutely essential. It is probably one of the most expensive pieces of equipment which will be purchased and much care should be taken in buying it. It is best to simulate foundry conditions when purchasing laboratory equipment, therefore the core baking oven should be the same type as is used in the foundry.

Naturally, it should be small enough so that it can conveniently fit into a laboratory yet large enough to bake at least 100 standard tensile cores uniformly. It may be either gas-fired or electrically heated and should be provided with an automatic temperature controller, preferably of the recording type. It should have a maximum working temperature of 550 F, be well insulated, and the temperature uniform throughout the oven.

2. *Sand mixer or muller.* This piece of apparatus should simulate production conditions. There are several excellent laboratory mixers on the market from which to choose. The laboratory mixer should have a capacity of not less than 25 lb of sand so that enough specimens may be made from each mixture.

3. *Mechanical sieve shaker.* Although many foundries perform their screen analyses by hand sifting, duplication of results is extremely tedious unless a mechanical shaker is used. A shaker with an automatic timing device regulating the time of sifting is advisable. The nest of screens should include those from U. S. Series No. 20 through 270 and Pan, inclusive.

4. *Universal sand testing machine.* Most of the laboratory results will be procured through the use of this instrument and it is better to have one which is electrically driven than one manually motivated.

5. *Sand rammers (two).* The sand rammers made according to American Foundrymen's Association's standard are another piece of fundamental apparatus. It is best to have two of these devices in order to eliminate the time consumed in changing from one type of plunger to another.

The two main types of specimens used in sand testing are the cylindrical compression specimen and the hourglass-shaped tensile specimen. Although the plungers are interchangeable on the sand rammers, it is advisable to attach the tensile plunger to one, leaving the other rammer for the compression specimen.

6. *Permeability meter.* The instrument and accessories for measuring permeability are essential in the foundry laboratory.

7. *Tensile strength core accessory.* This apparatus includes all the necessary parts for making tensile strength cores; should be purchased with sand rammer.

8. *Moisture tester.* The moisture content of molding and core sand mixtures is of utmost importance and the rapid moisture testers now on the market provide an easy means of obtaining these data. Of course, small quantities of the sand mixtures may be weighed, dried and reweighed but the time consumed and the extra handling of the sand makes it worthwhile to purchase a rapid moisture tester.

9. *Compression instrument.* If a great deal of work is going to be done in the measuring of green strength of core sand mixtures, the use of a Saeger-type compression tester is advised. Although green core sand may be tested for compression values on the universal sand strength machine, it is much easier and more accurate to determine the green compression strength with a spring-type tester. Molding sand green compressive values are satisfactorily obtained on the sand strength machine.

10. *Rapid sand washer.* This piece of apparatus may be purchased or made in the shop.

11. *Core hardness tester.* This small gauge is indispensable for the foundry laboratory and for testing production cores.

12. *A laboratory drying oven (maximum temperature 350 F).* A small oven of this nature is often used in the foundry laboratory for drying sands prior to testing and for evaporating the moisture from cereal binders, clays, etc.

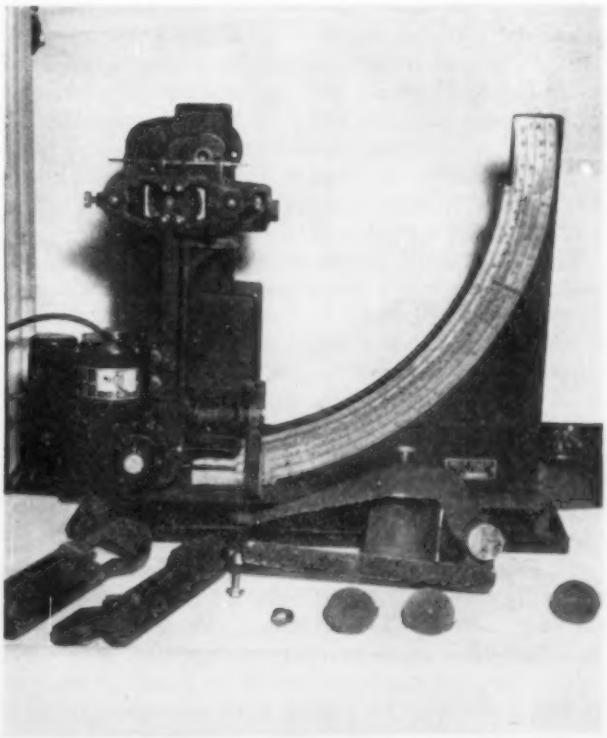
13. *A torsion balance and weights.* The necessity for an accurate, rapid means of weighing sands and binders for testing is obvious. The torsion balance is used for performing screen analyses and for weighing binders when making experimental mixtures.

14. *Muffle furnace.* This piece of equipment is usually found in one of the other laboratories in the plant and may not have to be purchased specifically for the sand laboratory. However, its main use is in igniting sands and sometimes in studying them under high temperature conditions when a dilatometer is not available. A large capacity heavy duty muffle furnace is advisable.

15. *Scale.* Some means must be provided for weighing comparatively large quantities (25 lb) of sand. An accurate platform scale meets these requirements.

The instruments on this list should not cost more than 1500 dollars and are essential for the proper functioning of a laboratory. Other pieces of equipment such

A universal sand testing machine for testing the various strengths of all rammed sand mixture specimens.





Compression tester used to test unbaked sand in compression—an important factor in holding sand in mold during assembly of cores in practical foundry operation.

as sand samplers, mold hardness testers, dry core permeability tubes, transverse strength core accessories, gas determinators, flowability indicators, sinter meters, dilatometers, etc. may be purchased as the need arises. A small supply of laboratory glassware should be obtained, although most of the work is done with the core and molding sands themselves.

In the arrangement of the laboratory, some thought should be given to the spacing of the equipment. A sand bin may be constructed in one corner of the laboratory under which the platform scale will fit. Next to the bin a sink with hot and cold running water should be placed. The mixer should be near the sink and a table containing the torsion balance and weights is best situated just far enough away from the mixer so that it will not be subjected to undue vibration.

The mixer should be placed high enough off the floor so that the discharge spout will not interfere with the insertion of a pail under it. A concerete foundation (¾ to 1 ft high) with bolts set in is admirably suited for this purpose. An electrical outlet (preferably 220 volts) should be provided for the mixer. It is best to have a grounded electrical plug at or near the sink to facilitate safe clay analyses. To save steps, it is well to have the sand rammers mounted near the sand mixer on firm concrete pedestals approximately 3 ft high.

A preparation table should be situated nearby for the compression instrument, moisture tester and permeability meter. This table should be equipped with gas and electricity as well as air. Compressed air is a useful cleaning medium if properly applied.

The core baking oven should be raised about 4 ft so that cores may be placed in it conveniently. It is advisable not to have the baking oven too close to the sand mixer since the heat from the oven may dry out the sand during mixing. If the oven is of the gas-fired type, it will be necessary to provide an outlet for the fan or blower.

A large table for the universal tensile strength and sand testing machines, drying oven, and muffle furnace should provide enough room for tabulation of data and storing of cores before testing. It is well to have at least two large desiccators on this table for the storage of specimens before testing.

The automatic sieve shaker may be placed anywhere in the room, but preferably located near the scale and weighing table. If a ro-tap machine is used, a foundation must also be provided for this piece of equipment otherwise the vibration will be transferred to other pieces of apparatus.

Once the equipment is in the laboratory, it is necessary to become familiar with the instruments. This means learning the calibration of the various devices, the method of making them function most proficiently, and a general knowledge of the working parts. Once this has been accomplished, duplication of individual results is more likely.

Many of the variables in sand research and control have been standardized or eliminated by the American Foundrymen's Association. However, others still exist and these are a problem for individual laboratories. The core baking oven must be investigated. Cores from the same type of mixture must be rammed and baked in the oven in different times and temperatures in order to be assured of complete uniformity. Tests like this are apt to become tedious but are absolutely necessary so that future results may be properly interpreted.

Slight adjustments such as air intake, type of flame in the case of a gas burner, rheostat adjustment in the case of an electric oven, are of the utmost importance and must be carefully controlled and standardized.

Filing Methods

The materials to be used in the laboratory must be studied. Complete knowledge of the sands, binders, washes, etc. is imperative. While the testing of this nature is being accomplished, at the same time the foundry is being aided through the compilation of important data.

A method of keeping and filing data must be set up. The organization of a filing system for foundry sand data is complicated and much study should be devoted to it in order to find out which type will best suit your needs. Probably a cross reference system is the best so that one may find, for example, "bentonite" not only under "clay binders" but also under "synthetic molding sands" and "mixtures."

In the last analysis, the best thing to do is to standardize within the laboratory before advising production supervisors what is wrong with their procedures. The duplication of results in the laboratory is imperative and requires a great deal of tedious work before accomplishment. Naturally, one has to use imagination or good common sense in the interpretation of the results.

For instance, in breaking a tensile specimen, the result need not be 34.5 psi for every specimen made in a mixture but if the results should vary between 20.1 and 56.3 psi, a repetition of the test is necessary.

DISCUSSION

Chairman: G. R. GARDNER, Aluminum Co. of America, Cleveland

Co-Chairman: K. J. JACOBSON, Griffin Wheel Co., Chicago

DR. H. RIES[1]: What are your thoughts on the green tensile test?

MR. MYERS: Since the green tensile test can be correlated directly with core sand stickiness, it should be investigated more thoroughly. Unfortunately at the present time there is not a delicate enough method available for testing the green tensile strength of a core sand mixture. In the Werner G. Smith Company foundry research laboratory we are experimenting with a simple apparatus for determining the green tensile strength of a standard unbaked tensile strength briquette.

[1] Cornell University, Ithaca, N. Y.

We know that green compression strength is an indication of "stickiness" since core sand mixtures showing values above 1.0 psi. do not stick to the pattern. Since most core sand mixtures have much lower green compression values than 1.0 psi. we need a more delicate instrument to measure them.

DR. RIES: Have you been using it for core mixtures?

MR. MYERS: Yes.

DR. RIES: I have had a few foundrymen tell me that they thought green tensile tests gave them more information than the green compression. What are your thoughts on the green shear test?

MR. MYERS: No green shear tests were made by us.

DR. RIES: I visited foundries using both the green compression and green shear, and I asked them why they made both tests. They thought at times the green shear might show something which the green compression test did not.

DETERMINATION OF MOLTEN METAL TEMPERATURES

G. Vennerholm and L. C. Tate

Ford Motor Company, Dearborn, Michigan

THE ABILITY TO MEASURE accurately the temperature of the liquid metal in the furnace has for many years been the goal of metallurgists in both the steel mill and the foundry.

When we consider that the whole refining process in steel making is based on a series of chemical reactions which are largely a function of the temperature, and that the degree of solubility of the various gases present such as oxygen, nitrogen, hydrogen, etc., is also governed thereby, the vital importance of this subject is evident.

Many of us who have, furthermore, labored in the foundry with the problems arising from lack of proper temperature control such as dripping roofs, short lining life, not to mention runouts, burn-ins, misruns and ladle skulls will readily appreciate that accurate knowledge of the temperature at all stages of the manufacturing process will make the task of uniformly reproducing quality castings from heat to heat somewhat easier.

It is the intention of this paper to review some of the various methods tried and also to discuss the results of an investigation carried out at the Ford Motor Co.

It may be well before we proceed with our discussion to consider the requirements which must be met by instruments for this type of work in order that the greatest benefit may be secured and the expenditure justified.

Accuracy of Instrument

The instrument must be of such accuracy that the temperature readings obtained do not vary more than $\pm 10°$ at about 2900° F. Otherwise there is little reason for adopting instruments, in particular when we consider the quite remarkable ability of trained melters in judging temperature by the eye.

With the conditions usually existing on the melting floor, it is, furthermore, necessary that the instrument be sufficiently rugged in its construction to withstand the rather rough treatment to which it will be exposed. It is also of importance that it be readily portable so





Photographs on this and opposite page illustrate operation of immersion unit. The whole operation requires about 12 sec. (see Fig. 9 for schematic diagram).

that it can be rapidly transported from one furnace to another or moved out of the way when charging.

The operation must also be simple enough so that specially trained personnel will not be required as this would be objectionable, particularly in smaller foundries. In order to secure maximum benefit as well as greatest ease in evaluating the temperature readings, the instrument must be self-recording so that the melter can use the curve produced as an indication of the physical conditions in his furnace at any given time.

Temperature Determination Methods

It is also well to remember that the steel melter, whether in the foundry or in the mill, will never accept an instrument of any type unless its operation and the information obtained convince him that it is to his benefit to use it. It must help to reduce the mysteries of steel making to a science.

Spoon Test Method. The spoon test, where a sample of the metal is removed from the furnace in a spoon previously coated with slag, and the temperature judged either by the eye or read with an optical pyrometer, is probably most common today. The accuracy, however, of this method even when the optical pyrometer is used leaves much to be desired because of the rapid loss of temperature in the spoon, difference in vision, changes in the emissivity of the metal, etc. Furthermore, it seems that most melters prefer to use their own judgment, when they do not agree with the pyrometer man.

Rod Cutting Method. Another method commonly used, particularly in the open hearth, is the rod cutting method where a steel bar is inserted through the wicket hole and submerged in the liquid bath for a given length of time. Upon withdrawal, the temperature of the bath is indicated by the shape of the hot end, high temperatures resulting in a clean straight cut while low temperatures are revealed by a cone shape.

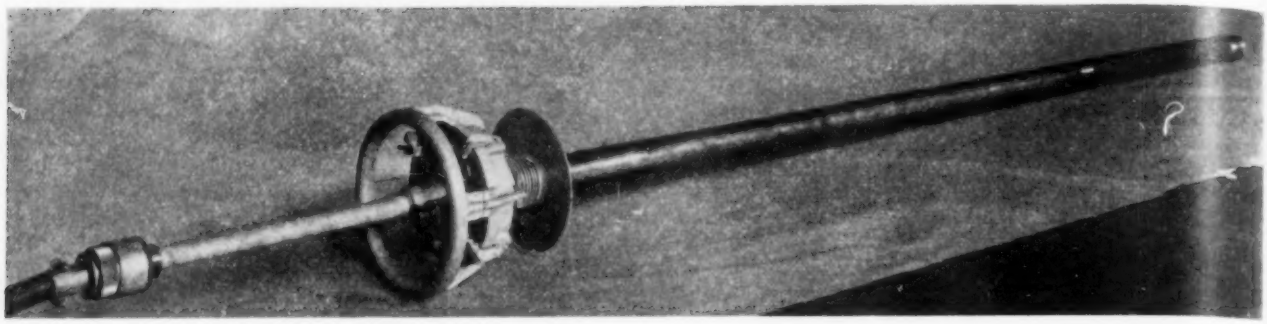
Bath Equalization Method. The bath equalization method, developed by Larsen and Shenk, is based on radiative equilibrium between the roof and the slag surface of the metal. It consists of a photoelectric roof pyrometer which is sighted on the inner surface of the roof and another pyrometer which is sighted downward on the slag. In order to take a reading, the power or the fuel is shut off to permit the metal, slag and roof temperatures to equalize. The equalization at these temperatures is accomplished in less than a minute's time. The point at which the roof temperature and the slag temperature coincide is then used to represent the bath temperature.

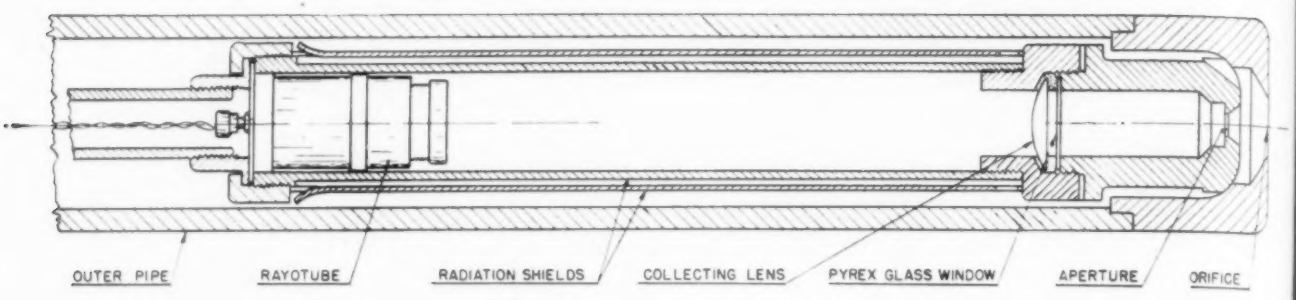
The disadvantage of a method of this type is, however, that the metal itself is not measured and, therefore, considerable errors are apt to result. In addition, the furnace operation must be interrupted at the time of each reading.

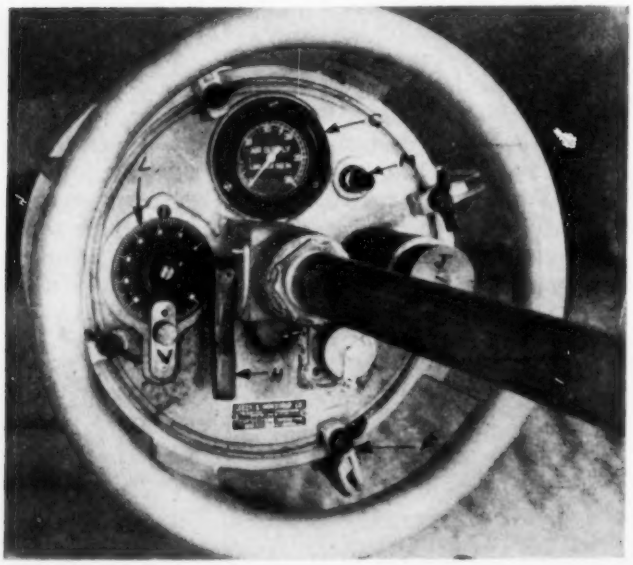
Graphite-Silicon Carbide Method. A graphite-silicon carbide thermocouple first announced in 1933 by G. R. Fitterer, has met with some success according to published data. In principle this instrument is a thermocouple formed by a graphite tube sealed at one end into which is inserted a silicon carbide rod. This is insulated from the graphite through a surrounding air space, except at the sealed end where the two are joined thereby forming the hot junction. To the assembly is attached a graphite extension tube which is mounted in a water-cooled head containing the terminals connected through wires with a potentiometer.

Dissolving Tube Method. The thermocouple principle has formed the basis for several additional methods such as the dissolving tube method which consists of a platinum-platinum-rhodium thermocouple protected by a small porcelain tube which in turn is enclosed in a graphite tube. This graphite tube is set in a water cooled head and the thermocouple leads are led out through a pipe mounted inside one of the two coolant conductors which also act as handling extensions.

The graphite tube is tapered at the end to about one-





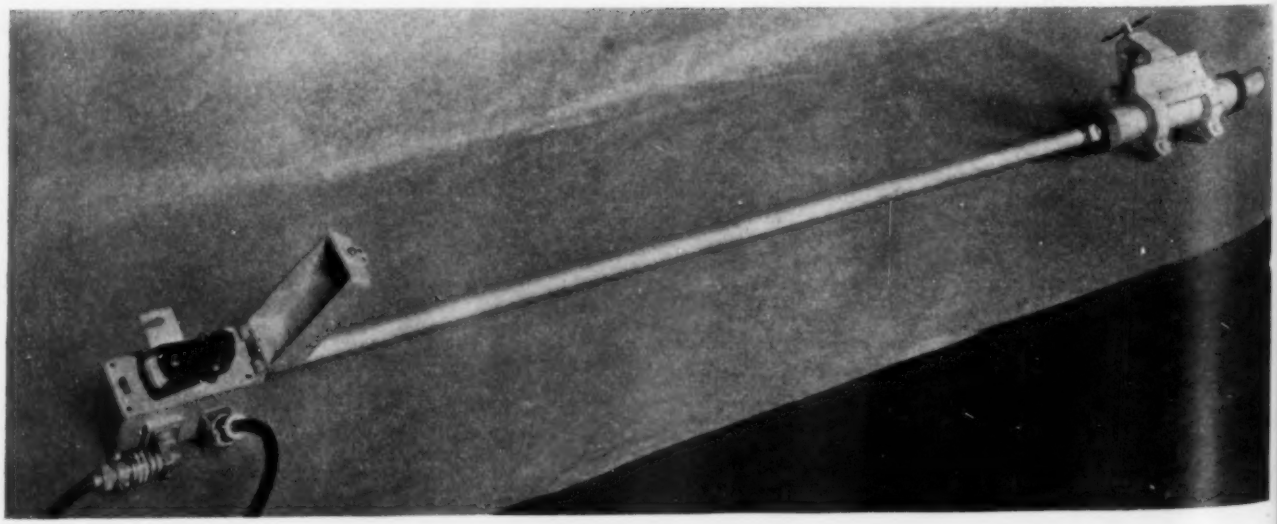


Figs. 1 and 2 (above)—Immersion unit and sectional drawing, radiation tube assembly. Fig. 3 (left)—Control head unit. Fig. 4 (below)—Standardizing unit.

half inch thickness and acts as a protection for the porcelain tube until the same has passed through the slag and into the liquid bath where the graphite burns off exposing the porcelain. The temperature readings are obtained by connecting the leads to a suitable potentiometer. Although in principle the same as a modern version to be described later, this particular method has not proven entirely satisfactory. This is mainly because of the water cooling principle used which frequently does not meet with required safety codes.

Numerous additional instruments have been tried with varying degrees of success but their importance does not warrant discussion.

Instruments Used in the Present Investigation. Realizing the great need for an accurate method for measuring the temperature of the liquid bath, the authors'



company about a year ago made a survey of the various instruments available and selected two, a radiation type pyrometer and an immersion type thermocouple which seemed to offer considerable promise.

Radiation Method. The radiation method was originally developed by F. L. Collins and Carl Oseland in 1936, and later further improved by other investigations. It consists of a 6-ft. length of 2-in. steel pipe provided with a glass window at one end and a steel plug at the other end having an orifice of about one-half inch diameter. The tube itself was connected with a high pressure air line.

By submerging the orifice end of the tube into the metal bath and forcing a stream of air through the opening, a bubble is formed in the metal. Temperature readings are obtained by means of an optical pyrometer sighted through the tube on the inside wall of the air cavity formed at the orifice.

Although this method was found to give consistent readings when operated by an experienced observer, several serious objections were present. The cavity formed in the liquid metal, not being a true black body, i.e., a perfect radiator, necessitated corrections to the pyrometer readings.

The difficulty, furthermore, of having to locate the small orifice and align it with the filament of the pyrometer in the short space of time permissible before the tube softened sufficiently to start bending, was a serious handicap. Also, no automatic record of the temperature could be obtained.

The principle of the Collins-Oseland method, however, forms the basis for a self-recording radiation pyrometer which was selected as one of the instruments to be investigated.

The difficulties encountered by Collins-Oseland have in this instrument been overcome by mounting a thermopile with a suitable optical system, known as a radiation tube, close to the orifice and connecting the same with a self-recording instrument. This instrument eliminates the optical pyrometer and the difficulties connected therewith, and also makes it possible to record automatically all readings.

This instrument in its present form consists of two essential parts: (1) the immersion unit, frequently called the "dunker," containing the photo-electric cell, and (2) the self-recording equipment.

The immersion unit as shown in Fig. 1 is built up of three sections welded together. These consist of a tip which is machined to form a $\frac{5}{8}$-in. orifice and also acts as a locator for the aperture, a 2-ft. length of heavy pipe which houses the radiation tube assembly and protects it from the slag and molten metal, and a 6-ft. length of standard pipe to which the control head is attached.

The radiation tube assembly, shown in Fig. 2, con-

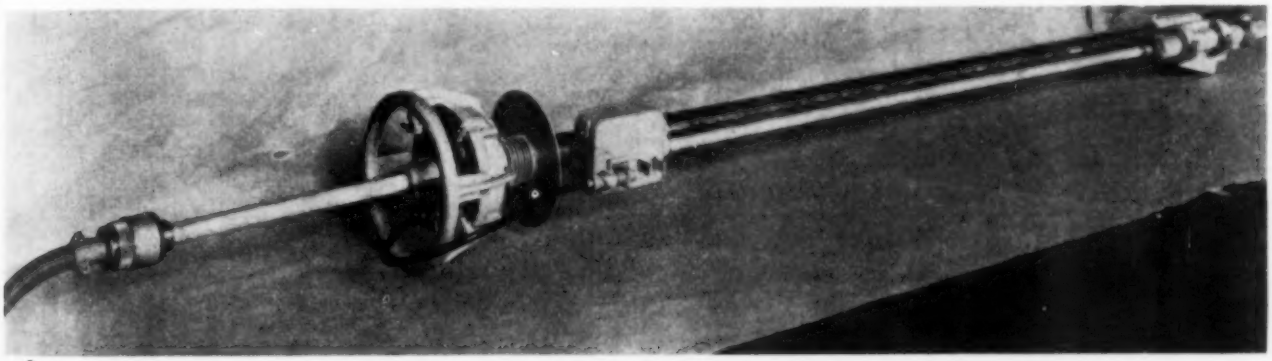
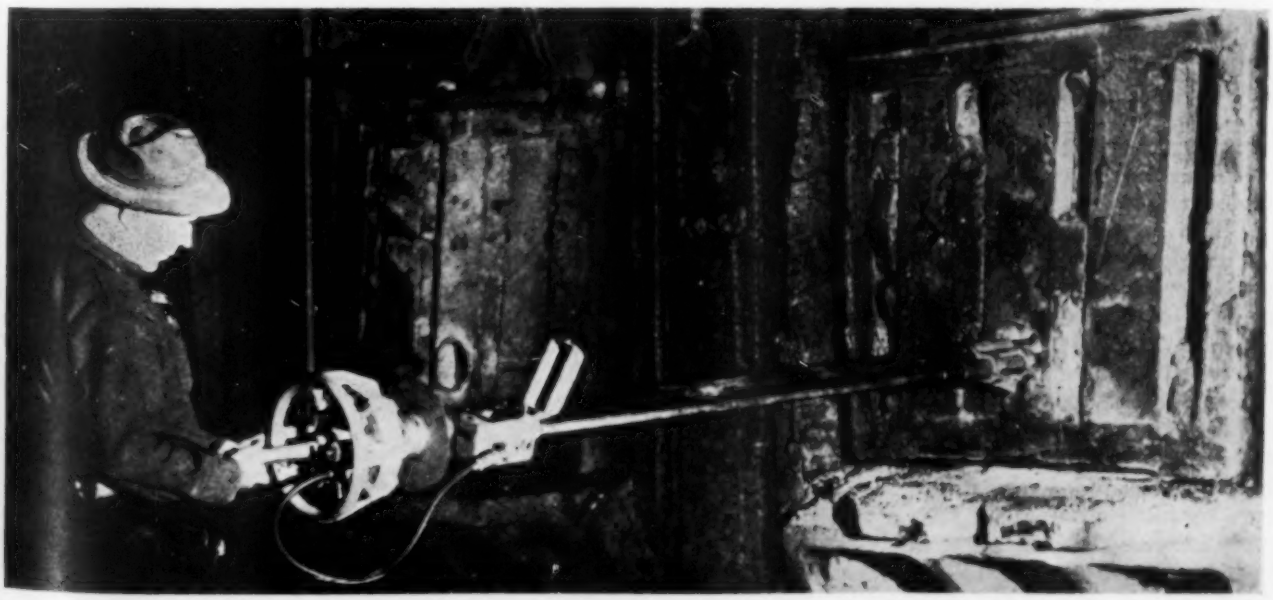


Fig. 5 (above)—Standardizing unit attached to immersion unit. Fig. 6 (below)—The standardizing operation.



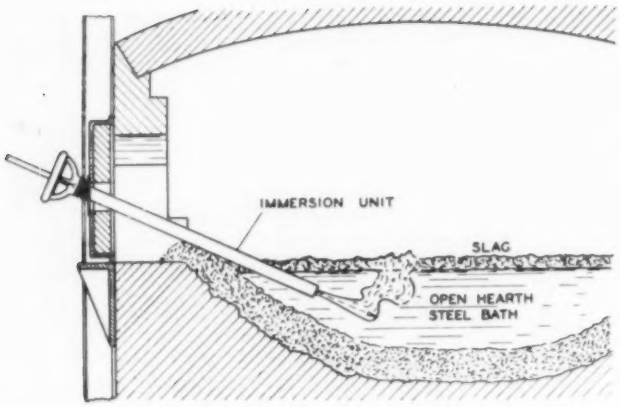
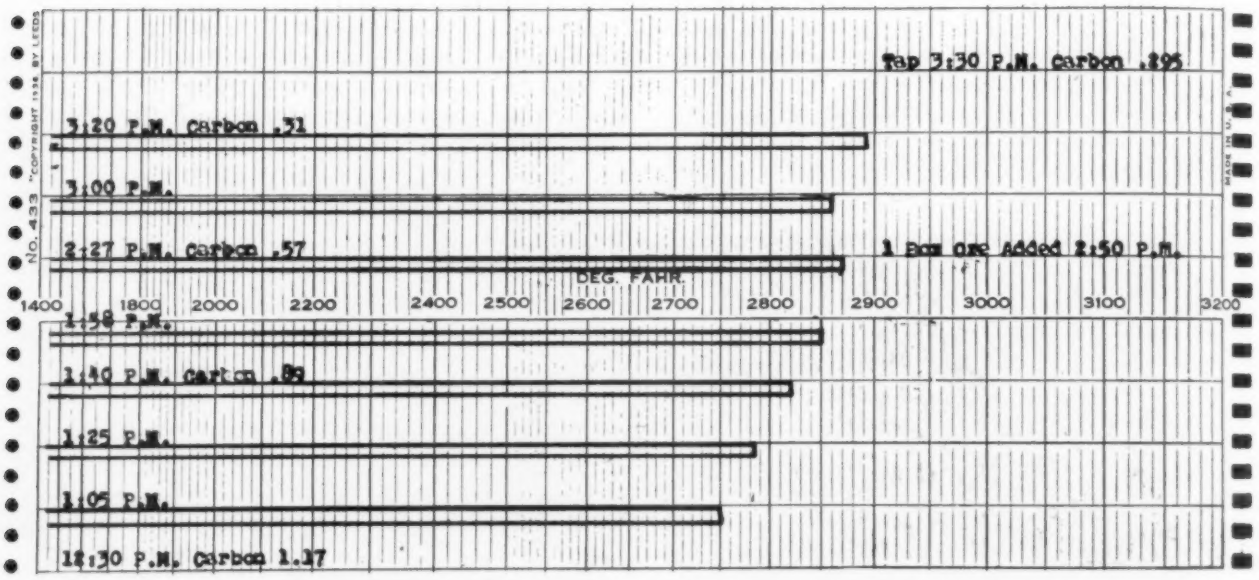


Fig. 7 (above)—Schematic diagram demonstrating operation of the immersion unit in furnace application.

Fig. 8 (below)—A representative temperature chart.



readings are to be obtained, and varies with the depth of immersion. It has been our experience that most satisfactory results are obtained with a pressure of 12 psi. and with the tube immersed approximately 6 in. into the liquid metal.

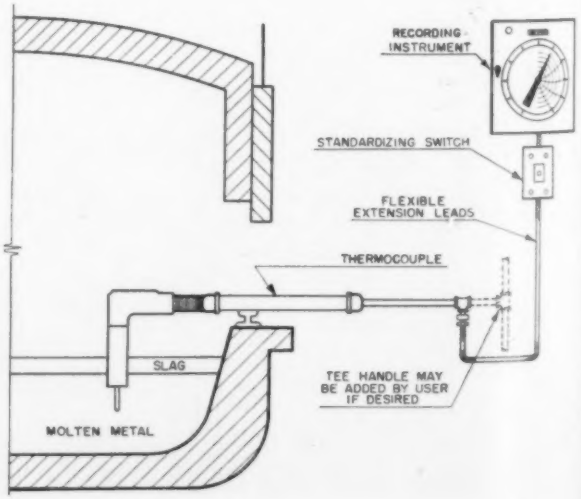
To assure accuracy, it is advisable to standardize the instrument once a day. A unique method has been developed for this purpose. A standard radiation tube fitted with a galvanometer as shown in Fig. 4 is attached alongside of the immersion unit and plugged into the control head, Fig. 5. The whole assembly is then sighted against the inside wall of the furnace, as indicated in Fig. 6, and the dial on the control head adjusted until a zero reading on the galvanometer is obtained, at which point the dial is locked. In this manner, compensation is made for any dust or foreign matter collected on the instrument.

The operation of the immersion unit in the foundry or the mill is relatively simple and can readily be performed. It consists of the aperture, slotted at the end for air passage, and attached to it a double walled brass protection pipe. Inside is mounted the collecting lens shielded by a pyrex window and the radiation tube itself. The brass tube is finned to permit some of the air to pass between the inner and the outer wall for increased heat dissipation. Attached to this assembly is a half-inch pipe which connects with the control head and acts an as added protection for the leadwires.

The control head, Fig. 3, is fitted with an air gauge, G, an off and on air valve, H, a switch, J, which permits readings to be taken when the unit is immersed in the molten metal only, a second switch, M, used when standardizing the instrument, and a potentiometer dial, L, for adjusting the tube. Attached to the head is a short length of 1½-in. pipe connecting with an air hose to the reducer station. The leadwires from the head to the recording instrument run inside the air hose for added protection. The recording instrument is a standard recording unit with a range of from 1400°-3200° F and a chart speed of 2 in. per min.

The air pressure used plays a vital part if consistent

Fig. 9 (below)—Schematic diagram showing construction details of thermocouple and application method.



formed by one man. To take a reading, the tube is inserted through the wicket hole and the tip immersed to a depth of approximately 6 in. in the liquid metal. The air valve is held open to allow the air to flow through the orifice and form the cavity, the inner wall of which becomes target for the radiation tube.

As soon as the tube is in place, the pyrometer switch is closed thereby recording the reading after which the tube is withdrawn. The whole operation requires about 12 sec. with an actual contact time between the radiation tube and the recorder of about 2 sec. A schematic diagram, Fig. 7, demonstrates the operation.

The temperatures are recorded on a chart in a manner shown in Fig. 8 which represents an open-hearth heat. Each double line indicates the temperature of a particular reading. As such recordings can be taken at



Fig. 12 (right)—Temperature recorder (circular chart).

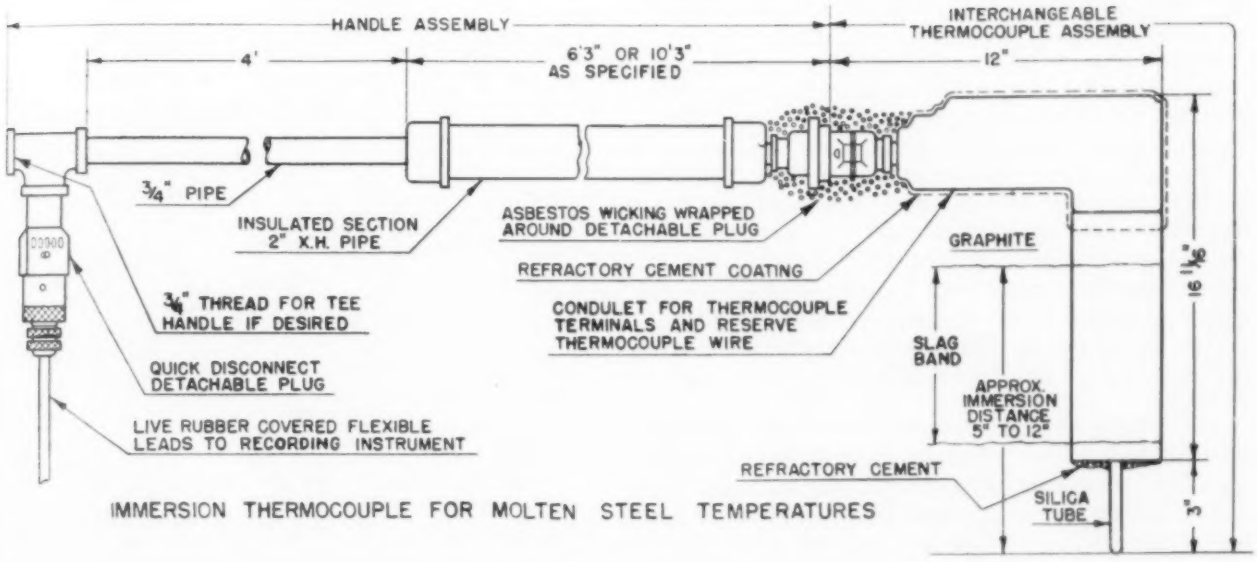


Fig. 11 (above)—Drawing of immersion thermocouple.

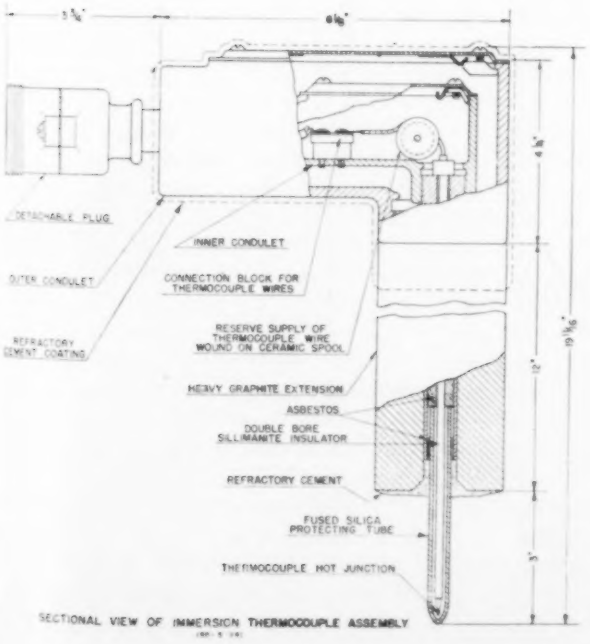
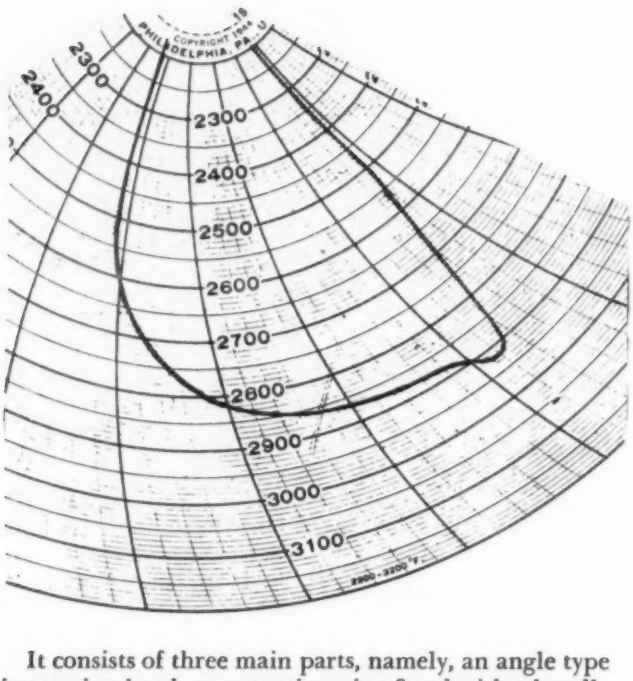


Fig. 10 (left)—Drawing showing the construction details and materials for an immersion head thermocouple.

about 6 to 10-min. intervals, a very complete log can be obtained by drawing a curve connecting these points.

The maintenance of this instrument appears from the results obtained to date, to be low, usually confined to replacement of the pyrex glass window which occasionally gets splattered with metal. Although a coating of metal and slag frequently adheres to the tube when withdrawn from the furnace, no difficulties have been encountered in rapidly removing the same.

Immersion Thermocouple Method. The second instrument selected for this investigation is a direct reading platinum-platinum-rhodium immersion thermocouple. This instrument is used in an adaptation of the so-called "quick-immersion" technique first developed by Schofield and Grace in England and later modified by other investigators in this country.



It consists of three main parts, namely, an angle type immersion head, an extension pipe fitted with a handle and the recorder, Fig. 9.

The immersion head shown in Fig. 10 contains a fused silica protection tube which comes in direct contact with the molten metal and acts as a protection for the hot junction of a platinum versus platinum–13 per cent rhodium thermocouple. The silica tube is attached to a heavy graphite extension block which acts as a protection at the slag line for the platinum leads which in turn are mounted in a double bore sillimanite insulator.

This graphite block is threaded to an 18-8 pipe screwed into an inner condulet, housing, a ceramic spool and a connecting block for the thermocouple wires. Although the required length of the wires is only about 20 in., a 30-in. thermocouple is used with the excess wire being coiled around the ceramic spool as a reserve. An outer condulet fitted with a detachable plug and coated with a refractory cement acts as a protection for the inner assembly.

The extension pipe, Fig. 11, which carries the lead wires and varies in length depending upon the furnace used, is protected by a 2-in. pipe fitted with rockwool at the end exposed to the furnace temperatures to prevent warping and damage to the wires.

The opposite end is fitted with a "quick-disconnect" locking type detachable plug for connecting the flexible rubber insulated wires leading to the recorder.

Taking Temperature Readings

The recorder, Fig. 12, is an electronic potentiometer having a range of from 2200 to 3200° F and fitted with a circular chart making a complete revolution in 4 min. A selector switch which is mounted on the panel enables the operator to balance the recording instrument before each temperature reading.

To assure maximum accuracy, the power or fuel as the case may be, is shut off and the bath thoroughly rabbled, just prior to taking a temperature reading. The unit is then inserted in the furnace by sliding it on the breast with the immersion head in the horizontal position, an operation easily performed by one man. The head is usually held for a few seconds above the slag for preheating whereupon the tip is rapidly immersed in the metal to a depth of about six in. As soon as the temperature curve levels off, a matter of 35 to 40 sec., the unit is removed from the furnace.

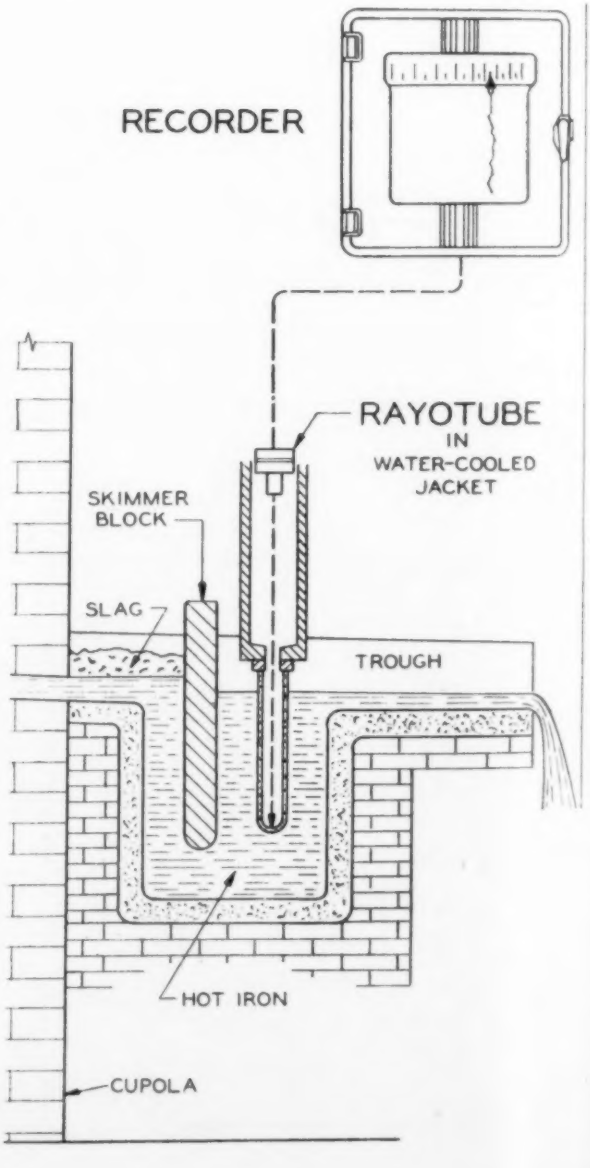
A typical reading is shown in Fig. 13. The sharp rise in temperature occurring at the end of the reading is caused by the couple passing through the hot slag when withdrawn and indicates the extreme sensitivity of this instrument.

Before taking a new reading it is best to replace the silica tube, although it is possible to obtain several readings on the same tube but always at the risk of losing a portion of the platinum wires.

The thermocouple which is calibrated with a stand-

Fig. 13 (left)—Typical temperature curve for thermocouple unit; 35 to 40 sec. required for curve to level off.

Fig. 14 (below)—Radiation type of pyrometer, permanently mounted, records temperatures for entire heat.



ard couple before being put into operation, is generally used for about ten readings after which time approximately one-fourth inch is removed and the wires rewelded at the hot junction and the entire couple annealed. After about 275 to 300 readings, or when the leads become too short, the thermocouple is replaced.

The accuracy of the instrument is within 10 to 15° at 2900° F and is dependent upon care in the assembly and replacement of the hot junction at proper intervals.

Evaluation of Results Obtained. In evaluating the results obtained from the rather extensive investigation carried out, we believe that we are justified in stating that both principles involved form the basis for very valuable tools which when properly handled can be a great help to the metallurgist in the foundry and in the mill.

Common to both are accuracy, reliability and ease in handling which are all necessary if the expenditure is to be justified.

Certain advantages and disadvantages, depending upon the application and requirements, are, however, inherent in each principle and these must be carefully considered when selecting the method most suitable for a particular shop.

When a complete time-temperature log from meltdown to tap is desired, an instrument based on the radiation principle offers the most in that it permits frequent readings, i.e., at about 6 to 10- min. intervals, to be taken. Such an instrument, furthermore, requires little maintenance. In addition, there is little danger of damage when adverse slag conditions are encountered.

Possible Sources of Error

Offsetting these advantages, however, is the fact that an instrument of this type is not standardized under operating conditions, therefore, slight errors are possible due to the difference in emissivity between the standardizing body and the molten metal as well as to the chilling effect and oxidizing effect of the air stream. However, by repeated checking against thermocouple readings, these errors have been found not to exceed about ±10° F at 2900° F provided that the necessary care in the operation is exercised. Such errors are within acceptable limits.

Where maximum flexibility is of prime importance and the instrument is to be used for temperature recordings of the metal in the ladle as well as in the furnace, the thermocouple principle permits easier handling and has the advantage that the actual temperature is recorded without requiring emissivity correction.

Offseting this, however, is the need for replacing the silica protection tube after each reading at a cost of approximately $0.90 to $1.00 plus labor. This expenditure, which is rather high in particular when small furnaces are used, cannot be evaluated properly, however, without considering the much lower initial installation cost of the instrument.

Application in the Gray Iron Foundry. Of particular interest to the men in the foundries is the application of radiation type pyrometers. The operation of both these instruments is similar in that they sight a radiamatic or radiation tube into an enclosed protection tube, submerged in the molten metal.

The protection tubes are purged with a small amount of air to free them from fumes that would impair true temperature readings. Also a suitable cooling system is provided to insure protection for the radiation collector units. The instruments are mounted permanently on continuous cupolas as shown in Fig. 14 and produce a complete temperature record for the entire heat.

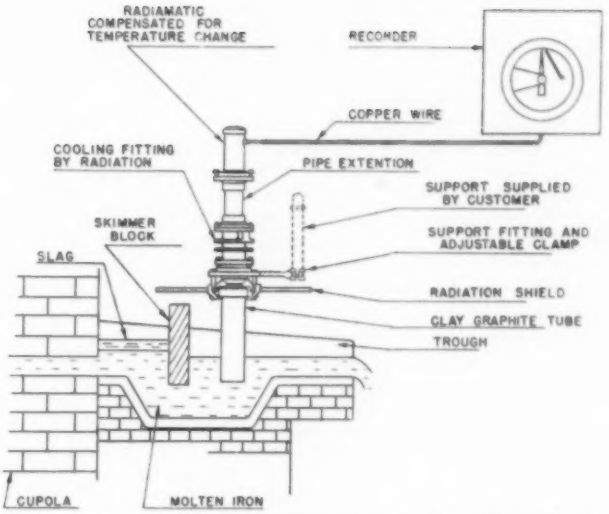


Fig. 15—Drawing showing construction details and application method of another type radiation pyrometer.

Complete temperature records are a great aid to the foundryman in that they help to improve his cupola control and thereby the quality of his castings.

Although the writers have had no first hand experience with these units, it is our intention to investigate their usefulness in the immediate future in connection with all types of melting units used in the foundry.

Conclusions

Accurate determination of liquid metal temperatures is essential to control of melting operations. In general, temperature measuring instruments for foundry use must be accurate within approximately plus or minus 10° F at about 2900° F. These instruments must respond quickly to temperature changes, must be rugged but portable, and should have low initial and low maintenance costs. Operation should be simple and the instrument must be recording as well as direct reading.

This paper has been presented in an effort to highlight the developments in the field of molten metal temperature measurements and thereby stimulate further interest in a subject of vital importance to all foundrymen. It is our belief that no efforts should be spared in further developing and perfecting methods and instruments, as this will not only help to improve our understanding and control of metallurgical procedures, but in doing so will raise the quality of our work and thereby benefit the industry as a whole.

Acknowledgment

The authors wish to express their appreciation to the Leeds and Northrup Company of Philadelphia, Pa., the Brown Instrument Company, also of Philadelphia, and the Rustless Iron & Steel Corp., Baltimore, Md., for their permission to use some of the drawings and photographs appearing in the paper.

DISCUSSION

Chairman: J. B. CAINE, Sawbrook Steel Castings Co., Cincinnati.

Co-Chairman: C. K. DONOHO, American Cast Iron Pipe Co., Birmingham.

DR. J. T. MACKENZIE:[1] I would like to ask how this graphite tungsten is protected in the closed couple or the open end type.

MR. TATE: On the closed end type we are using a protective coating of zirconious wash, whereas on the open type a high temperature cement wash to protect the graphite tube from excessive erosion will suffice. Indications are that we will get around 75 to 100 readings per foot of graphite by coating the outside with such a ceramic wash.

MR. VENNERHOLM: As the price of the tube is about 30 cents a foot and we hope to get 100 readings per foot, I do not think that this cost will be prohibitive.

L. P. WILSON:[2] The paper was excellent. Our work is in brass and other copper alloys and we have been working for about 2½ years on that same problem. What about tungsten oxidation? What is the alloy you use on the end of your radiation tube?

MR. TATE: The end of the radiation tube is just common steel. As for tungsten oxidation, we are coating the tungsten rod with an alumina wash which protects it from oxidation as well as carburization.

MEMBER: I have been much interested in this immersion thermocouple, and I had the opportunity last year of visiting the steel mills in Sheffield, England. They have been doing a lot of work on it, and a paper has been prepared for presentation before the British Iron and Steel Institute in which they substituted the carbon block by a steel block. The steel being immersed through the slag, it is covered with the slag and it serves as a protection for the couple. They have found out by that means, according to this British paper, that they have eliminated completely the necessity for shortening the thermocouple every six immersions. And from that paper it is believed that the corrosion of the platinum, platinum-rhodium couple is due to carbon pick-up from the graphite protection under the high temperature of the steel bath.

In our plant, we are installing a quick immersion thermocouple and we believe, and the manufacturers believe too, that we will not have to use up the platinum couple during the operation.

MR. VENNERHOLM: We are not using a graphite tube in conjunction with the platinum couple but a refractory tube. One serious objection to the platinum-rhodium couple is the instability of these metals above approximately 2500° F.

MR. TATE: There is an inner pipe of nickel steel on the inside of the graphite block which protects the platinum couple from any gases present as a result of reactions with the carbon block.

MEMBER: We have had no actual experience, but according to this British paper, they took 600 readings with this steel-protected thermocouple and had to do nothing to the couple; there was no deterioration of the couple.

CO-CHAIRMAN DONOHO: I would like to ask what is the maximum temperature that you can read with this silica sheath on the platinum-rhodium couple without the silica giving way.

MR. TATE: I think you can take a reading up as high as 3300° F. The silica is a special silica tube. It starts to soften up around about 2900° F., but I think you can get up to about 3300° F. before it disappears.

CO-CHAIRMAN DONOHO: We were hoping to read tapping temperatures in an electric furnace and even at 3200° F. the silica tube would curl up to where it finally broke the platinum wire.

MILTON TILLEY:[3] Do you find any trouble with oxidation on the metal interfering with the temperature readings with the air that you use to keep the metal from coming up into the tube? Is there any oxidation or any fumes coming up?

MR. TATE: There is enough pressure in the tube to prevent any flowing in same. As far as oxidation on the inside of the bubble formed is concerned, this is compensated for on your Speedomax meter. A Speedomax meter will give you any temperature scale you desire, and you actually standardize this instrument against your platinum immersion couple to find the proper scale to work with. Now, if we run our steels within the carbon range for which the instrument has been standardized, we do not experience any pronounced deviation.

MR. VENNERHOLM: The reaction of the air on the inside wall of the bubble is, naturally, exothermic so that you actually do get a slightly higher reading, but at the same time I believe that this anticipated increase is pretty well offset by the cooling effect from the air stream.

MR. TILLEY: I was thinking of the oxides interfering.

MR. VENNERHOLM: We do not consider this, at any rate up to the present time, to be much of a handicap. We frequently check our readings against a regular thermocouple; these readings compare very well.

MR. TILLEY: I was wondering if you did have some fluctuations there, whether it would be worth while using a non-oxidized gas.

MR. VENERHOLM: It may be beneficial to use a non-oxidizing gas; as long as the accuracy of this instrument, however, lies in the range of ± 10° F. we believe it is satisfactory for all practical purposes. The accuracy of the direct reading thermocouple is approximately ± 5° F.

J. E. KERR:[4] We would like to get temperature readings on our metal while we are pouring it; this should be comparatively easy in comparison to measuring metal temperature in the furnace. We pour Hadfield's manganese steel which oxidizes readily; it is therefore, accompanied by large quantities of smoke and flame during pouring operations. Due to the presence of this smoke and flame we cannot obtain consistent optical pyrometer readings. Do you think that the stream of air used with your instrument would cause sufficient oxidation of our material to interfere with accurate or consistent temperature measurements?

MR. TATE: If you intend to use a rayotube, the same should be standardized against a thermocouple first. I do not believe that with this type of metal your temperature scale will vary as long as the analyses stay within reasonable limits.

MR. KERR: Did you use it on any alloys that were more susceptible to oxidation?

MR. TATE: We have used it on plain carbon and the low alloy steels only.

M. C. UDY:[5] I think the real limitation of the temperature you can measure with the platinum, platinum-rhodium thermocouple is the melting point of platinum, which, as I remember it, is between 3100 and 3200° F.

MR. TATE: That is right.

CO-CHAIRMAN DONOHO: Our trouble was that the silica tube softened and floated up in the steel. Is there a special kind of silica that does not do that?

MR. UDY: Not silica, but there are other materials, as Mr. Tate mentioned, the alumina. If you can stand the cost, beryllia is a very excellent protection tube material.

MR. VENNERHOLM: I think the biggest limitation which we find right now is the instability of the platinum-rhodium alloy, more so than the refractory. You are speaking of 3200° F. tapping temperature; that is higher than we generally encounter, as we tap around 2900 to 2950° F., depending on the type of steel.

MEMBER: We have an application where we would like to melt around 3400° F.; and we would like to have a thermocouple that would do the job at that temperature.

MR. TATE: Using the graphite tungsten couple when finally developed will do this. According to theory, we can take temperatures up to 5000° F. by using a hydrogen stream in the tube.

CHAIRMAN CAINE: Along that same line, may I ask why the authors have not mentioned photoelectric cells? The rayotube when used in metal over 3100° F. is impractical, because you lose the tube. The whole instrument is in the bath and the steel tube will cut off within a matter of seconds. A photoelectric cell can be attached to the back of the tube, out of the furnace, using an ordinary pipe that is expendable, and offers possibilities. What is your thought along that line?

MR. TATE: I think a short explanation will clarify this point. We coat our tube with slag before the same enters into the metal bath; and the tube, furthermore, is not in the bath more than three seconds. I do not think that any difficulties need

[1] American Cast Iron Pipe Co., Birmingham, Ala.
[2] Western Cartridge Co., E. Alton, Ill.
[3] National Malleable and Steel Castings Co., Cleveland.
[4] Pettibone Mulliken Corp., Chicago.
[5] Battelle Memorial Institute, Columbus, Ohio.

to be anticipated within that length of time when the tube is coated with slag.

MR. VENNERHOLM: I would like to add that our ambition right now is to develop satisfactory instruments for the steel foundry and the mill; and that we feel that anything over 3000 to 3100° F. is out of the range of this present investigation.

CHAIRMAN CAINE: Then you would say there is a limit, let us say 3000 to 3100° F. for the two instruments that you have shown here?

MR. VENNERHOLM: I would say there is, yes.

J. A. RASSENFOSS:[6] In regard to the instability of the calibration of platinum, platinum-rhodium thermocouple at these operating temperatures, we did some work last summer and used a steel tip; we did not use the combination of graphite with the metal tube inside as you have, but one similar to those used in England. This tip was made from a piece of tapping bar 1½ in. in diameter and having a drilled hole through the center approximately 5/16 in. in diameter. The thermocouple projected from the end of this steel immersion tip for a distance of approximately 3 in. and was encased in a ¼ in. outside diameter vitreosil tube. That couple was used for 14 immersions before it was broken mechanically. It was in use on the floor and someone hit the couple and broke one of the wires. We had calibrated it immediately before that and it had changed only 5° F. from its original calibration. You might question that 5° F. with the equipment we used to calibrate it; there might be some suspicion that perhaps had we had equipment of greater precision for calibration it might have been better, but we did not think the 5-degree variation in the 14 readings was so much as to say it was unsatisfactory at that point. Perhaps had we been more careful, we could have carried it on for a considerably greater number of readings.

MR. TATE: When the decision was made to repair the couple after every tenth reading, it was not because the couple had deteriorated to any great extent, but to insure maximum accuracy that we made this rule to cut off ¼ in. after every tenth reading.

MR. RASSENFOSS: I think you can extend it a lot more than you have done so far. The English reports indicate perhaps as much as 25 or 30 readings, and usually then the couple has to be cut off, perhaps because of mechanical damage more than changing calibration due to its exposure to the temperature, because, actually you are immersing it and exposing it to that temperature for only a very short time.

MR. TATE: About 30 seconds.

MR. RASSENFOSS: Our readings were taken using a hand-operated potentiometer and our maximum was 30 seconds. We could normally get a reading in 23 seconds total immersion, from the time we started through the slag until we got a reading. So I do not believe that even on a theoretical basis we are in so much danger, because, actually our time at that temperature which would change the composition of the couple by the proximity of the wires is such that we do not need to be worried until we get up to quite a large number of readings. I think perhaps 10 is too conservative and that much better can be done.

MR. TATE: I agree with you that with highly skilled men, no such precautions would be necessary as they could tell just by looking at the end of the couple if it was starting to deteriorate. The first sign of any deterioration would be a frosty appearance at the hot junction or else black spots from carburization. If those symptoms are not present you can be reasonably sure that the couple gives satisfactory results. If the instrument, however, is to be a production unit, unskilled labor to handle the same, such as furnace operators, then certain definite rules have to be established.

MR. VENNERHOLM: Perhaps we did not stress in our paper the fact that the same represents only a preliminary report of our findings up to date and that our immediate goal was to fully evaluate two instruments of entirely different construction. The impression, therefore, may have been somewhat misleading; in particular relative to the point brought out by Mr. Rassenfoss regarding total number of readings obtainable before a correction had to be made, but for this particular investigation maximum accuracy was essential and we, therefore, did not want to take any chances.

MEMBER: In several plants where this same type of thermocouple is used as a production control device, they are getting more than 10 readings without having to cut off the end of the thermocouple. However, I think we have a big difference there that we cannot overlook. There, they are using the device as a production control instrument. In this study we had a research project, where the thermocouple instrument was being used as the master to calibrate a less sensitive and less accurate instrument that was being used to take the great number of readings that are always necessary in any research project. The precaution of establishing a figure that would allow too high a safety factor for normal practice was probably a good practice in this case.

MR. RASSENFOSS: Might not the use of the steel instead of the graphite head have something to do with the improvement? The English investigators seem to feel so.

MEMBER: I would seriously question that from the construction that we employ on the thermocouple. That is, I believe, a nickel alloy tube that protects the thermocouple or seals the thermocouple, in effect, from the protecting tube, so that there is, in effect, a barrier produced there and it would seem to me that that would have the same effect as the steel block.

MR. VENNERHOLM: That is correct; our refractory tube is inserted in a graphite block, but the thermocouple is insulated from the graphite block through an 18-8 stainless steel tube extending through the graphite block from the refractory tube to the upper portion of the head, so that the thermocouple itself is insulated from the graphite all the way down. I do not think that this particular phase is as much of an objection to an instrument of this type as the short life of the refractory tube. We are at any rate concentrating our efforts on finding a material that is less sensitive to all the physical and other conditions encountered in this type of work.

[6] American Steel Foundries, Indiana Harbor, Ind.

MALLEABLE IRON FINISHING

By
E. M. Strick, Finishing Supt.
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FINISHING OPERATIONS on malleable iron castings have sometimes been regarded as a necessary evil and neglected accordingly. Actually, they are an important consideration in the production of good, salable castings and have an important bearing on costs.

There is a tendency on the part of many foundry and pattern superintendents to regard the molding of castings with only two objectives in mind—high yield and low scrap loss. They pay too little attention to finishing. The removal of excessive and misplaced gates, feeders, crack strips, core fins and parting lines are the problems left to the finishing departments to solve as best they can. Many of these operations are performed at an excessive cost which could be reduced by greater co-operation between pattern, foundry and finishing sections.

Unnecessary finishing operations are sometimes caused by the unfamiliarity of the designing engineer with foundry techniques. His chief interest lies in the ultimate use of the casting. Therefore, it is the foundryman's responsibility to try to reconcile the customer's requirements with sound foundry practice.

When a print of a new job is received, it should be submitted to the foundry, pattern and finishing departments for thorough study. Based on this study, a cost estimate of the job is prepared from which the selling price is established and a quotation given the customer. If accepted, an order follows and the pattern and other equipment is constructed.

The pattern with temporary gating is first used by the foundry to produce sample castings. Careful examination of these samples reveals conditions which must be corrected and the job is resampled and the process repeated often enough to get the best possible results.

Subsequently, a sample casting is sent to the customer for his inspection and on receipt of his approval, the job is put into production.

It is important that the chief inspector obtain from the customer full finishing specifications. These would include information on the ultimate use of the casting, tolerances, locating points, sequence of machining operations and other details.

It often happens that castings are given unnecessary finishing due to a lack of information concerning the requirements of the customer. If in doubt, the foundry should always consult with the customer and, if possible, should obtain written confirmation to avoid any later misunderstanding.

Hard Iron Cleaning

Control of the finish on castings does not start in the hard iron cleaning department as many believe. It commences with the molder and much depends on the sand, the facing, the ramming, the pouring and many other considerations. With certain castings, gates must be knocked off hot and on many they can be so constructed that the gates can be knocked off with a minimum amount of stock left for grinding.

There is always considerable controvery relative to the advantages of cleaning castings in the hard iron state, and there are good arguments for and against this practice. In a jobbing foundry with a wide





Light malleable castings are ground on stand grinder.



Swing grinding does job on heavy malleable castings.

variety of castings requiring close tolerances, gauging, and an exceptionally good appearance, cleaning in the hard iron is essential. On long running jobs, after the foundry has produced many good castings with low scrap loss, cleaning of hard iron can be discontinued.

Obviously, effective cleaning of hard iron depends on the proper choice of cleaning equipment. Improper equipment or careless handling will result in cracking and breaking of castings because of their brittleness at this stage of their manufacture.

Hard Iron Inspection

Another argument for cleaning hard iron results from the fact that many customers receive daily reports of good castings molded and plan their production schedules accordingly. If, subsequently, deliveries are delayed due to the scrapping of castings discovered defective only after they have gone through the annealing process and other finishing operations, it tends to make the customer dissatisfied. Furthermore, the cost of all of the finishing operations on such castings is a complete loss to the foundry.

Hard iron can be cleaned by tumbling, wire brushing, water, shot and sand blasting. The method which shows the lowest cost for the type of work being produced should be the one selected. One method may prove more productive with higher equipment maintenance and lower labor cost while another may be lower in maintenance and higher in labor cost.

After castings have been cleaned, they are inspected and chipped in the hard iron inspection department. This function, in the opinion of the writer, is one of the most important of all finishing operations. The hard iron inspector must be familiar with defects such as slag holes, sand holes, cold shuts, misruns, shrinks, cracks, hot tears, roughness and other defects. He must be able to determine soundness of a casting by tapping it; must observe whether the correct cores have been used; must continually break castings to insure soundness; and know where to look for sub-surface gas holes.

Light malleable iron castings are hand straightened, inspected, gaged and bagged in this department section.

The pattern, molding and coremaking departments must be kept informed as to all defects which cause castings to be scrapped or which require extra finishing to salvage them. One method which has been found to be helpful has been to sort and lay out all scrap daily so that the molding and core room supervision (and the operators, if necessary) will have an opportunity to see and analyze it.

Wherever casting sections will permit, fins and crack strips should be knocked off to keep down grinding and chipping costs. Excessive and misplaced gates and feeders require extra finishing and should be brought to the attention of the personnel concerned for study and correction. By careful vigilance, the hard iron inspection department can be of great help to all departments.

Grinding. Light gates, feeders and fins can be removed by grinding in hard iron but there are certain objections to this. These objections include low production, high grinding wheel costs, breakage hazards and grinding heat checks incident to the grinding of too large gates, feeders and fins. For that reason, malleable iron castings are usually ground after annealing.

Determining Grinding Method

There are many methods of grinding hard iron or soft iron. Different type fixtures, pressure bars and other considerations based on the design of the casting usually determine the best grinding method.

Grinding machines, stand or swing, of proper speeds are most essential and they should be equipped with motors of ample horsepower so that there is no appreciable deceleration even when pressure bars are used. To obtain best grinding wheel efficiency, recommended peripheral speeds should be maintained.

There are two types of wheels commonly used in malleable foundries. Both are tested by the wheel manufacturers at much higher speeds than are called for by state safety codes. The recommended maximum speed of vitrified or low speed wheels is 6500 surface ft. per min., while that of the resinoid, bakelite and rubber high speed wheels is 9500 surface ft. per min.

Low wheel costs and maximum production are obtained by maintaining these speeds as the wheels wear. This can be accomplished by an increase in the RPM as the wheel becomes smaller in circumference. Variable speed grinding machines are ideal, of course, but very good results may be had on other grinding machines by changing pulleys after about every four in. diameter of wheel wear, or by changing the wheels from one machine to another of higher spindle speed so that maximum surface speed can be maintained.

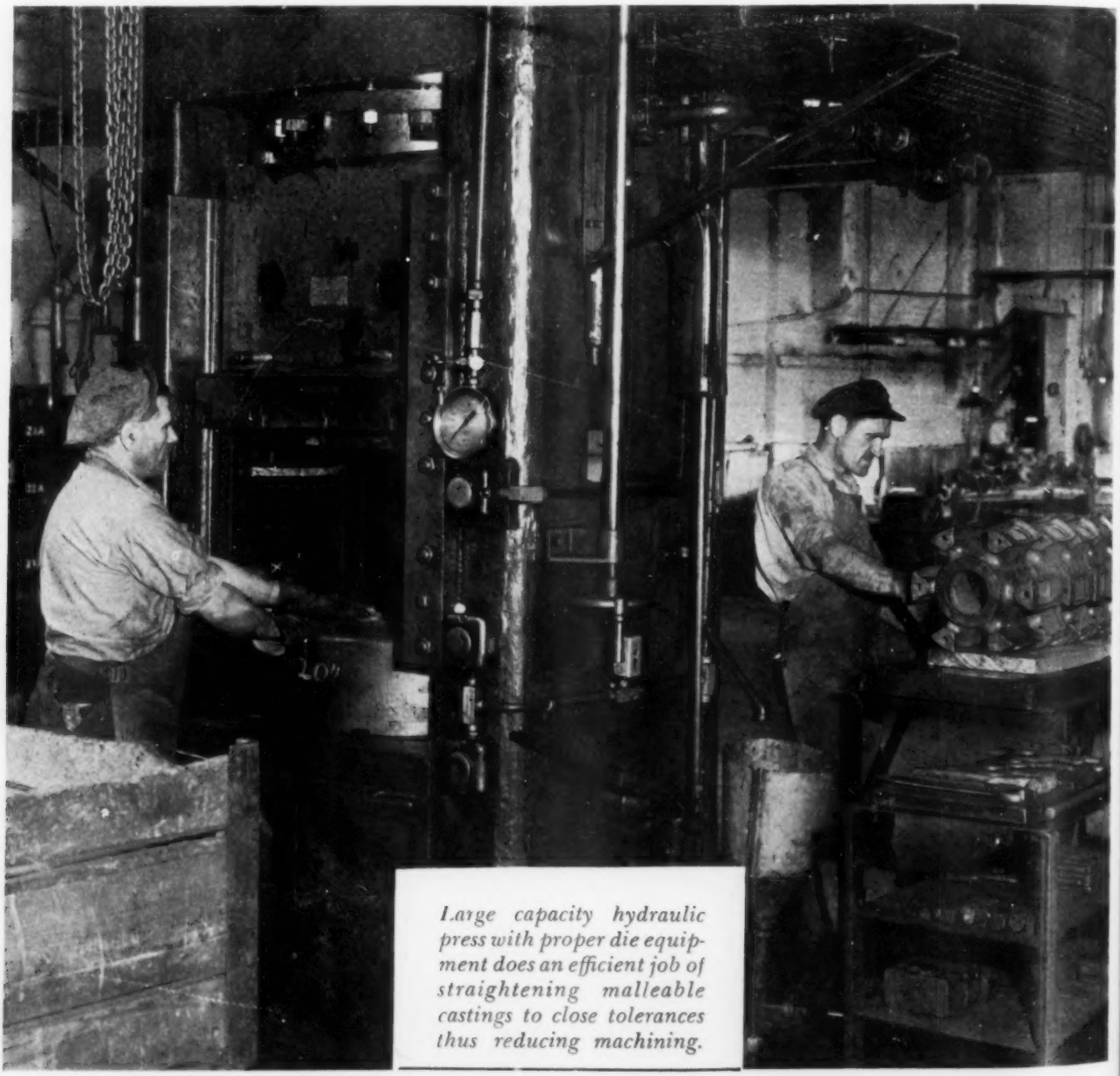
In order to make a comparison between any two types of grinding wheels, the following information should be obtained from the operating data:

Make of wheel; type of wheel; size of wheel; grade of wheel; price of wheel; number cubic inches available for use (full size until discarded); cost per cubic inch of wheel; diameter of wheel at start of test; diameter of wheel at end of test; cubic inches used on test; cost of wheel used on test; number of pieces ground; piece rate per hundred; earnings of operator per hour.

A careful comparison of the cubic inches of wheel (or weight) used, operator earnings and other elements should determine which wheel would be most economical. There are, of course, many variables, such as type of stand, grade of iron, whether or not pressure bars are used, and other considerations.

In comparative tests of different wheels, the same type of casting and the same operator should be used. The amount of metal removed by weight is a further indication of wheel efficiency. Of course, proper safeguards and dust removing equipment are important factors in any grinding department.

Shearing. Gating removal by shearing is quite effec-



Large capacity hydraulic press with proper die equipment does an efficient job of straightening malleable castings to close tolerances thus reducing machining.

tive but usually is adaptable only where quantities are substantial and justify the cost of the fixtures involved. The size and location of gates are very important if shearing is contemplated and considerable attention should be given to the design of fixtures and shearing tools if this operation is to be successful.

Ordinarily, less skill is required for this operation than for grinding and this may often be a determining factor. Iron recovery is also one of the advantages of the shearing operation.

Chipping. Due to the design of certain castings, the gates, feeders, parting lines and other projections are located where they cannot be removed either by grinding or shearing. This entails a chipping operation which is expensive at best and everything possible should be done to hold it to the minimum.

Skill and Equipment Required

Chipping requires considerable skill and universal fixtures to hold the work in a given position are desirable. Air operated vises mounted on a suitable table are very generally used and a small, portable air-operated grinding wheel is an essential supplement to the chipping equipment.

Soft Iron Cleaning. The cleaning of malleable iron in the soft, ductile state after annealing and after grinding is of utmost importance because in this operation all scale and sand adhering to the casting which might prove detrimental in subsequent machining or galvanizing is removed. Modern methods permit castings to be cleaned so that they can be machined more efficiently with longer tool life and with some reduction in time necessary for subsequent cleaning incident to galvanizing and plating.

The equipment used for the cleaning of soft iron is generally the same as that mentioned in connection with hard iron cleaning.

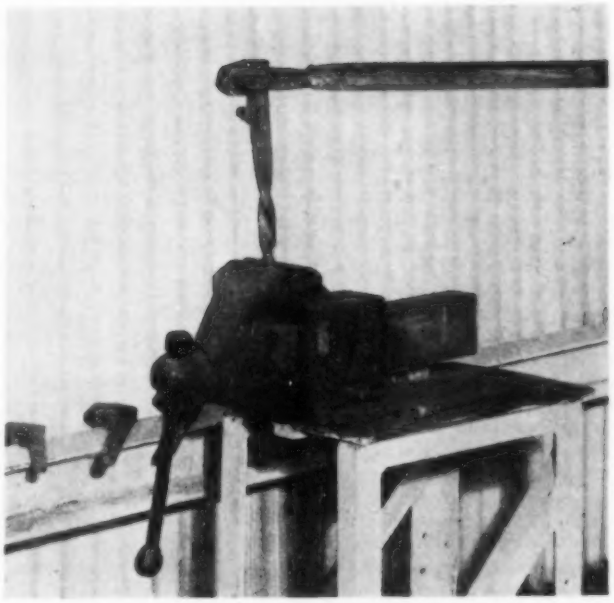
Straightening and Inspection. After cleaning, castings are straightened and inspected. Soft, ductile malleable iron lends itself to hand or die straightening where a high degree of uniformity is desired. This quality permits close tolerance straightening and consequent reduction in casting finishing and machining operations. Today, hydraulic presses of large capacity with very efficient die equipment are performing coining and pressing operations to tolerances previously deemed impossible. This has manifold advantages for the foundry and the purchaser of castings.

Die Design and Construction

An important corollary to all of this is the design and construction of dies which constitutes a comprehensive technical subject in itself. No attempt will be made to cover this subject here except to say that it affords a wide field for engineering ingenuity and large potential savings.

Other methods of straightening malleable castings include the use of slow speed presses without dies, hand straightening on the bench or plate with soft hammers, and drop hammer straightening. Every effort should be made to avoid hand straightening operations as these usually require skill and are slow and expensive.

Obviously, straightening is closely related to the packing of castings for annealing—the better they are packed, the less warpage to be corrected. Considerable straightening can be avoided by careful packing prior to the anneal although in some cases this consideration can be ignored and a properly constructed die depended upon to straighten *all* castings.



Torsion test made on pearlitic malleable iron casting.

Final Inspection

The final operation in connection with finishing is a piece inspection for which carefully trained personnel is required. Certain imperfections which are not apparent in the earlier processing or as a result of that processing, such as cracks, bad grinding, shearing, chipping, improper straightening and cleaning, burnt castings and metal defects should be detected at this point.

In many cases, it is necessary to ream, broach, drill or perform certain tests and other simple operations just before this final inspection takes place. Here, again, is indicated the necessity for close liaison with the customer and a wide knowledge on the part of the inspection department as to the customer's requirements.

Inspection as a function should be entirely divorced from any other operating functions and the chief inspector should report to general management or to a general superintendent rather than be under the jurisdiction of the foundry or finishing departments.

This sets up a function which can be quite independent and impartial, both of which are essential in the inspection operation.

Conclusion

Malleable iron because of its toughness, ductility and extremely high machineability has a wide industrial use. This results in a great range of types and sizes of castings so that, particularly in the jobbing foundry, it is necessary to generalize on the subject of finishing operations. A study of each casting must be made to arrive at the best combination of finishing operations.

There are no hard and fast methods which can be put down as being the best so all one can do is to hope that these thoughts and suggestions which are the results of experience may be helpful to others.

DISCUSSION

Chairman: C. F. JOHNSON, Central Foundry Div., General Motors Corp., Saginaw, Mich.

Co-Chairman: W. D. MCMILLAN, International Harvester Co., Chicago.

N. J. HENKE:[1] What are your recommendations for grits and hardnesses for grinding wheels for hard iron and for soft iron?

MR. STRICK: The Erie Malleable Iron Company is using practically 90 per cent low-speed wheels. We had to run a number of experiments to find the wheel that was adaptable for both malleable and pearlitic irons. A good part of our production is pearlitic malleable, so the regular standard wheels that other malleable companies have and which grinding wheel companies have brought in to use have not worked out very well on our castings. Our standard wheel is the N14Q treated wheel.

We use one wheel through the entire grinding department, both for soft and hard iron, so that we have a wheel that is in between, one that gives good production in soft iron and also stands up fairly well in hard iron. We use the wheels down to 8 in. in diameter on the smaller castings.

MR. HENKE: Is that originally a 30-in. wheel?

MR. STRICK: No, that is a 24 x 2 x 2-in. wheel originally. We use it down to 8 in. before discarding it. My recommendation, as a result of a great number of tests, is that the malleable foundry find the wheel most adaptable to their castings, not recommending any specific grade or grit.

D. TAMOR:[2] Have you any experience with the Do-All type machine for cutting off gates on small castings and, if so, have you any idea what the costs are?

MR. STRICK: No, the only thing we tried was a cut-off wheel on some of our large castings, and it did not turn out too well, due to the fact that the larger heads that we had on these castings were difficult to get at.

P. KNOP:[3] What is the difference between high and low-speed wheels in grinding costs?

MR. STRICK: Vitrified or low-speed wheels are much cheaper than high-speed resinoid or bakelite wheels. With correct grain, grit and hardness the one wheel will remove metal just as fast as the other. If you use low-speed wheels which are nearly solid down to a small diameter before discarding, the wheel cost should be less than that of the high-speed wheels because you discard a greater number of cubic inches of high-speed wheels since with a 10 or 12-in. center hole they usually are discarded when three and four inches outside this hole. High-speed wheels will grind smoother than low-speed wheels as the latter is usually a more open wheel which is necessary to get same metal removal.

Tests have proven to me that it is possible to remove metal just as fast with low-speed vitrified wheels as it is with high-speed wheels thus giving one lower costs.

Proper horsepower motors and pressure bars have a decided bearing on costs whether you use a high-speed wheel or a low-speed vitrified wheel. Overall grinding costs of various plants are related to type and size of castings manufactured, size of gates and feeders and method of grinding. They are two foundries of the same company that show very little difference in overall grinding costs. One uses entirely low-speed vitrified wheels while the other uses all high-speed resinoid wheels.

C. MADDICK:[4] I would like to have an expression of opinion on cleaning in hard iron versus soft iron.

MR. STRICK: It is a very controversial question. How many of the foundries represented clean in the hard iron? (2) How many do not clean in the hard iron? (About 20) It seems that the majority of them do not clean in hard iron. As a jobbing foundry we clean both in hard and soft iron for reasons explained in the paper.

J. O. KLEIN:[5] You have gone into the question of grinding wheel costs very extensively. I also note that you are using a 24 x 2 x 2-in. wheel. How do you find that that compares with the cost of, say, a 24 x 2x10-in. wheel?

MR. STRICK: We use vitrified wheels as it is unsafe to use wheels of this make with holes larger than three inches. The 24 x 2 x 10-in. wheels you speak of are high-speed wheels and as I stated in answer to Mr. Knop's question that due to the fact that high-speed wheels are much more expensive than low-speed wheels, the latter is the cheaper.

Our grinding machines are speeded up after every four inches of wheel wear to maintain proper surface feet per minute and this is vitally necessary to keep grinding costs at a minimum. Another manner of accomplishing the same results is to move the wheels as they wear to other machines of higher speeds. Two speeds are usually used on 24x 2 x 10-in. high-speed wheels while four speeds are used on 24 x 2 x 2-in. wheels. I say that the low-speed vitrified 24 x 2 x 2-in. wheel is cheaper than the 24 x 2 x 10-in. resinoid wheel if you can make full use of it.

MR. KLEIN: Some foundries claim that they can definitely save more by using the 10-in. bore and, I think that we do. I think that we are a little better off. Of course, our castings are probably a great deal larger castings to grind than yours. Therefore, the accessibility of the larger bore wheel, the larger diameter, is much better for our type than the small 8 or 9-in. wheel would be.

MR. STRICK: Here is one thing that I found out. We do not want to kid ourselves with the thought that by increasing the speed of the wheel, we get the efficiency out of a smaller wheel that we do from a larger one. The smaller wheel is going around so fast with increased speed that it does not have a chance to cool and it loads much faster. I have found that the castings cut more freely with the larger wheel. Where you have small castings, it does not make a particle of difference. The grinding and contact time is such a small amount of actual grinding time that a small wheel is just as good as a large one. But for larger castings, I think you probably have the right wheel.

MR. KLEIN: That is what we have found.

J. H. LANSING:[6] In connection with the use of high-speed and low-speed wheels, I think it has been interesting to observe, in some comparisons that have been made, that the wheel cost is generally higher with the high-speed wheels but the labor cost is generally lower. That has been our observation in connection with the operations of even such a good comparison as two foundries, two plants of the same company, where one plant might be grinding with high-speed wheels and would have a little higher wheel cost and the other plant might be grinding with low-speed wheels and have a lower wheel cost but a higher labor cost; so there would be very little difference between their total costs. I wonder if that checks at all with your experience and also with the experience of others?

Another item in which I was interested and which you mentioned was the pounds of metal removed per contact hour. I would be interested in further comment as to the experience of the range within which that varied.

MR. STRICK: I think you have gone into grinding costs thoroughly and have a published paper that I have just had an opportunity of reading. It was sent to the various malleable foundries in which you report that you have found there is very little difference in the two, the high- and low-speed grinding wheel costs; that where one was higher in wheel cost, the production was lower, and vice versa. I had a very good opportunity on a job that weighed 17¾ lb. to compare both. We made thousands of them and I used the same operator on the same stands and ran eight different types of wheels. I did not take the contact time but I took the actual grinding time, because, running them all equally, I did not feel that it was necessary to stand behind them with a stop watch to get contact time. We started out grinding a box of castings weighing 875 lb. and when we finished, we found they weighed 805 lb. showing 70 lb. of metal removed. The actual grinding time for the metal removed was 3¼ to 5 hr. on various wheels. The actual weight of metal removed per hour ran from 14 lb. up to 21 lb. This plainly denoted variation of different wheels.

J. J. WITENHAFER:[7] When you say actual grinding time, do you mean total grinding time, pick-up, contact, drop?

MR. STRICK: Yes, that is actual operator's time.

MR. LANSING: Do you not find that in the case of the slow-speed wheels it is much more important to have high-pressure against the wheel at grinding than in the case of the high-speed wheel?

MR. STRICK: I would say that you do, but with pressure bars

[1] Central Foundry Div., General Motors, Saginaw, Mich.
[2] American Chain & Cable Co., York, Pa.
[3] Albion Malleable Iron Co., Albion, Mich.
[4] International Harvester Co., Hamilton, Ont.
[5] Texas Foundries, Inc., Lufkin, Texas.
[6] Malleable Founders' Society, Cleveland.
[7] Columbus Malleable Iron, Columbus, Ohio.

that are used for grinding castings in foundries today, a little more pressure does not mean much.

A. M. FULTON:[8] You use a 24 x 2 x 2-in. wheel. Why do you use that width of wheel? When you speak of slow speed, are you using a vitrified or a bakelite wheel?

MR. STRICK: It is most confusing I know to speak of low- and high-speed grinding wheels. You can run any wheel at any speed you wish, high or low but grinding wheel manufacturers recommend the speeds that are safe for various wheels to be run at. Many foundries are running their grinding wheels at a much lower speed than recommended. This is costly. Lower speeds are recommended for vitrified wheels.

MR. FULTON: Is that what you are using?

MR. STRICK: Yes.

MR. FULTON: Why did you pick the 24 x 2 x 2-in. wheel?

MR. STRICK: Because it seemed to be more adaptable to the type of work that we are doing. We had larger width wheels and we found that with a pressure bar, when you are grinding the same gate in the same location of the wheel, the wider the wheel is, the more wheel you have to dress off because of the groove worn in it. We also found out that 2-in. wheels did the best job for the size gates we have on our castings.

MR. KNOP: Mr. Lansing recently conducted a test and I was told that he found that high-speed wheels removed 200 per cent as much iron as a low-speed wheel, and I can see no reason why that should be, when the one wheel is only 50 per cent faster than the other one.

MR. LANSING: I think there was one comparison that had been made some time ago that showed a very substantial increase in the amount of metal removed per contact-hour by the high-speed wheels as compared with the low-speed wheels. I think probably the outstanding reason for that was that pressure was not being used. In other words, pressure bars were not being used.

MR. KNOP: Is that on the slow speed?

MR. LANSING: They were not used on either, and that made a lot of difference. In other words, those small castings had relatively small gates, but with the slower-speed wheel, there was an appreciable amount of time required for removing the gate when the casting was shoved against the wheel, whereas with the high-speed wheel, the gate seemed to melt right off. That was not given as an average comparison; it was just given as one example, and the factors affecting it were explained. Pressure is a big factor and, as you pointed out, the size of your motor is a big factor, too. You mentioned, I think, in the case of the high-speed comparison, that on your stand you did not have a big enough motor to be able to crowd the wheels, and that is certainly a big factor in the case of either high-speed or slow-speed wheels.

MR. STRICK: It is enlightening of late years that there is some interest being shown in grinding wheel costs for the first time in the history of the foundry. Malleable iron, gray iron and steel associations are now analyzing grinding wheel costs and giving their foundries information on the subject which has resulted in considerable savings and increased production.

MR. WITENHAFER: Is it necessary to use pressure sticks when you have the right horsepower, the right speed and the proper surface speed per minute on a grinding wheel?

MR. STRICK: Oh, absolutely. The man who is pushing the iron off with his body cannot do nearly as much with considerable more effort than the one who uses a pressure bar.

W. D. MCMILLAN:[9] In your article you have pictures of some malleable chuck wheels. I believe they are dual wheels. I would like to ask, those must be wheelabrated or cleaned with shot. Is this a cold melt foundry or is it a duplex, or duplex cupola air furnace?

MR. STRICK: We have the conventional air furnaces; no duplexing.

MR. MCMILLAN: Are they cleaned with shot?

MR. STRICK: We clean them with shot in a wheelabrator.

MR. MCMILLAN: Do you clean both the hard and the soft iron?

MR. STRICK: In the hard iron, we tumble them in large tumblers to break off the fins and for inspection purposes and wheelabrate them in the soft iron.

R. W. PARSONS:[10] This question has nothing to do with cleaning problems, but I noticed you mentioned plating or galvanizing and I wonder if you know what sort of plating your customers put on the malleable castings?

MR. STRICK: I have seen malleable iron castings nickel-plated, galvanized and chrome-plated, also painted and enameled. With the modern cleaning equipment they can be cleaned more thoroughly in a very short period of time compared with the way we had to clean them in years gone by.

W. J. KEEFE:[11] What method do you use, other than ringing, to locate invisible cracks? You spoke of the elaborate equipment required to shear off gates from castings. When you shear a gate off, do you aim to shear it close enough so it requires no grinding or do you do it to save grinding time? We do it on some castings to save grinding time and find that the equipment required does not cost very much.

MR. STRICK: We try to shear it down to where it is not necessary to handle them a second time. On many castings, you can put them under a machine against a stop and shear the gate off, but there are many others that require fixtures. We have found that a change of gating was necessary on many jobs to shear them, and also different shearing tools. In a few instances, we have sheared a number of castings and then handled them the second time, where it was necessary to grind off some parting lines. I found it was costing more to handle them the second time than by grinding and doing the operation with one handling. So I try to keep away as much as possible from shearing any castings where it requires an extra handling in the grinding.

The technique we use to find cracks in the hard iron is by careful inspection, sounding them, and observation. That is why we clean castings in the hard iron. In the soft iron, most of our castings go through presses for straightening. By pressing them the crack will show up most of the time. Most of our cracks I would say are caused by handling in the hard iron.

MR. HENKE: We had the same experience that Mr. Strick did regarding shearing and duplicating the handling for grinding. As far as fine cracks are concerned, we have gone into magnaflux inspection, which has proved satisfactory.

MR. STRICK: You do not do this operation on real small castings, do you?

MR. HENKE: We do it on pearlitic castings.

MR. STRICK: Not on the regular malleable, though.

MR. HENKE: No.

[8] Northern Malleable Iron Co., St. Paul, Minn.

[9] International Harvester Co., Chicago.

[10] Ohio Brass Co., Mansfield, Ohio.

[11] Wm. E. Pratt Mfg. Co., Joliet, Ill.

LIQUID PHENOLIC CASTING RESINS FOR FOUNDRY PATTERNS

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ABSTRACT

Synthetic plastic materials are finding increasing application in the production of patterns and coreboxes. Capable of being cast, like some other materials used in pattern and matchplate construction, certain plastics provide the patternmaker with a pattern easily cleaned and finished. Cast resins machine something like hard maple and woodworking tools may be used. Advantages to the foundryman include rapid duplication of patterns which are wear-resistant and may be drawn readily from the sand.

SYNTHETIC PLASTIC MATERIALS may be used in a variety of ways by patternmakers. By the use of casting resins, the patternmaker can reproduce patterns rapidly and economically for mounting on a matchplate. He may even cast in one piece a plastic matchplate, complete with patterns, gates and runners, the only metal parts being the frame and lugs. Large loose patterns and core boxes may be cast of plastic as well as small patterns and matchplates.

There are many types of plastic materials, so to avoid confusion or misunderstanding, this discussion will be limited to the processing and application of accelerated type liquid phenolic casting resins. The cast resins available today in sheet, rod, tube, and block form, and multi-colored castings will not be considered.

Instead, attention will be confined to the liquid phenolics which, during the past few years, have been welcomed into industry for many basic uses. These include, in addition to foundry applications, forming dies for both hydropress and stretchpress operation, assembly jigs and fixtures, checking fixtures, design and working models, plating shields, masking fixtures, and patterns used in duplicating machines.

Every one of these applications presents an interesting study from the viewpoint of increased production as well as economy.

Casting resins in the main, are of the phenol-formaldehyde type, and their physical properties are similar to those of the compression or transfer molded phenolic plastics. The physical properties chart, Table 1, shows a few basic properties of casting resins. The figure illustrates a composite casting resin, representing the average of specifications of several manufacturers. The resin illustrated is an unfilled material containing 8 per cent of acid accelerator. The percentage of accelerator and the amount of filler affect the ultimate values for the properties.

In making plastic patterns, the preparation of the mold is the very heart of the whole project. As you know, everything that is not in the mold will not be in the cast part. Plaster molds are most widely used in practice. However, for certain jobs it may be more practical to use wood, metal, rubber, or plastic molds. It is simpler and faster to use plaster, although a cast resin mold may be desirable if greater resistance to breakage and chipping is desired.

A resin mold must be coated with Tygon paint or equivalent for without such a coating, a homogeneous unit would result when the plastic mix is poured into the plastic mold. Hardwood naturally makes more satisfactory molds than softwood, but all wood molds should be coated.

Plaster Mold Casting

A plaster mold requires a coating which serves as a parting agent. The coating may consist of a primer of Tygon Green Chromate Primer or equivalent and a finish coat of Black Tygon paint or equivalent.

While the primer is not necessary it is advisable to use it, if a smoother finish on the cast part is desired. Use of the primer tends to fill out irregularities in porous molds, gives better adhesion, and cuts down the amount of Black Tygon paint required. A very light coating of wax, such as a hard automobile wax, will assure easiest parting.

If a mold is made from an acid-resistant metal such as lead, no protective coating is necessary. Lead molds

TABLE 1.—PHYSICAL PROPERTIES OF PHENOL-FORMALDEHYDE TYPE RESINS
Unfilled Casting Resin with 8 Per Cent Accelerator

Physical Property	Value
Shrinkage from Mold Dimension, in. per in.	0.0025
Rockwell Hardness "R" Scale	115
Flexural Strength, psi	12,000
Compressive Strength, psi, (ASTM)	12,000
Tensile Strength, psi	5,000
Izod Impact Strength—ft. lb. per in. of notch, (ASTM)	0.24
Water Absorption, per cent	0.25
Specific Gravity	1.30



Fig. 1—Plaster mold (left) and the original metal master pattern. Note detail in plaster mold.



Fig. 2 (above)—Large loose pattern mold being painted.
Fig. 3 (below)—Patternmaker pouring molten resin into a mold.



are definitely the best to use on production runs. If a mold is made of non-acid-resistant metal, a protective coating must be applied. Otherwise the acid accelerator will attack the metal and cause the formation of bubbles and porosity in the cast resin.

Black Tygon Paint TP-21 or equivalent makes a satisfactory resistant coating when used as already mentioned. No primer should be used. Another satisfactory coating for metal molds is 13118 Durez Varnish or equivalent. It may be used as received or diluted with toluol if a thinner coating is desired. Two coats should be applied with a bake of 15 min. at 300 F after each coat.

Rubber Mold Coatings

Some resins can be cast in rubber or latex molds without the use of any coating material. However, the life of the mold is increased considerably by the application of a light film of lacquer or castor oil.

A new mold material, Nuplamold, is flexible, soft, and softens only at a high temperature. It may be reused and some moderate tests have shown it capable of remelting fifty times. In the case of undercuts, Nuplamold, rubber, or latex are the only materials practical for molds.

In preparing the plastic for casting, the resin and accelerator and filler, if used, must be weighed carefully before mixing, since only a slight change in proportions will affect the finished product. The temperature of the mixture must be rather carefully controlled within a range which varies with the type of resin and the type and amount of accelerator. If the temperature goes too high the reaction between the resin and accelerator may begin at once and proceed to the final stage without the assistance of additional heat. Usually, when this happens, the reaction goes too fast, ebullition occurs, and the resin froths out of the mold.

Resin and accelerator are always mixed slowly to avoid bubbles. If the mixture has only a few bubbles, these will come out during the period the resin stands at room temperature before it goes in the oven to bake. If too many bubbles are stirred in, they will not escape and the result will be an inferior casting, full of holes where the bubbles were entrapped.

After the mixing operation, the resin should be allowed to remain in the container for 10 to 15 min. to permit any air bubbles to rise to the surface. If a vacuum mixer is available, bubble-free mixtures can be prepared by mixing under vacuum and then stopping agitation before admitting air.

Minimizing Porosity in Cast Resins

By pouring a resin and accelerator mixture into a mold through one or more layers of an open-weave fabric, trapped air is removed to a large extent. The result is that bubbles and porosity in the hardened casting are greatly reduced. Of course, this would not be practical with resin mixes containing filler, as the fabric would filter out the filler.

The mixture of resin and accelerator must be poured soon after mixing, 15 to 30 min. being the usual practice. The mixture should be poured slowly into the mold to prevent formation of air bubbles

and subsequent pores in the finished casting.

After pouring the prepared mixture into the mold, baking is necessary to cure the resin and to make it hard and strong. The curing cycle depends on the volume of the resin, the amount of accelerator and the thickness and heat-transfer characteristics of the mold. As the resin sets, it becomes opaque and gelatinous and finally takes on a creamy appearance as it reaches its final hardness. The state of cure may be judged roughly by the appearance.

Baking Temperature Control

Control of temperature during baking is vitally important. Ovens should be checked to assure uniformity of temperature throughout. Even with an automatically controlled oven it is good practice to hang a thermometer in the oven very near the casting. It should read from 138 to 142 F. Higher temperature may cause foaming.

If the casting is soft and pliable when removed from the mold, it is still uncured. It must be completely cured and hard before removing from the oven. After curing, the color of the resin should be yellow. If an orange color develops, it is an indication of over-cure caused by too much accelerator or too high a temperature. When fast baking is required a higher percentage of accelerator and a higher baking temperature are used for pieces with thin sections.

Fillers may be added to the resin but in general they lower the strength of the cured plastic due to the formation of a weaker structure. For example, the addition of 20 per cent walnut shell flour reduces the flexural strength of a casting about 30 per cent. The use of a special asbestos, Powminco 10AA or equivalent, materially reduces shrinkage. However, only small quantities can be added to the resin without encountering difficulty in making a solid casting, due to the fluffy nature of the asbestos. The addition of asbestos filler also improves heat resistance and minimizes cracking due to thermal shock from rapid alternate heating and cooling.

Shrinkage as low as 0.001 in. per in. has been obtained with a mixture containing 8 per cent accelerator to which was added 5 per cent asbestos and 3 per cent boric acid. The asbestos and boric acid content were calculated on the combined weight of the resin and accelerator. The boric acid was first heated 4 hr. at 300 F to remove the water of crystallization.

Machining of Cast Resins

Cast resins machine about like hard maple and, in general, woodworking tools may be used. However, if many pieces are to be machined, carbide-tipped tools are recommended. Data on workability and finishing of cast resins may be obtained from standard handbooks and other sources. Usually very little finishing of plastic patterns is required although some uneven surfaces may have to be smoothed with a file, scraper and sandpaper.

Porosity or surface defects may be easily patched with a small amount of resin containing about 15 per cent accelerator. This mixture is poured into the cavity which has been surrounded with a dam of clay or putty. The clay restricts the resin to a small area around the hole and reduces clean-up time.

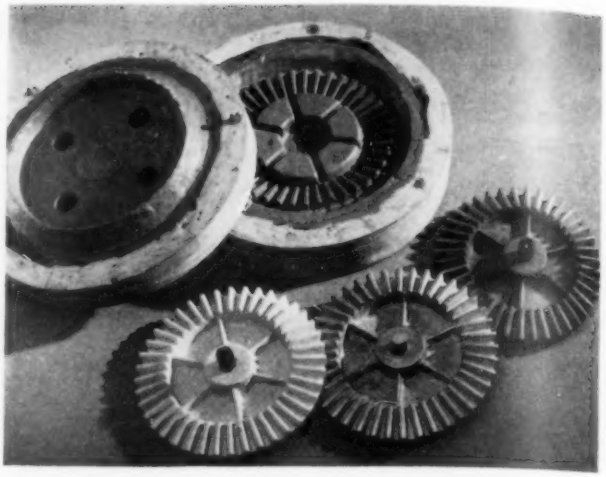


Fig. 4—Wood master pattern, aluminum pattern, cast resin pattern, and the plaster mold of a bevel gear.



Fig. 5 (above)—Match plate pattern. The oarlocks and gates and runners of this pattern of cast resin. Fig. 6 (below)—Group of patterns made from liquid phenolic casting resins.



The savings effected through the use of cast plastic patterns are shown by examples of the commonest case—that of duplicating existing patterns. In one instance, a wood pattern costing 175 dollars was duplicated to meet production requirements. The duplicate was made in a plaster mold in less than 24 hr. and at a cost of 25 dollars. In another case, a patternmaker compared the costs of duplicating a bevel gear pattern requiring 3 lb. of aluminum alloy, 16 hr. finishing time and three sheets of emery cloth. To produce the cast resin pattern required 5 lb. of plaster, 4½ hr. to make the mold and mix the resin, 1¾ lb. of resin, ¼ sheet of sandpaper and a few spoonfuls of paint to coat the plaster mold. Additional patterns required only 1¾ lb. of resin and 1½ hr. time.

DISCUSSION

Chairman: V. J. SEDLON, Master Pattern Co., Cleveland

Co-Chairman: A. F. PFEIFFER, Allis-Chalmers Mfg. Co., Milwaukee

MEMBER: Have plastic core boxes been put to practical use in core blowing operations?

MR. SIMMONS: Not that I know of. When this plastic is subjected to 350° F. heat or higher, which I think you are concerned with, a crackle will begin to form in the material.

MEMBER: I do not necessarily mean the drier but the core box itself. What action would extreme cold have on it?

MR. SIMMONS: Temperature would not affect it.

MEMBER: We have used plastic patterns and core boxes to some success. It is true they break very easily. That has been our experience. We used them for driers. The heat does not affect them. The core blowing has been successful unless you drop the box on a concrete floor. That will break it. But we have been successful with it, and we find that the sand will not adhere to the plastic as it will to wood or aluminum.

MR. SIMMONS: You do not have to lubricate the mold but the first few times it is used. The surface smooths off and the sand runs freely.

MEMBER: The first question asked was in regard to a blower core boxes. Apparently, that is a hand-rammed core box.

MEMBER: We have blown cores into plastic core boxes with some success. I would not say you are going to have a box that will have a long life, because we hape strictly a jobbing shop, running around 500 to a 1000 cores in one pattern. We are new in it, and we are working with the pattern shop, using anything that they produce as an experiment.

MR. SIMMONS: How long do your aluminum patterns stand up under those circumstances?

MEMBER: They stand up, on the average, half the wear of cast iron.

MR. SIMMONS: I think you will find these will stand up even better than that. They have greater abrasion resistance.

MEMBER: In one case we have duplicate patterns made from aluminum and plastic. Our experience has been about one-half. I do not know whether these higher plastic boxes are being heat-treated. Possibly you could improve on it, but at least it has been running approximately 20 per cent.

MEMBER: We manufacture these core boxes. We also manufacture plates. In core box practice we have not really had a long range program where we could truly say that they will run indefinitely. We subjected them to sand blasting tests. We find that, in comparison ratio tests, it will outwear aluminum sections three to one. There are not many plastic core boxes in the field today from our shop, but we know that they have had successful runs of more than 9,000 cores.

We reinforce our boxes. In some cases, we metal case them. The only failure we have had in fracture was not by dropping but by neglecting to clear the two faces of the box. When the men put them together slightly open and then apply the pressure to the clamp, then the boxes fracture right down the center. That was the reason we put them into a metal frame or housing with a plastic liner, and they proved successful.

E. A. GOERGER[1]: How does that apply to large core boxes?

MEMBER: The largest core box that we have made was a motor section. It is about 28 in. long and about 14 in. wide, the largest piece we have had. We also make core boxes about 1 ft. in length. We have not had any return yet.

MEMBER: What resistance does this plastic have to warpage when it is stored in the pattern vault for about a year and then brought back for re-use?

CHAIRMAN SEDLON: Did you notice the very low water absorption property of the material in Table I? You get very little, if any, effect from it.

MEMBER: If you add any fillers outside of fiber or asbestos filler, you will have water absorption by that filler. The plate in itself may become unstable. That is one of the main reasons of an aluminum frame that has been incorporated in the newer type of plastic plate.

There is another reason for that warpage and that is improper mixing of the catalyst to the resin. You have to have it thoroughly, uniformly mixed throughout the resin. Otherwise, the reaction of the curing in the plate itself will definitely distort it, probably not immediately but in time. I have seen plates that have been out in the field over a year, and they are perfectly straight. Then I have seen some come back that look like a bow.

MEMBER: We are trying to use this material. In core boxes about 8 in. wide by 12 in. long we find during curing warpage which we would like to eliminate. We are also interested in shrinkage up to 0.0005 in. per in. instead of 0.0025 in. per in.

MEMBER: That is an engineering problem. We have had it and it is just a question of applying common, ordinary mechanics. In some cases you have to metallize your plate or pattern, and sometimes you also have to reinforce it. And in some cases it is best to apply pressure.

MEMBER: Do you do that before or after it has started to cure?

MEMBER: As soon as you close your mold, make sure it is locked so it cannot give. In fact, your biggest trouble is not in the plastic. It is in your mold.

MEMBER: We opened a mold where it was definitely bowed on each end. It was up about 1/16 in. We are using a resin plaster as a pattern and curing against it.

MEMBER: Your plaster gets away from the plastic, not the plastic away from the plaster. You check your mold and you will find that it is the mold that causes the trouble.

MEMBER: Can you pour into a sand mold?

MR. SIMMONS: No, it does not work.

MEMBER: Mr. Simmons made the statement that plastic phenolic patterns are lower in cost from 10 to 30 per cent. If that is the case, then does the purchasing agent not take that into consideration when letting out jobs to different jobbers to have the pattern made in plastic?

MR. SIMMONS: I gave quite a wide range. It is just as if you attempt to buy a pattern. You can take it to one shop and it will cost you a certain figure. You can take it to three others and get three other bids. The same thing would still be true, no matter who is making the plastic pattern. The approximation of a saving of from 10 to 30 per cent is feasible and possible.

MEMBER: We use a lot of plaster patterns to make guides. How does this plastic work in regard to plaster? Does it tend to pick up that touch of sand which we try to eliminate to gain perfection of surface?

MR. SIMMONS: After it is dried and cured, the plastic will not touch the sand.

MEMBER: Would it be better than plaster itself?

MR. SIMMONS: It should be very definitely.

CHAIRMAN SEDLON: At what temperature do you pour kirksite?

MEMBER: We pour kirksite at 850 to 925° F.

MR. SIMMONS: You are operating above 350° F. so that precludes use of this plastic.

CHAIRMAN SEDLON: In other words, you cannot pour kirksite into the plastic.

MEMBER: He wants to use the plastic as a pattern, not as a mold.

MR. SIMMONS: In that case, I would say it is all right. We have had the question of molten material come up. It chars the plastic and helps to disintegrate it. It does not burn.

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[1] City Pattern & Model Co. Inc., St. Louis

SLAG CONTROL IN THE ACID ELECTRIC FURNACE

H. H. Johnson, M. T. McDonough and D. L. Radford *

ABSTRACT

The results of a study of some of the characteristics of acid electric furnace slags, together with some applications of these characteristics to serve as a guide for such furnace practice, are presented.

Consideration is given to the correlation that might exist between such items as (a) slag viscosity and corresponding chemical composition, (b) slag composition and corresponding bath analysis and bath temperature and (c) slag viscosity and composition in relation to the bath conditions that exist at several stages in the course of the melting cycle.

Some attempts at the application of slag measurements to the control of several steps in the process indicate

a. That such measurements may be useful as an indication of the probable manganese recovery obtained;

b. That they provide a measure (which can be incorporated in the form of quality control charts) to assist in directing the technique employed by several melters and to form a basis for comparison of the performance of several melting units;

c. That they afford an excellent indication as to the progress of the heat and serve as a guide on how to proceed with the working of the heat;

d. That there is some correlation between slag characteristics and the physical properties of the metal produced.

IN THE MELTING OF STEEL there are certain criteria by which the melter can judge the condition of the metal during the several stages of the melting operation. One of the most important of these in basic open hearth practice is based on the appearance and measurements of certain properties of the slag as they change during the working of the heat. Much study has been made of this subject and slag control is a well understood and very useful tool in the hands of the melters in many basic open hearth shops.

Previous Work in the Field of Acid Furnace Practice. In the field of acid furnace practice (both open hearth and electric) the study of the slag reactions has received only superficial attention until recent years, so that slag measurements and slag control are not as useful tools for acid practice at the present time as they might be. Recently considerable work has been carried out in the field of acid open hearth practice by the Acid Open Hearth Research Association and many of their findings have been published.[1, 2]

The work of Juppenlatz in the field of acid electric practice, which was presented before the A.F.A. in 1942,[3] summarizes most of the work in this field up to that time and indicates the possibilities of slag control in this practice. In 1943, Bowers[4] reported on an application of slag measurements, particularly as applied to the control of manganese in acid electric furnace practice and some discussion of slag control was reported in the proceedings of the Electric Furnace Conferences of the Iron and Steel Division of the AIME[5, 6, 7] for recent years.

Scope of Present Work. It is the authors' purpose in the present paper to relate some of their experiences in attempting to measure the properties of the slags produced in their practice and to use these findings in the development of slag control as a factor in the production of electric furnace heats.

Melting Practice. The electric melting equipment used consists of two batteries of furnaces, one unit composed of furnaces in which six tons are melted while in the other unit nine tons are melted. The metal produced is largely plain carbon and low alloy steel, such as the pearlitic manganese grade.

The melting practice is conventional for this class of steel. The charge melts in at about 0.35 per cent carbon and usually is followed with ore additions to give a vigorous boil which reduces the carbon to about 0.15 per cent. In case of a soft melt, pig iron is added after melt down to give a boil and also to raise the carbon slightly. Apparently the added carbon reacts with the iron oxide in the bath to give a boil, as it would if it had been present at melt down.

After the boil the heat is partially blocked with silico-manganese and then a sample is analyzed for carbon, manganese, phosphorus and sulfur. While waiting on the carbon and manganese analyses, the temperature of the metal is adjusted so that the final additions can be made to the heat as soon as the analyses are received. The slag that forms during the melting and boiling periods is not raked off but is "shaped" during the working of the heat so that, as far as possible, it has the desired characteristics when the heat is ready to tap. In manipulating the slag, it is recognized that the slag is merely an indicator of the condition of the metal itself. However, in controlling the characteristics of the slag, the characteristics of

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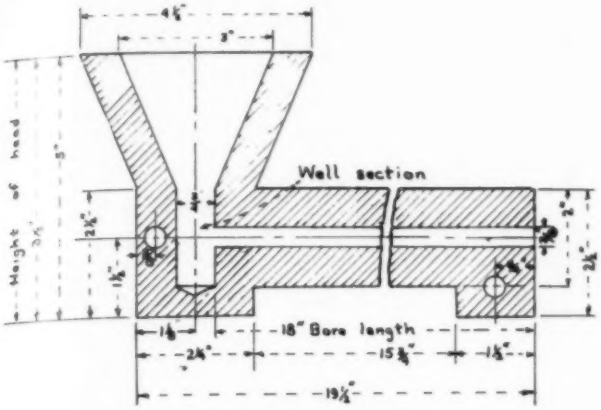


Fig. 1—Sketch of section through viscosimeter used in tests. Bore is 3/8 in. diameter, rather than usual 1/4.

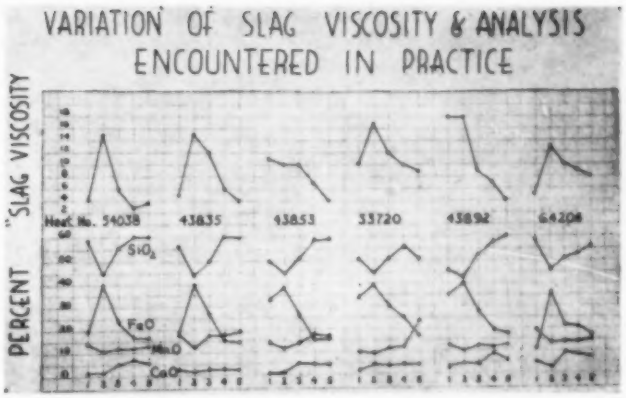


Fig. 2—Tests results on six heats. Horizontal numbers indicate time of sampling: 1, at meltdown; 2, after oil boil; 3, after block; 4, before final additions; 5, at tap.

the metal are being controlled to some extent.

Slag Sampling. For several years, it has been the practice to take a slag sample just before making final additions. This sample was poured on an iron plate and when it was cooled it was broken. From the appearance of the slag cake, the condition of the slag was appraised as well as the probable recovery of manganese that could be obtained from the addition.

While this has been a useful guide, need has been felt for a more definite measurement of the condition of the slag and, in this connection, the viscosimeter has shown itself to be a useful tool. The authors have used a viscosimeter of the size and design described by Fitterer and used in the work of the Acid Open Hearth Research Association.[1, 2] The principal difference between it and the one commonly used in basic open hearth practice (and in most of the work that has been published on acid electric slags) is that the bore is 3/8 in. in diameter instead of 1/4 in.

The larger bore seems to be more sensitive in that the length of run with viscous slags is sufficient to allow accurate measurement, while it is seldom that one encounters slags so thin that they run out at the end. To measure the length of run, a rod graduated in inches, is inserted at the open end of the mold and the unfilled distance measured. This distance, subtracted from the length of the mold gives the slag length for the particular test. A sketch of a cross section of the mold is shown in Fig. 1.

Certain obvious precautions are observed in taking and pouring the samples. The spoon in which the sample is taken is well slagged before taking the sample and the sample is always taken from a spot about 2/3 of the distance between the door and the nearest electrode. Any heavy top slag is pushed away and the slag layer nearest the slag-metal interface is sampled. No time is lost in pouring the sample into the viscosimeter mold.

In pouring the test, the lip of the spoon is brought close to the edge of the head section of the mold so that the head factor remains the same for all tests. Care is also taken to fill completely the head section each time. The mold is well cleaned out after each test and *tightly* clamped to be ready for the next test.

Changes in Slag Composition. One of the first concerns was to determine what slag behavior might be expected during the melting and refining period of typical heats, recognizing that the characteristics of heats will vary due to differences in melting stock, in available power, in operating conditions, etc.

Slag samples and corresponding viscosimeter tests were taken at five intervals during the working of the heats as follows: (1) at meltdown, when practically all the scrap was melted and before any large ore (or pig) additions were made; (2) after the boil; (3) about 5 min. after the block of silico-manganese or ferro-manganese had been added; (4) before the final additions were added (perhaps 20 min. after (3) was taken); and (5) immediately before tapping.

These time intervals seemed to mark the steps in the course of heats when the corresponding slag conditions are of greatest interest. Fig. 2 presents graphically the results obtained on six heats. Sets of samples were analyzed from some 20 heats for a background, but in the interests of brevity only some of the results are shown to indicate the range of conditions that may be encountered in practice.

The differences in viscosity that may be encountered at the meltdown point from heat to heat should be noted. A range of viscosity from 4 in. to 18 in. is indicated, and while this represents quite nonuniform conditions of melting stock and meltdown practice, such conditions *can* occur.

As would be expected, the SiO_2 and the FeO in the slag are the constituents that vary most, the SiO_2 varying inversely and the FeO directly with the slag length measurement. After the boil has taken place, the FeO in the slag is at its highest peak, the SiO_2 is at its lowest value, and the slag viscosity value is at its highest peak. It is interesting that all the slags seemed to show almost exactly the same amounts of SiO_2 and FeO after the boil (45 per cent SiO_2 and 40 per cent FeO) although the viscosities were not in such close agreement.

Examination of the results obtained from other heats, not included on the chart, however, show an expected range of 45 to 50 per cent SiO_2 and 30 to 40 per cent FeO. The majority of the analyses of samples taken at this point fall within this range. During the remainder of the working of each heat, the

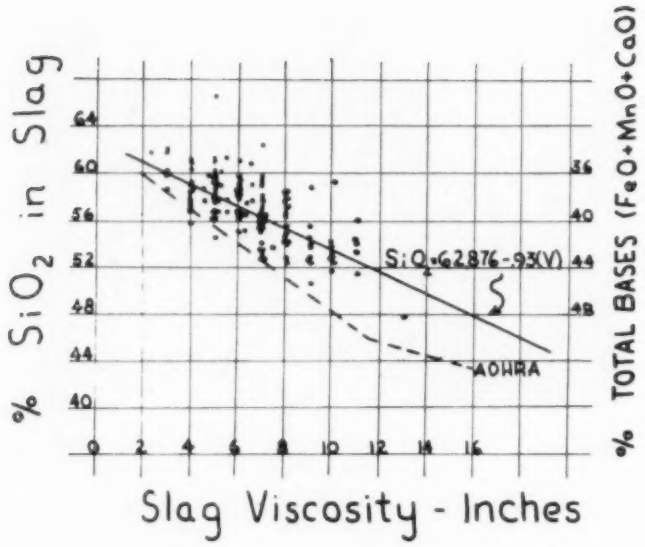


Fig. 3—Individual results of tests undertaken to determine the relationship between slag viscosity and the composition of the slag itself—SiO_2 content of slag plotted against the viscosity observed. These determinations were made on a steel of medium manganese analysis.

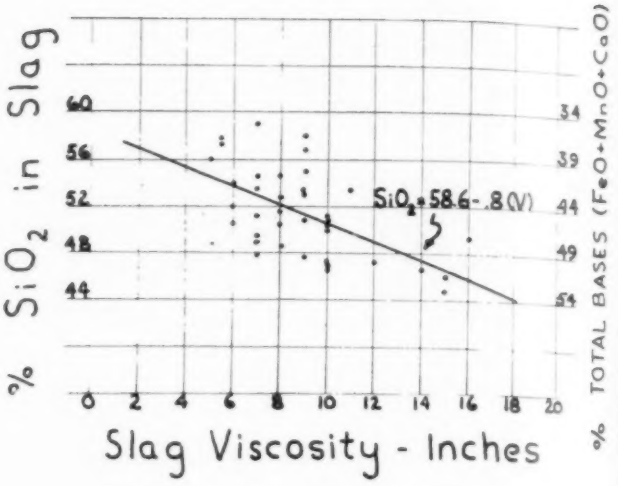


Fig. 4—Test results, as in Fig. 3, for grade "B" steel.

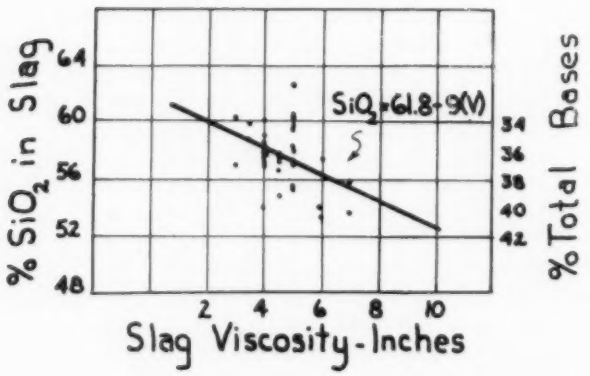


Fig. 5—Slag SiO_2 content-viscosity curve described in Fig. 3, based on high carbon steel test results.

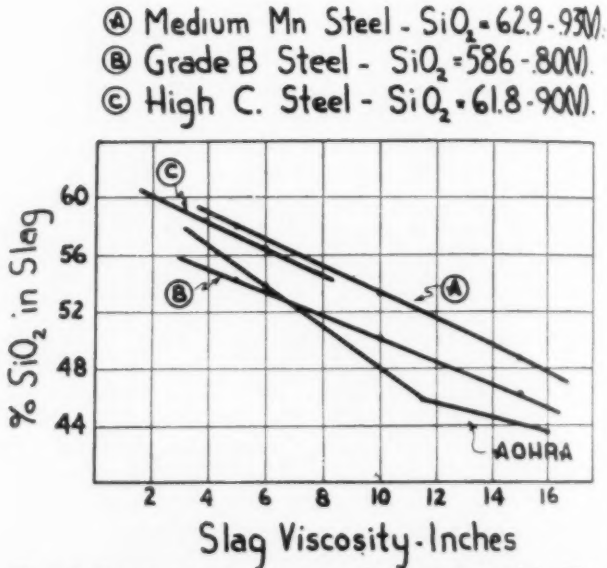


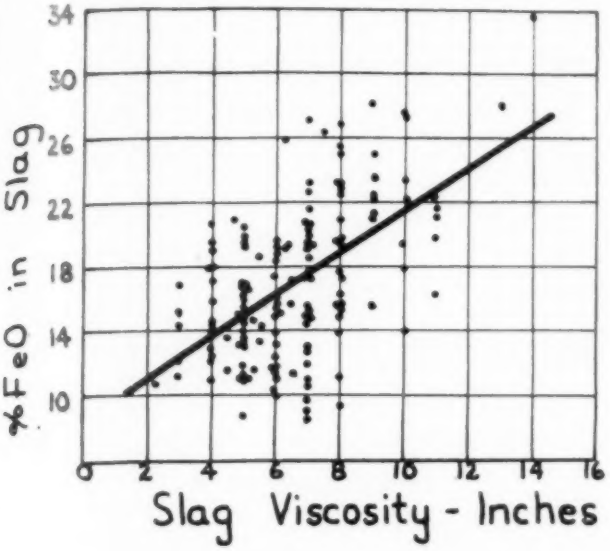
Fig. 6—Comparison of test results represented in Figs. 3, 4 and 5 with curve given in the bulletin of OAHRA.

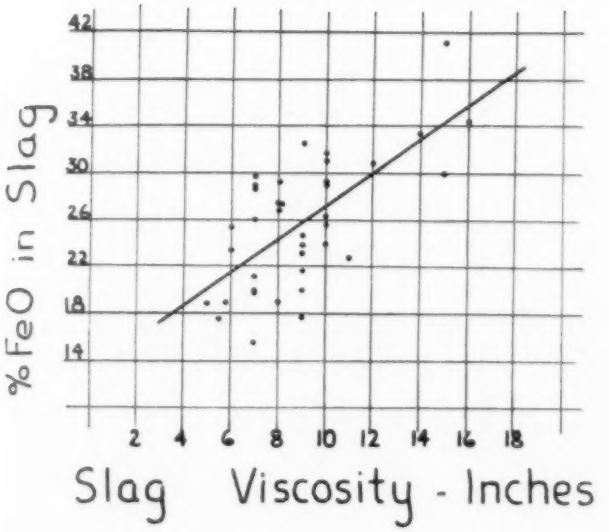
several variables seem to be trying to approach equilibrium, which is one reason why the authors prefer to hold a heat in the furnace for about 20 min. after blocking. During this period temperature adjustments of the bath are made and carbon and manganese contents on which to base calculations for the final additions are secured.

It is obvious that the amount of CaO present in the slag is largely determined by the amount of lime that is added to the slag and this, in turn, is largely a matter of the melter's judgment. There seems to be no close correlation between the CaO content and the working of the slag. The authors prefer to have about 3 to 5 per cent CaO. The effect of the CaO content is discussed in somewhat more detail later in the paper.

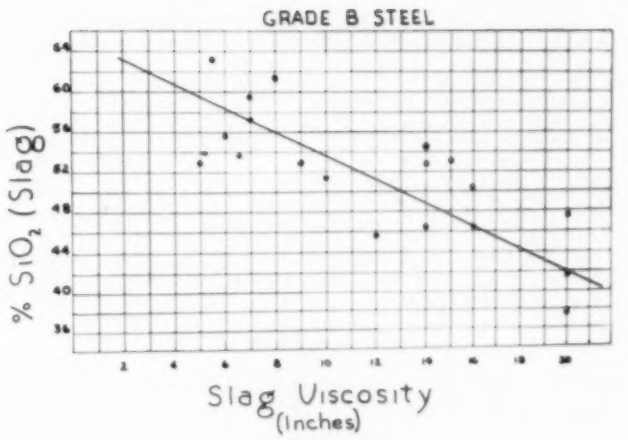
The amount of MnO present seems usually to remain fairly constant throughout the working of the heats. Note that in heat No. 33720, however, the MnO increases very markedly after the final additions were made. Probably some of the manganese addition was trapped in the slag. An interesting example of this condition was encountered when electrolytic manganese (which comes in the form of thin flakes) was used as the source of manganese in a few heats.[8]

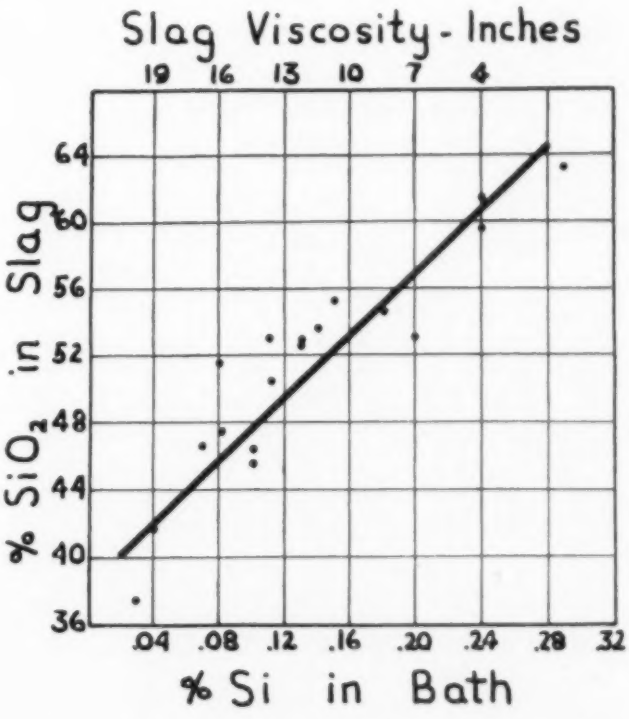
Unless considerable precautions were taken when adding the manganese in this form, some of it would be trapped in the slag, giving an apparent increase in MnO content of from perhaps 11 per cent to 19 per cent, while the FeO would decrease from about 22 per cent to 16 per cent. Samples of slag taken from the ladle showed this relationship of MnO and FeO to still hold during the pouring of the heat. When the electrolytic manganese was added in containers so that it all got into the bath, the slag analyses followed the expected pattern.





Figs. 7 (top) and 8—Relationship of the FeO content of slag to its viscosity plotted for, respectively, medium manganese steel and grade "B" plain carbon steel.





Figs. 9 and 10—Respectively, SiO_2 content-viscosity curve; comparison of SiO_2 in slag and silicon in metal.

The viscosities at tap for the six heats exhibited all fall within the range of 4 to 9 in. This is considered satisfactory and an indication that the metal has reached a satisfactory equilibrium and is ready to tap.

Slag Viscosity as Related to SiO_2. The authors were interested in further determining the relationships that exist between the observed slag viscosities and the composition of the slags themselves. Some 300 slag samples were taken representing the three analyses of grades of steel that the authors' plant commonly produces, and representing all the melting units. In Fig. 3 the individual results correlating SiO_2 in the slag with viscosity, for medium manganese steel, have been plotted. The best straight line to represent the points was calculated by the method of least squares and is represented by the equation $SiO_2=62.876-0.93V$ where SiO_2 is the per cent of SiO_2 in the slag and V the slag viscosity measured in inches.

It follows that the amount of total bases (FeO—MnO—CaO) will bear a reciprocal relationship to the SiO_2 content. Because the analyses do not include determinations of Al_2O_3, MgO, and other bases that may be present in small quantities, the scale for plotting the bases is not equivalent to the difference between the per cent of SiO_2 and 100 per cent, but is approximately as noted on the graph. Similar graphs were constructed from observations representing two grades of plain carbon steel, grade "B" (Fig. 4) and a high carbon steel (Fig. 5).

For comparison the authors have plotted the curves together in Fig. 6 and have included the curve for acid open hearth practice as given in the bulletin of that association.[2] It seems interesting that the calculated lines representing the three grades of steel parallel each other but are displaced somewhat.

The individual points representing the medium manganese steel (Naco) show a grouping in the range

TABLE I—VALUES OBTAINED IN SLAG SAMPLING

Grade of Steel	Relationship	Sandard Deviation Vis-cosity	Other Variables	(r) • (σ_r)	Coefficient of Correlation (r)
Naco	SiO_2—Viscosity	2.04	SiO_2—2.95	.046	—.66
Naco	Bases—Viscosity	2.03	Bases—2.91	.046	+.655
Naco	FeO—Viscosity	2.04	FeO—4.65	.055	+.56
Grade B	SiO_2—Viscosity	2.65	SiO_2—3.70	.113	—.565
Grade B	Bases—Viscosity	2.65	Bases—3.99	.094	+.66
Grade B	FeO—Viscosity	2.63	FeO—5.40	.094	+.66

• Not corrected for sample size.

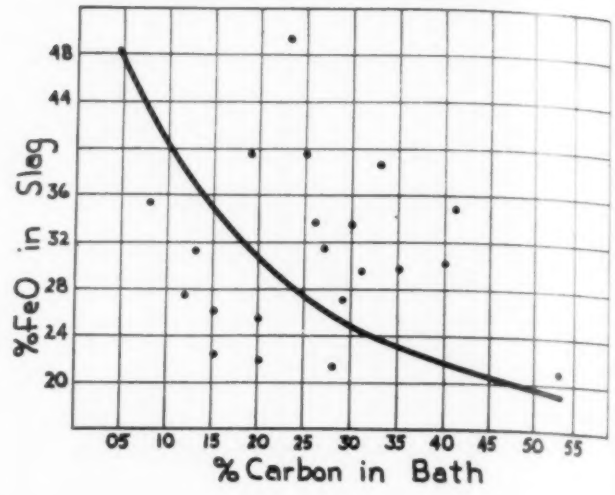
of 4 to 10 in. viscosity, while the values representing grade "B" heats are scattered over a somewhat greater range and there is a trend to viscosimeter specimens of longer length. Part of the answer may be that the Naco heats were worked and tapped somewhat hotter than the grade "B" heats. It will also be noted that the slag length measurements found in the high carbon heats largely fall in the narrow range of 4 to 6 in. These heats are melted in at about 0.30 per cent carbon, the carbon is reduced to about 0.10 per cent, and then pig iron is added to recarburize to about 0.70 per cent carbon. The temperatures required for this practice are considerably higher than those used in the grade "B" practice, which may partially account for the different characteristics of the two sets of slags. The question of the effect of temperature is considered somewhat more at length later in the discussion. The large carbon additions also act to bring about a reduction in the FeO, which in turn acts to decrease the length of the viscosimeter specimens.

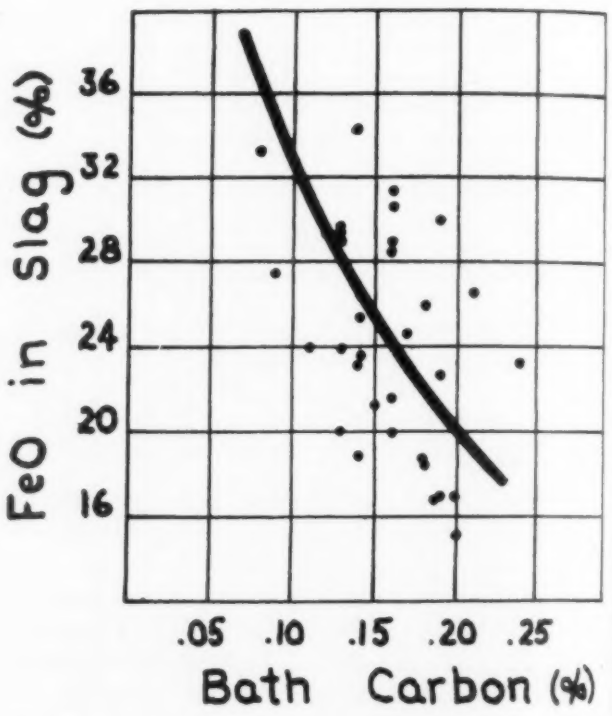
Attention is directed to the change in the direction of the line given in the Acid Open Hearth Research Association's bulletin as best representing the relation of SiO_2 and viscosity, and which shows an abrupt change in direction at a slag length measurement of 11½ in. The authors have few samples in the present data which have a slag length measurement greater than 12 in., but those that have do not indicate an abrupt change in direction such as the AOHRA found. Also the slope of the AOHRA line, for slag length measurements less than 11½ in., is considerably greater than it is for the steels reported in this paper.

The AOHRA bulletin states for its curve that, "although quantitatively the curve is somewhat different from plant to plant, it may be concluded that for a given practice, the SiO_2 of the slag is indicated by the fluidity test." The authors agree with this from observation of their own data, although their scatter of results, considering a large number of heats, is somewhat greater than previous discussions have indicated.[1, 2, 3, 4]

Slag Viscosity as Related to FeO Content. It was previously pointed out that the FeO and the SiO_2 contents were the principal variables in the slag composition and that the relationship between total bases and slag viscosity was the reciprocal of that between SiO_2 and viscosity.

The relation of FeO only to the viscosity of the slag is shown in Fig. 7 for medium manganese steel and in Fig. 8 for grade "B" steel. The straight lines that would best represent the data were calculated by the method of least squares and are given on the graphs.



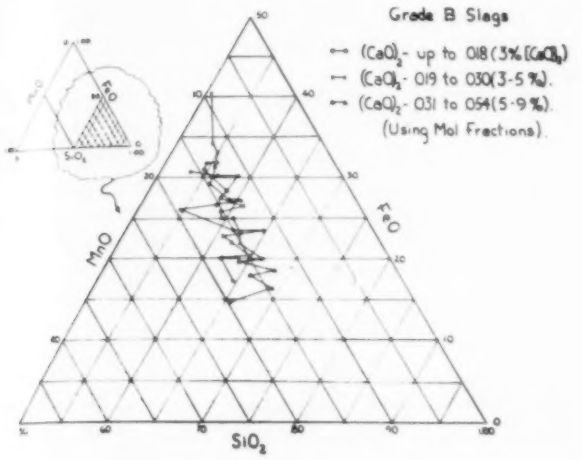


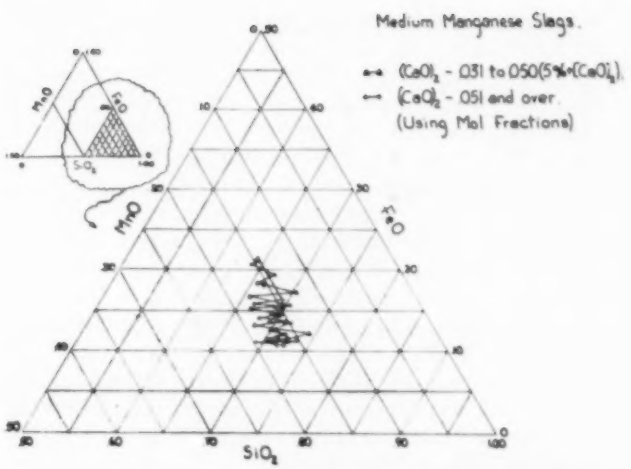
Figs. 11 (top) and 12—Comparison of FeO in slag and carbon content of bath, samples taken, respectively, immediately after melting and before final additions.

Statistical Tests of Data. The scatter of the several groups of data that have been discussed can be evaluated best, perhaps, by calculation of the standard deviation, correlation coefficient and the standard deviation of the correlation coefficient for each group of observations. These are shown Table 1.

In making these calculations the authors considered the SiO_2, the total bases, and the FeO respectively as the independent variable (the cause), and the viscosity, the dependent variable (the effect) in each case.

It will be remembered that usually the closer the correlation coefficient approaches +1 or −1, the more nearly perfect the relationship for the variables under consideration probably is. The negative coefficient





Figs. 14 (left) and 15—Effect of varying amounts of CaO on relationship of constituents in two types of slag.

(found for the SiO_2—viscosity relationship) indicates that the one variable is increasing while the other variable is decreasing. The positive coefficients for the other relationships indicate that the variables vary in the same direction.

A further check on the degree of correlation is to be found by determining the standard deviation of the correlation coefficient. If the value of r is greater than 3 times its standard deviation there are less than three chances in 1000 that the correlation is not real. In every case noted above, the tests show the correlation to be significant.

Correlation between Slag and Bath Analyses. Some of the relationships that may exist between the composition of the slag and that of the metal in the bath will be considered now. As an approach to this, there is a group of slag and bath samples taken as soon as possible after melting in (all from Grade "B" Heats). Fig. 9 shows the relationship between the slag viscosities and the corresponding SiO_2 contents of the slag. As would be expected, this is approximately a straight line. Fig. 10 shows fairly close agreement between the SiO_2 content of the slags and the corresponding silicon content of the metal.

This relationship is of interest because it indicates that the viscosity test gives an indication of the silicon content of the metal, if this close relationship, as just described, always exists. As will be noted later, the authors feel that a measure of the silicon content of the bath at this time in the heat is as important as that of the carbon content, for the silicon must be reduced to a low value before the carbon is reduced, if quality steel is to be produced.

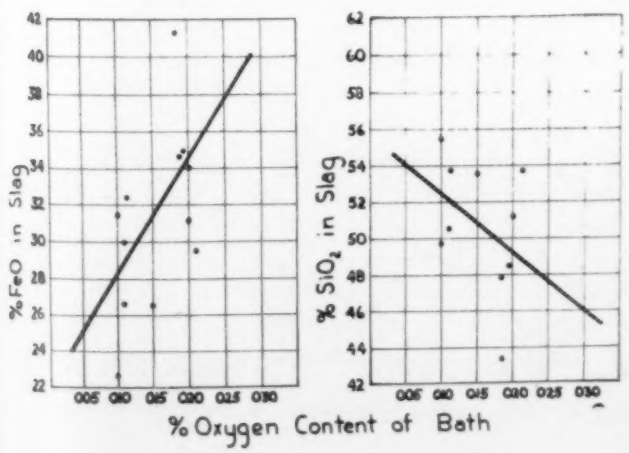


Fig. 13—Possible relationship between oxygen content of the bath and the FeO or SiO_2 content of the slag.

The FeO in the slag presumably tends toward equilibrium with the carbon in the bath following the equation $[FeO] \times (C) = K$, Where K = a constant.

In Fig. 11, the FeO—C analyses are plotted, corresponding to the SiO_2-Si analyses of Fig. 10. In other words, the samples which were analyzed were taken as soon as the heat was melted in. Obviously, there is a very poor state of equilibrium existing between the FeO of the slag and the carbon of the bath at this point in the heat.

On samples taken before the final additions were made, however, a considerably better correlation exists between the FeO and the carbon, as shown in Fig. 12, although there is still considerable scatter of the observations.

It is known that a relationship may exist between the rate of loss of carbon at any particular time and the oxygen content of the steel at that time.[10] Schwartz, in a private communication, has pointed out that such a relationship might be expressed by an equation:

$$O = \frac{0.002475 \ \frac{dc}{dt} - 0.001324}{C}$$

Where C = Carbon content
O = Oxygen content
and $\frac{dc}{dt}$ = Rate of change of carbon content at some particular moment.

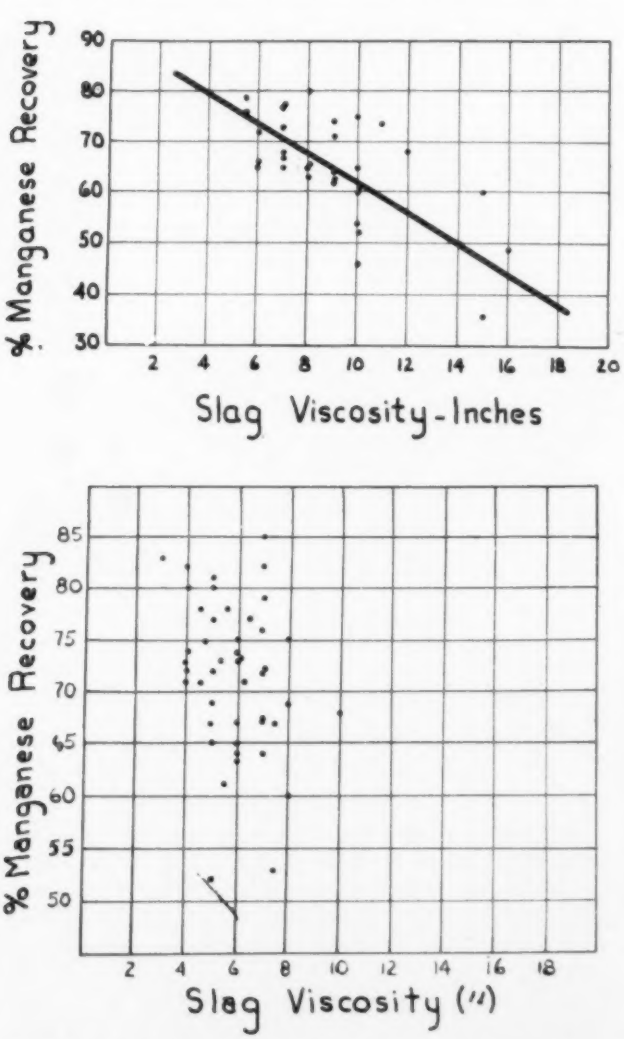
Samples were taken from a few heats at known time intervals, of both the slag and the metal. Fig. 13 indicates the relationship that might exist between the oxygen content of the bath (as calculated from the above equation) with the FeO content and the SiO_2 content of the slag. While the experiment was far from exhaustive, the indications are that the relationships are in the right direction and that the slag analysis may be taken as an indication of the oxygen content of the bath.

Apparently slag viscosity measures the SiO_2 content of the slag, which in turn is related to the silicon content of the metal, together with the FeO content which in turn is related to the carbon and probably also to the oxygen in the bath. This assumes that, for a given practice, the manganese content of the bath (and the corresponding MnO content of the slag) together with the CaO content of the slag will be maintained fairly constant at the desired operating levels.

Composition Relationships in the Slag. The relationships that might exist among the principal constituents of the slags seemed to be of importance and to indicate the desirability of some detailed study. For ease of computation, the analyses (by weight per cent) for a group of finishing slags from Grade "B" carbon heats and for a group from medium-manganese heats were circulated to mol fractions.

It was then assumed that the CaO present would probably be combined with SiO_2 as $(CaO)_2$-SiO_2 and that probably the FeO and the MnO would be present as FeO-SiO_2 and MnO-SiO_2. By simple calculations, using the mol fractions, it was determined that there was a considerable excess of SiO_2 over that required to combine in the form noted. Therefore, it was concluded that the FeO and MnO present would not be free to react to carry on further decarburization of the metal.

The question is frequently raised as to the amount of SiO_2 one should strike to have in the slag. From the viewpoint just discussed, the figures indicate that in slags containing about 45 per cent SiO_2, there is usually enough SiO_2 to combine with the other constituents and that those slags containing above 50 per cent SiO_2 have an ample amount present.



Figs. 16 (left) and 17—Slag viscosity and Mn recovery relationship for grade "B" and medium manganese steels.

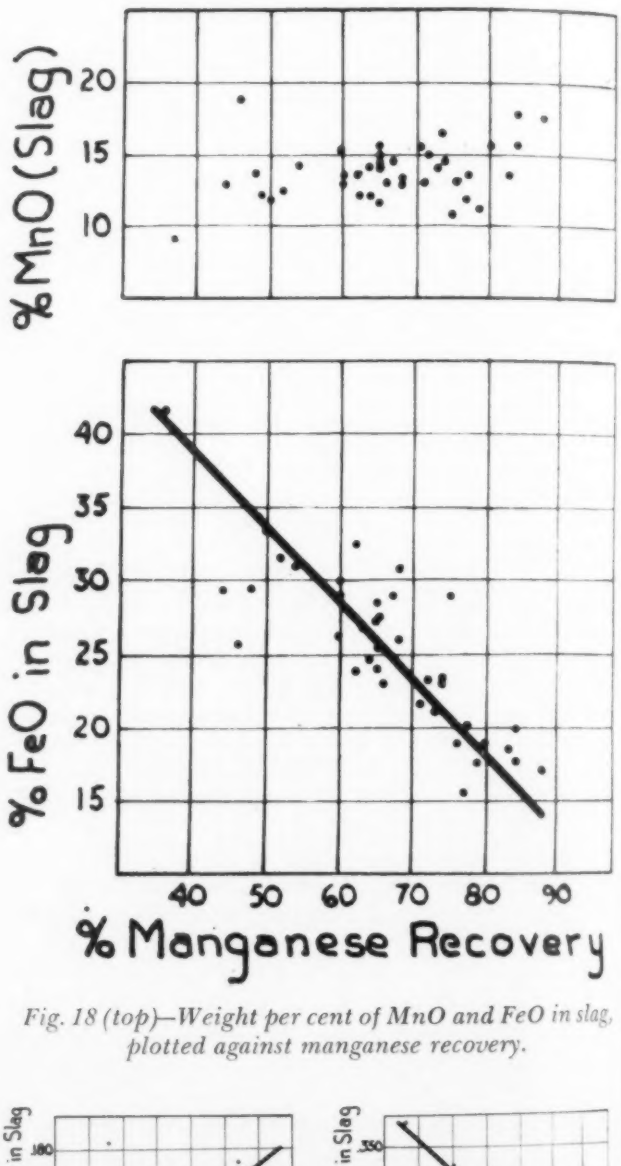


Fig. 18 (top)—Weight per cent of MnO and FeO in slag, plotted against manganese recovery.

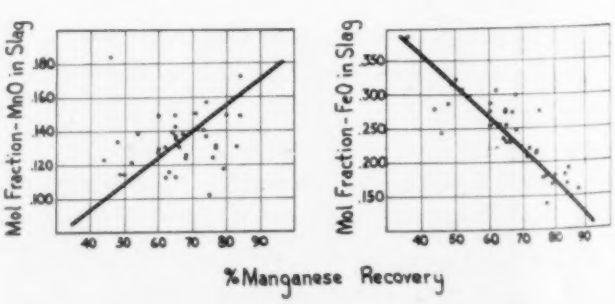


Fig. 19—Mol fraction of MnO and FeO in slag, plotted against manganese recovery, showing a better correlation. Grade "B" steel.

The authors were also interested in determining the effect of widely varying amounts of CaO [or $(CaO)_2$-SiO_2] on the relationships among the other constituents present. Using the same two groups of data, the amount of SiO_2 combined to form $(CaO)_2$-SiO_2 was deducted and the remaining $SiO_2 + FeO + MnO$ calculated to equal 100 per cent. Figs. 14 and 15 show the relationships for the slags from the two kinds of heats. There is no appreciable effect of the varying amounts of CaO, as indicated by the fact that the observations fall randomly and do not group themselves according to the several ranges of CaO that were chosen.

One thing of interest is the way the analyses for the slags from both the Grade "B" heats and from the Naco heats group themselves along the line corresponding to a mol fraction of 0.15 of MnO. The level of the MnO content is largely determined by charging practice, as Fitterer has also shown to be the case in acid open hearth practice.[1] Since the charge for the two kinds of steel is essentially the same, as is the working of the heats, this is easily understood.

There is a greater spread of SiO_2–FeO values in the practice for the Grade "B" steel than in that for the Naco or medium manganese steel and the level of values is different. In the case of the Grade "B" slags, the amount of SiO_2 is usually less than in the case of the Naco slags. This is probably due to differences in temperatures used in working the heats of the two kinds of steel. Unfortunately, accurate temperature observations for the working of the heats involved are not available although it is fair to make the general observation that Naco heats were worked and tapped hotter than the Grade "B" heats. This is reflected in the difference in levels of the SiO_2 content as noted. It may be following the principle that the slag will try to keep itself saturated with SiO_2 at all temperatures and that the higher the temperature the greater the amount of SiO_2 required for saturation.

Slag Viscosity as Related to Manganese Recovery. In the authors' melting practice, one of the principal uses made of slag control is that of a guide or indicator as to the recovery of manganese from the final addition. Before calculating these additions one slag

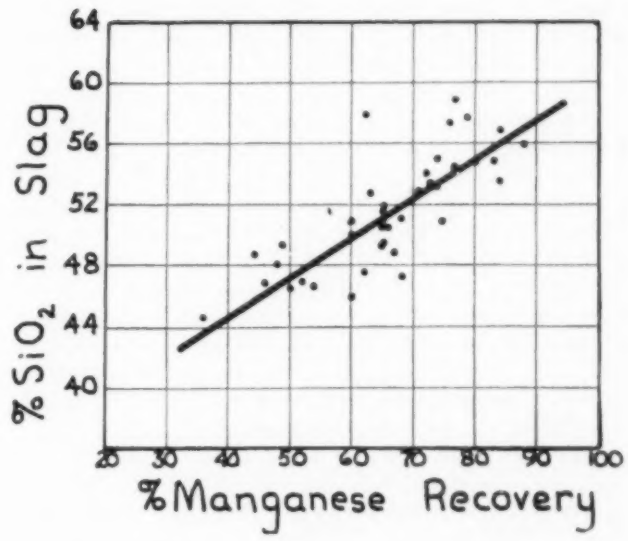


Fig. 21—Relationship of SiO_2 content of the slag to the manganese recovery. A direct proportion is indicated.

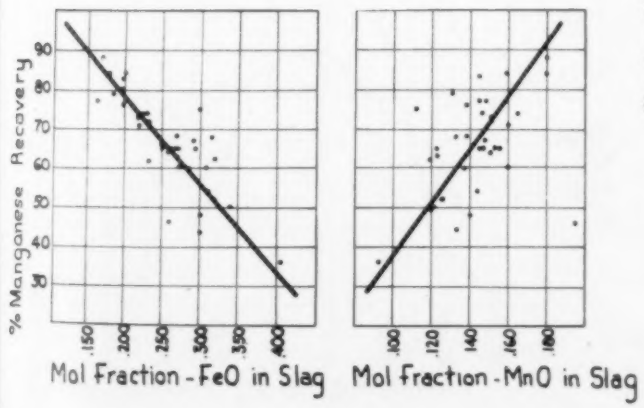
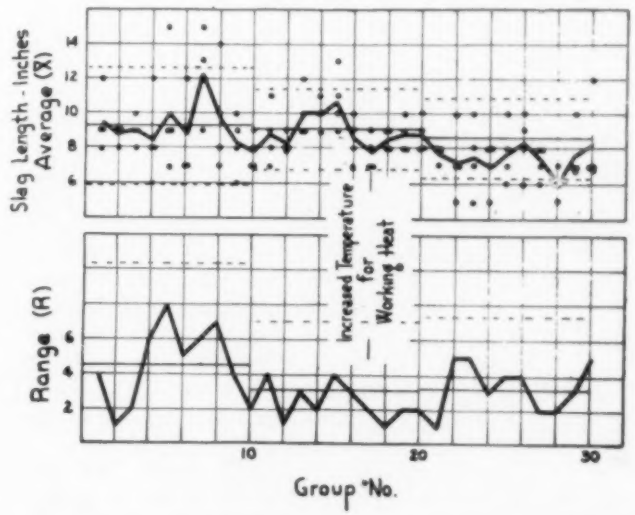
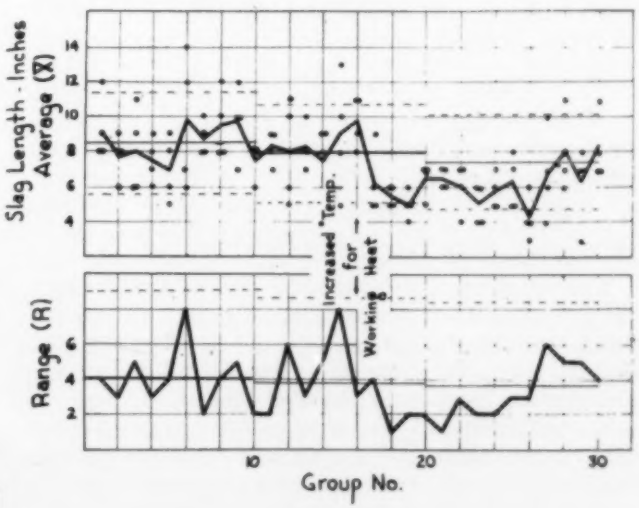


Fig. 20—Manganese recovery plotted vs. mol fraction of FeO and MnO in slag, with the $(CaO)_2$-SiO_2 deducted.





Figs. 22 (top) and 23—Control records of viscosity by heats, kept by two operators over the same period.

sample is poured into a viscosimeter and another onto an iron plate. The latter sample is air cooled until it is black, finish cooled in water, and then broken. From the color of the fractured section and the appearance of the outside of the slag cake the probable recovery of manganese is estimated.

This is a fairly accurate indication but it would be quite convenient if the measure of the viscosity of the slag could be taken as a measure of manganese recovery. To determine whether or not this would be possible, the calculated manganese recovery for a group of Grade "B" heats and for a group of medium manganese heats was plotted against the viscosity of the slags which were taken just before the final additions. These relationships are shown in Figs. 16 and 17. While there is some correlation in the case of the Grade "B" steel, there is only random scatter in the case of the medium manganese steel.

Using the data from Grade "B" heats, weight per cent FeO in the slag can be plotted against the actual manganese recovery, as in Fig. 18, to show a fairly close relationship. The relationship between weight per cent of MnO and of manganese recovery is not so well defined. If mol fractions of FeO and of MnO are used instead of weight per cent, a better correlation is found, as shown in Fig. 19. If the $(CaO)_2$-SiO_2 is deducted, the correlation remains practically unchanged as shown in Fig. 20.

As would be expected, the manganese recovery varies inversely as the FeO content of the slag and directly as the MnO content. That this is the direction these relationships should take is indicated by the chemistry of the process. In the bath, FeO + Mn = MnO + Fe and therefore,

$\frac{(MnO)\,(Fe)}{(FeO)\,(Mn)} = K$, where () indicates metal constituents.

Since the bath is largely (Fe), (Fe) = 1

and $\frac{(MnO)}{(FeO)\,(Mn)} = K$,

Now $[MnO] = \alpha\ (MnO)$

and $[FeO] = \beta\ (FeO)$

Where [] indicates slag constituents and α and β are numerical coefficients.

then $\frac{[MnO]}{\frac{\alpha}{\beta}\,(Mn)} = K_1$

and if $K_2 = K_1 \frac{\alpha}{\beta}$,

then $\frac{[MnO]}{[FeO]\,(Mn)} = K_2$ and $(Mn) = \frac{[MnO]}{[FeO]} \cdot \frac{1}{K_2}$

The manganese recovery also varies directly with the per cent SiO_2 in the slag, as shown in Fig. 21. For a constant level of CaO content, the slope of the SiO_2 line will be about the same as the resultant lines obtained by combining the FeO and MnO lines. In other words, the expected manganese recovery of a heat will increase as the SiO_2 and MnO of the slag increase and as the FeO of the slag decreases.

Now when the melter notes the appearance of a slag cake or measures the viscosity of a slag, he is determining the integrated effects of these three variables and, as a result of experience, these observations become a very useful guide in calculating the recovery from the manganese addition. As a matter of practice the authors depend almost entirely on experience gained in estimating the manganese recovery by appearance of the slag cake, using the viscosity measurement as a check. Using these observations, much better manganese control is obtained than if they were entirely neglected.

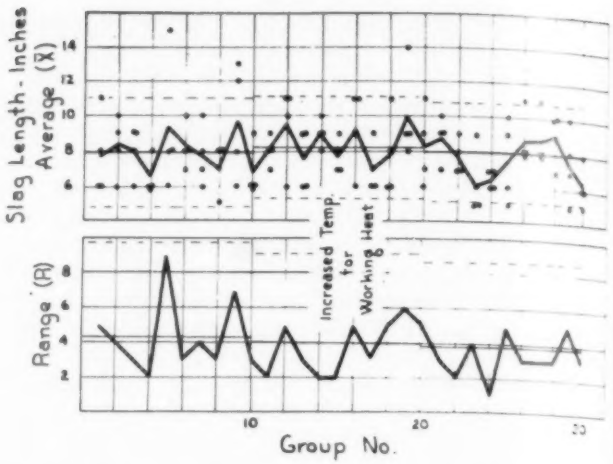


Fig. 24—Viscosity-heats record kept by third operator.

Slag Viscosity as a Measure of Melter Performance. One useful application of slag viscosity measurements is that of determining to some extent the differences in technique between operators. To accomplish this, the authors keep a quality control chart for each operator on which is recorded the viscosity measurements, as reported heat by heat. Figs. 22, 23 and 24 show the results reported by three operators over the same period.

It will be noted that these are the type of control chart adopted during the war by the Ordnance De-

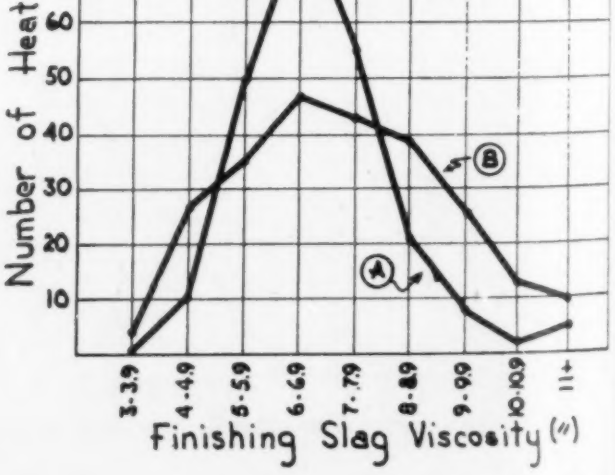


Fig. 25—Frequency distribution curves of slag viscosity for same type of steel melted in two different units.

partment and set forth in their "Steel Castings' Quality Control Plan." In this scheme, four successive readings are plotted on the same line and their average is also plotted on this line. The difference between maximum and minimum values is plotted at the bottom of the page, giving a "Range" value. When ten groups (or 40 results) are plotted, the upper and lower control limits are calculated for the group. These represent three times the standard deviation and for the system to be operating within the limits of chance cause, the average for each group must be within these limits. The same is also true for the range values.

Considering operator "A," the control was poor between groups 5 and 8 but it improved and stayed at a fairly constant level during the rest of the period under consideration. The level at which he was operating has been gradually decreasing, indicating that his method of operation has evidently been changing. Among the heats in the first ten groups he had a number that had a slag with a viscosity of over 10 in., before the final additions were made. About the time of the sixteenth group, the operators were told that they should operate their furnaces a little hotter and this change reflects itself in the change in level noted for the groups numbered 20 to 30.

Now considering operator "B" (who is operating under the same conditions as operators "A" and "C"), it is noted that his range is somewhat more erratic than that of operator "A." The level of his readings averages about an inch less in viscosity than that of "A," indicating that he probably operates the furnace a little hotter than "A" does. When given orders to operate hotter (group 16 on) it will be noted that his level dropped much faster than "A's" did and he seemed to be operating too hot, for in groups 27–30 it will be noted that he is returning to somewhat the level he had reached before he changed his practice.

Operator "C" seems to operate with a range about as great as "B" and at a level about the same as "B's" average. The striking thing is that he operates at a uniform level during all the time under consideration. His average group values fluctuate uniformly about the average line for each 10 groups of 40 points, and the average line stays constant for all 30 groups.

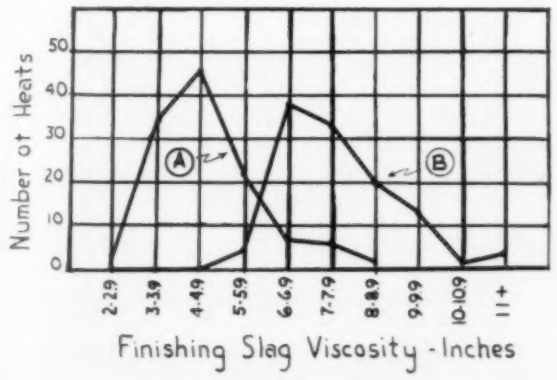


Fig. 26—Slag viscosity distribution curves for two types of steel melted in the same unit. "A" represents high carbon steel and "B" medium manganese steel.

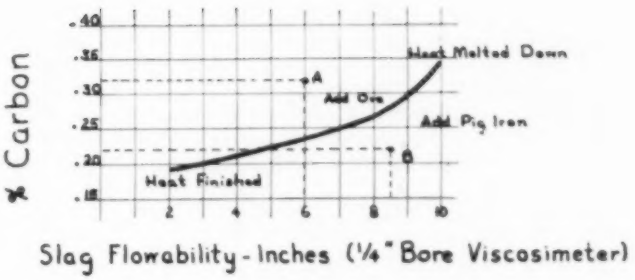


Fig. 27—Locke's graph, illustrating the principles of application of slag viscosity measurements to the control of ore or pig iron additions following meltdown.

It has been observed that this is a good picture of how these men actually work, so far as getting uniformity of results is concerned. It is interesting to note that when the authors first started using the viscosimeter and recording the results, they were much more erratic than they are at present. It is believed that the operators now have a measure of what they observe when they look at a piece of slag and that the orderly analyses of these measurements has been an aid in pointing out weaknesses in individual operation and, therefore, has been instrumental in securing and maintaining more uniform operation.

One interesting corollary that has to do with the tabulation of the viscosity measurements is the use of these measurements to compare melting practices on different units or for different types of metal. For example, in Fig. 25, are shown (in the form of frequency distribution curves) the results obtained on two different melting units, making the same type of steel. It is apparent that unit A is operating under better control than unit B, for the distribution curve is more nearly approaching the ideal curve in A than when it is flattened out as in B. From such an indication as this, it is possible to determine and overcome the weaknesses in furnace operation.

Fig. 26 shows the distribution curves for two types of steel made in the same melting unit. Type A represents the high carbon steel heats referred to earlier in the paper while type B is from the medium manganese steel heats. The difference may largely be ascribed to differences in temperature encountered in working the heats, as previously described. Type A operation requires a higher metal temperature before the addition of pig iron so that after the addition the metal may not be chilled too much. In any event, the difference is quite a marked one, which the viscosity test measures very well.

Slag Measurements Used to Control Working of the Heat. One of the chief uses of slag measurements is that of a control in determining the working of a heat from the time it is melted in until it is ready to tap. The word control is used here in the sense that a heat may be caused to follow certain desired trends in the meltdown and refining periods and thus produce a specified steel in a uniform manner.

For example, a slag sample taken at meltdown may show a length of from 4 to 18 in. as was shown earlier in the discussion (Fig. 2). If the length is short (4 in.

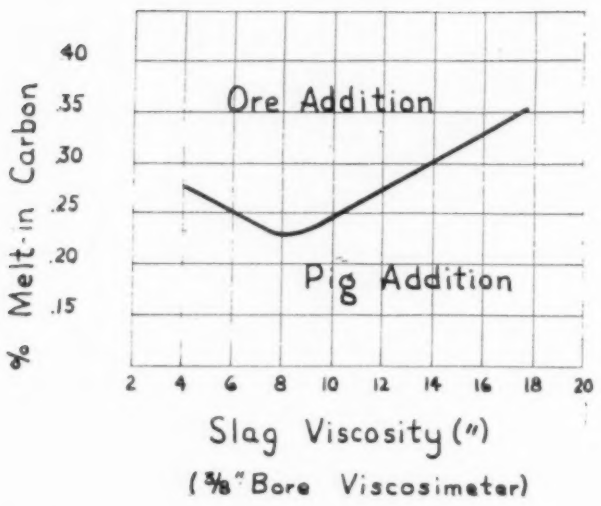


Fig. 28—Authors' schematic representation of additions control procedure similar to that described in Fig. 27.

for example), the operator knows that probably his meltdown power input was such that it produced excessive temperatures which reduced silica from the banks, giving a high residual silicon in the bath. This has been shown to be the case in Fig. 10.

It is as necessary to reduce the silicon to a fairly low value as it is to reduce the carbon to the desired minimum. Consequently, knowing the carbon level, the operator is in a position to know whether to add pig iron to get the carbon high enough so that he can add ore to get the desired action necessary to reduce the silicon or whether he can proceed at once with the ore addition and reduce both carbon and silicon simultaneously. Likewise, if the slag length at meltdown is long, indicating a high FeO content, the operator knows that he should immediately add pig iron to the bath and thus reduce the FeO content before he proceeds with the heat.

This principle is shown schematically by Locke[9] as applied to his practice, and his graph is reproduced in Fig. 27. It should be noted that the curve used does not represent the relationship between carbon and slag length (or FeO) but is a purely empirical curve to show the furnace practice to be followed, so far as pig iron and ore additions are concerned. The authors have adopted a somewhat similar scheme which is shown in Fig. 28.

The merit of such control procedure is that in this way the slag condition is adjusted throughout the working of the heat so that the heat works properly and is in the desired condition when the final additions are to be made. During the working period small adjustments in the characteristics of the slag are made by the addition of burnt lime, sand and finely powdered ferro-silicon and ferro-manganese.

Correlation between Tensile Properties of the Steel Produced and Slag Viscosity. It is generally recognized that melting practice has a marked influence over the physical properties of the steel produced. Since the slag characteristics are a measure of some of the variables that may be present in the making of heats, the authors were interested in determining if there is any correlation between tensile properties of the steel produced (assuming the heat treatment to be uniform) and slag characteristics, as measured by the slag viscosity.

An exhaustive study along this line has not been made but Fig. 29 shows the relationships between the viscosity of the slag just before the final additions are added and the relative number of tensile tests from the heats under consideration that have failed in elongation and/or reduction of area on the first specimen tested. Obviously many more heats are represented in the range of 5 to 10 in. slag length than are represented by the extreme lengths. To minimize somewhat this difference, heats were grouped, as indicated (escpecially for the range 12 to 15 in.) so as to give this point a little more weight.

While the conclusions drawn from such a scheme must be very general in nature, they follow the expected pattern. That is, heats worked so as to end with a viscous slag usually have had considerable residual silicon which tends to give poor sonims and therefore low ductility. Those worked to end with a fluid slag will have a high FeO and will tend to have porosity in both test bars and castings.

The authors try to have a slag length of 5 to 10 in. before making final additions, feeling that this represents the condition of the metal that will give the best results so far as castings and tensile tests are concerned.

Summary.—This discussion has tried to show some of the possibilities of the use of slag control as an aid in the production of steel by the acid electric process.

The use of the viscosimeter as a measure of the characteristics of the slag has been discussed, and consideration given to the correlation that might exist between such items as:

a. The slag viscosity and its chemical composition;

b. The slag viscosity and composition as related to conditions existing at steps in the melting cycle;

c. The slag composition and corresponding bath analysis; and

d. The slag composition and bath temperature.

Some practical applications of slag measurements to

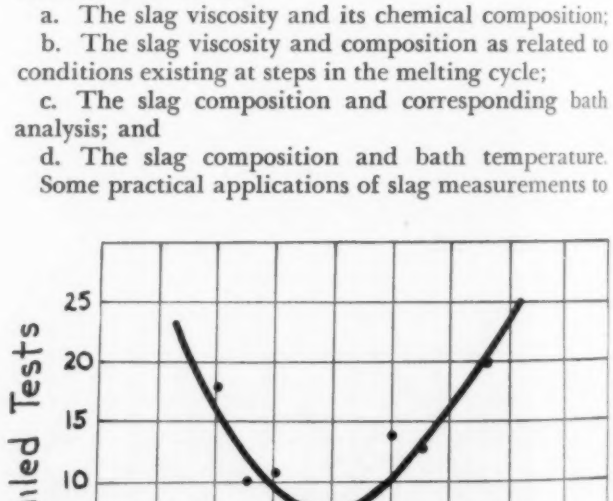


Fig. 29—Relationship between viscosity before final additions and relative number of unsatisfactory tensile tests for steels from the heats under consideration.

the control of several steps in the process indicate:

a. That a combination of the use of visual examination and slag viscosity measurements provides a good indication of the manganese recovery to be obtained from the final addition of manganese;

b. That the orderly tabulation and analysis of "final" viscosity measurements (in the form of quality control charts) can be made a useful tool in directing the technique employed by the several melters. Likewise it is also a useful tool for comparing the performance of several melting units. The term "final" viscosity measurement is used to mean the measurement of the slag sample taken just before the final additions are made to the bath.

c. That the use of slag measurements throughout the working of a heat may be a useful guide on how to proceed with the heat. Especially is this true at the beginning of the working period, when a consideration of the melt-in carbon and slag characteristics will indicate what procedure to use.

d. That there is some correlation between slag characteristics and the resulting physical properties of the metal produced and that, for best operation, the slag should be shaped up to have a viscosity that falls within certain limits before the final additions are made and the heat tapped.

Acknowledgments

The authors express sincere thanks to all their colleagues who have cooperated in helping to carry out the present investigation. Special thanks are due Dr. H. A. Schwartz for his advice and encouragement in planning the investigation and in organizing the findings into their present form, and Messrs. W. L. Woody and W. M. Ewing, not only for their permission to use the data but also for their encouragement to publish it.

Messrs. William Johnson, Albert Fisher and Andrew Ellis, of the Metallurgical Department were especially helpful in carrying out the investigation and the authors' thanks go to them.

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DISCUSSION

Chairman: C. H. LORIG, Battelle Memorial Institute, Columbus, Ohio

Co-Chairman: E. C. TROY, Dodge Steel Co., Philadelphia

R. W. THOMAS[1]: Were the points which were shown on the quality control curve (Fig. 24) the slag viscosity values as actually found? Did you plot the viscosity as you went on with your heat?

MR. JOHNSON: Those points were viscosity readings before the melter made the final additions. The melter did not know we were going to use these readings, and on the report form for each heat, he recorded the viscosity before he made the additions. We took those readings and plotted them.

J. K. MCBROOM[2]: Does anyone have information on work of a similar nature applying to basic slag? Has anyone tried to check basic slag viscosities against carbon content or any other property of the metal?

CHAS. LOCKE[3]: I can refer you to a lengthy bibliography which I think would help you in your basic slag practice. This work was introduced by the open hearth metallurgists on basic slag work and if you search back for many years in the technical journals you will find numerous references. Some of these are as follows:

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17. W. O. Philbrook and A. H. Jolly, "A Survey of Slag Control Methods," *Blast Furnace and Steel Plant*, vol. 32, p. 793, July, and p. 938, Aug. (1944).

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19. N. H. Bacon, "Slag Control," *Iron and Steel*, vol. 18, p. 86, March, and p. 124, April (1945).

20. M. Tenenbaum, C. C. Brown, "Application of ph Slag—Basicity Measurements to Basic Open Hearth Phosphorus Control," *Metals Technology*, vol. 12, T. P. 1863, Aug. (1945).

[1] General Electric Co., Schenectady, N. Y.
[2] Stainless Foundry and Engineering Co., Milwaukee, Wis.
[3] West Michigan Steel Foundry, Muskegon, Mich.

21. J. S. Coulter, "Determining Lime-Silica Ratios for Open Hearth Control by Mathematical Computation Using FeO and Fe_2O_3," *Blast Furnace and Steel Plant*, vol. 33, p. 1242, Oct. (1945).

J. B. CAINE[4]: My question refers to the relationship between iron oxide in the slag and slag viscosity. I am more interested in iron oxide in the slag than anything else from the control angle. The range that you have at any individual slag viscosity (Fig. 7 and 8) is about equal to the total range over-all slag viscosity. From a control viewpoint, if you had a slag viscosity of, let us say, 8 in., the individual points range from about 9 to about 27 per cent iron oxide, and that is about equal to the total range. Could I use slag viscosity as a method of control of iron oxide or tell what the iron oxide is from slag viscosity?

MR. JOHNSON: I do not think you can.

MR. CAINE: I agree. I do not see how you possibly could. There have been some statements made in past literature saying that you could, and would you say that Fig. 7 and 8 would be more or less proof that you could not?

MR. JOHNSON: Yes, I would say that you cannot predict or control iron oxide from the slag viscosity. When you measure slag viscosity, you have integrated the SiO_2, plus MnO, plus CaO, plus FeO, and while the FeO and the SiO_2 are practically reciprocals of the other, yet these other constituents come in, so they do influence the basic side more than they influence the acid side. Your correlation coefficient between FeO and slag viscosity, and SiO_2 and slag viscosity shows that you have a much poorer relationship for the FeO than for the SiO_2 relationship.

MR. LOCKE: I seem to remember some work that Mr. Caine did on partition constants at various stages of the relationship between the iron oxide in the slag and iron oxide in the bath. I think he will admit that there is a rough correlation, and we fail because the chemical determination of the FeO in the slag is usually not too accurate as we try to correlate it with viscosity. But still, from what I remember he pointed out, there is something that can be used.

MR. CAINE: When I determined the partition constant between iron oxide in the slag and that in the metal, they were both run analytically in the laboratory. There was no viscosity factor entering into either factor. What I am trying to get at here is that there is no relationship between any one single element, other than SiO_2 and viscosity. In other words, if I wanted to control the iron oxide in the slag, I could not control it by means of viscosity, even though I will agree that the reading that you get is useful. The range of iron oxide at each viscosity is too great to say that a given viscosity corresponds to a certain iron oxide content.

MR. LOCKE: My question, Mr. Johnson, is about that portion of your melting where you finish and then hold for equilibrium. I noticed that your silica in the slag is not increased as it would occur with us if we held. Is that done with power on or power off?

[4] Sawbrook Steel Castings Co., Wyoming, Ohio

MR. JOHNSON: It is done on the C-tap, with a very small amount of power on.

CO-CHAIRMAN TROY: A test is only as good as the sample and it is apparently a difficult job to get a good sample of slag out of an electric furnace, particularly a small electric furnace. Our experience in viscosity has shown us that the melter must be careful in selecting his slag sample.

C. E. SIMS[5]: This relationship between slag and metal can be very useful if it is used properly, as I think the evidence has shown so far, though I think we have to avoid the danger of going too far with it and expecting too much. There seems to be no good evidence that we have any true equilibrium relationships between slag and metal. In fact, the accumulated evidence indicates that we never do get a true equilibrium, so that partition constants would not apply very accurately.

A recent paper by Urban and Derge* points out that fact very clearly. It refers to basic practice and they were studying the refining period, where they had a reduced slag, and every bit of the evidence they accumulated indicates there was no relationship of an equilibrium character between the slag and the metal. In other words, when the slag is very high in iron oxide, it will feed iron oxide to the metal, but there is no reverse flow of oxygen. The slag can never deoxidize the steel.

This discussion about the relationship between FeO content of the slag and viscosity seems to have been a little bit obscured by some of the facts that have been brought out. If there is a direct relationship between SiO_2 content and the viscosity, then we could not expect a direct relationship between FeO and viscosity, but there would have to be a relationship between total basic oxides, that is, FeO, MnO, and CaO. We could only have a relationship between FeO and viscosity when MnO and CaO are held constant. But the plot presented showing the relationship between FeO in the slag and the carbon in the metal did not look like any sort of a correlation to me, in spite of the fact a curve was drawn through the points. But that again shows the type of relation between the slag and the metal itself and apparently we can regard the slag mainly as a reservoir and a supplier of oxygen to the steel during the boil. When there is considerable turbulence, there is a rapid interchange or a rapid flow of oxygen from the slag to the steel, but it seems that we never get the reverse transfer.

CHAIRMAN LORIG: In Fig. 29 the authors show a relationship between viscosity before final additions and the relative number of unsatisfactory tensile tests of steel from the heats under consideration. Have you made any analysis of the steel to determine what the factors are which cause the steels to have unsatisfactory tensile properties? Is it the result of unsoundness or poor inclusion distribution?

MR. JOHNSON: Tests out of heats where the slags have the higher viscosity are very likely to be unsound due to holes. Samples from heats with the lower viscosity are very likely to exhibit a poor inclusion form in the metal. In conclusion, we would like to express our thanks to those who have contributed to the discussion of the paper.

[5] Battelle Memorial Institute, Columbus, Ohio

* Urban and Derge, "Oxygen in Basic Electric-furnace Baths," AIME Tech. Pub. No. 2185 (1947).

THERMOCHEMICAL ANALYSIS OF COMBUSTION IN A CUPOLA

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ABSTRACT

A study of the thermochemical reactions in a cupola, based on certain simplifying assumptions, has resulted in the formulation of two equations by which the heat of combustion and maximum combustion temperature may be calculated. These equations were developed after a large number of calculations of these quantities were made, and conform with the calculated data within a reasonable degree of accuracy. It is possible, through the use of such equations, to immediately estimate the relative effect of preheating of blast air, or of removal of moisture from the blast, on the heat of reaction or the maximum temperature attainable. On the assumption that the gas analysis in the cupola is quickly reduced to near 6 per cent CO_2, the effectiveness of these conditioning treatments can be appreciated. While the maximum combustion temperature is never reached in cupola operation, this temperature represents the relative potential from which heat is drawn and is an indication of the effectiveness of the operation. It must be remembered that combustion temperature is dependent only on the conditions of reaction and not on the quantity of coke used. When temperature is too low an infinite amount of coke will not ***melt iron.***

THE CUPOLA is recognized as the most economical furnace used for melting iron. This arises from the intimate admixture of fuel and charge which promotes a highly efficient heat transfer. It so happens that but little has been written about the conditions existing within the cupola, while much has been written about the general features of its operation as these affect economy and quality of product. A "Handbook of Cupola Operation" has been published by the American Foundrymen's Association and should be studied by those interested.

The difficulty of getting pertinent data with which to analyze the conditions within an operating cupola are quite apparent. Temperatures are almost impossible of measurement and gas sampling is very uncertain. Yet with these handicaps it is possible, with meager data, to approximate the conditions existing through the application of thermochemical reasoning directed toward the estimation of heat of combustion, heat contents of combustion gases, and maximum combustion temperatures. Such a treatment, based on certain simplifying assumptions, is the purpose of this paper.

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Thermochemical Analysis

I. *General*

The primary chemical reaction which produces heat in the cupola is the burning of coke to carbon dioxide and carbon monoxide. The percentage composition of these gases in the combustion products at the various levels in a cupola has received but little attention. Thompson and Becker,[1] Fugo and Thompson,[2] Massari,[3] Massari and Lindsay,[4] and Vial,[5 6] have studied the subject directly. The works of Bone, Reeve and Saunders[7] and of Richards[8] are directly applicable, although these were done primarily for other purposes. Of the various contributions, the author considers those of F. K. Vial of the most value.

The importance of the gas composition in the different portions of the cupola can only be realized if one appreciates the temperature limitations which these compositions dictate. For instance, a high percentage of carbon monoxide does not allow the existence of a high temperature. That such a percentage of carbon monoxide exists just above the combustion zone is part of the subject of this paper. Strauss, at the Buffalo annual meeting of the American Foundrymen's Association in 1944, in a general discussion, reported that only 3 to 5 per cent carbon dioxide was present a short distance above the tuyeres.

II. *Thermochemical Calculations*

In order to calculate the thermochemical quantities involved in a study of cupola combustion, certain assumptions were made.

1. Fuel was considered to be pure carbon. While coke consists of carbon, ash, sulphur, moisture, etc., these impurities were treated as extra burden. Any heat coming from the combustion of sulphur was neglected, but would partially compensate for any error arising from the basic premise. One pound mol (12 lb) of carbon was the unit for calculation. The unit of heat is the Btu.

2. The combustion zone was isolated for special study and coke entering this zone was assumed to have been preheated to 3000 F. and to contain 14,600 Btus. This temperature is an approximation of that probably existing just above this zone. It is unlikely that the coke reaches the existent flame temperature be-

TABLE 1.—THERMOCHEMICAL REACTIONS TAKING PLACE IN A CUPOLA

No.	Thermochemical Reaction	H in Btus
1	C (75°F.) = C (3000°F.)	14600
2	$C + \frac{1}{2} O_2 = CO$	−52200
3	$C + O_2 = CO_2$	−174600
4	$C + CO_2 = 2CO$	70200
5	$C + H_2O = CO + H_2$	70800
6	$CaCO_3 = CO_2 + CaO$	78200

TABLE 2.—HEAT CONTENTS OF GASES AT DIFFERENT TEMPERATURES AS TAKEN R. C. HECK [9]
BTU'S PER MOL.

Temperature		Gases					
°F	°R	O_2	N_2	H_2O	CO_2	CO	H_2
140	600	423	418	481	547	419	416
440	900	2546	2520	2940	3550	2528	2505
740	1200	4885	4676	5518	6913	4704	4608
1040	1500	7279	6905	8236	10538	6966	6727
1340	1800	9749	9214	11099	14362	9309	8873
1640	2100	12279	11596	14114	18330	11721	11065
1940	2400	14848	14031	17272	22406	14188	13300
2240	2700	17453	16511	20555	26570	16696	15588
2540	3000	20091	19028	23951	30803	19239	17931
2840	3300	22759	21575	27449	35091	21809	20320
3140	3600	25454	24146	31039	39243	24399	22754
3440	3900	28179	26737	34704	43709	27007	25229
3740	4200	30928	29345	38430	48190	29630	27740
4040	4500	33701	31967	42210	52614	32267	30283
4340	4800	36501	34601	46043	57063	34915	32858
4640	5100	39325	37246	49919	61539	37573	35459
4940	5400	42168	39898	53828	66030	40237	38084

TABLE 3.—HEAT CONTENTS OF GASES GIVEN IN TABLE 2 SUMMARIZED BY METHOD OF LEAST SQUARES

Reference is 75°F. = 535°R. Temperature is in °R.

Gas	Heat Content Equation
N_2	$-4050 + 7.094T + 0.0001976T^2$
O_2	$-4195 + 7.400T + 0.0002240T^2$
H_2	$-3540 + 6.483T + 0.0002283T^2$
CO_2	$-6530 + 11.142T + 0.0004434T^2$
CO	$-4135 + 7.210T + 0.0001907T^2$
H_2O	$-4650 + 7.868T + 0.0005563T^2$

fore entering the region of near approach to equilibrium between carbon monoxide and carbon dioxide.

3. Dry air was assumed to consist of 21 per cent O_2 and 79 per cent N_2 by volume. Moist air was treated as having been diluted in oxygen and nitrogen with the water acting as an additional source of oxygen.

4. The full heat of reaction at ordinary temperature was used to heat the combustion products to that temperature at which the heat contents become equal to the heat of reaction. This gave the maximum theoretical combustion temperature designated as *Tm*.

5. Dissociation of the gaseous compounds resulting from combustion has been ignored. Any dissociation would reduce the temperature obtainable from combustion. Dissociation is quite extensive at the higher temperatures when CO_2 is the chief constituent present.

A number of reactions taking place in a cupola are listed in Table 1. Heat content data for the various gases present were selected from R. C. H. Heck [9] and are given in Table 2. Equations for the heat content of these gases, calculated by the method of least squares, are given in Table 3. Specific heat equations may also be found in the *Handbook of Cupola Operation*, page 351. The specific heat of carbon was taken from Chipman [10] and is

$$Cp \text{ (carbon)} = 1.2 + 0.0050T\text{-}0.0000012T^2$$

The heat content of carbon at 3000 F was calculated from this equation. The reference temperature in the work of Heck is 75 F at which the heat content is zero.

In making these calculations, 12 lb. of carbon or 1 lb. mol has been burned to give various proportions of carbon dioxide and carbon monoxide. Following the convention of Massari and Lindsay:[4]

$$N_v = \frac{\%\ CO_2}{\%\ CO_2 + \%\ CO} = \text{fraction of carbon}$$

TABLE 4.—CALCULATED MIXTURES OF BLAST AIR AND COMBUSTION PRODUCTS FOR SEVERAL STAGES OF COMBUSTION AND MOISTURE CONTENTS

		Combustion Air			Combustion Products							
		Mols of			Mols of				Chemical Analysis by Volume, Per cent			
N_v	H_2O Per cent	O_2	N_2	H_2O	CO_2	CO	N_2	H_2	CO_2	CO	N_2	H_2
0.00	0.0	0.5	1.88	—	0.00	1.00	1.88	—	0.00	34.7	65.3	—
0.20	0.0	0.6	2.26	—	0.20	0.80	2.26	—	6.14	24.5	69.3	—
0.40	0.0	0.7	2.63	—	0.40	0.60	2.63	—	11.00	16.5	72.5	—
0.60	0.0	0.8	3.01	—	0.60	0.40	3.01	—	15.00	10.0	75.0	—
0.80	0.0	0.9	3.39	—	0.80	0.20	3.39	—	18.30	4.6	77.1	—
1.00	0.0	1.0	3.76	—	1.00	0.00	3.76	—	21.00	00.0	79.0	—
0.00	2.0	0.477	1.79	0.046	0.00	1.00	1.79	0.046	0.00	35.3	63.1	1.6
0.20	2.0	0.572	2.15	0.056	0.20	0.80	2.15	0.056	6.42	24.9	67.3	1.7
0.40	2.0	0.667	2.51	0.065	0.40	0.60	2.51	0.065	11.20	16.8	70.2	1.8
0.60	2.0	0.763	2.87	0.074	0.60	0.40	2.87	0.074	15.2	10.1	72.8	1.9
0.80	2.0	0.858	3.23	0.083	0.80	0.20	3.23	0.083	18.5	4.6	75.0	1.9
1.00	2.0	0.954	3.59	0.093	1.00	0.00	3.59	0.093	21.2	0.0	76.7	2.0
0.00	4.0	0.455	1.71	0.090	0.00	1.00	1.71	0.090	35.7	35.7	61.1	3.2
0.20	4.0	0.546	2.05	0.108	0.20	0.80	2.05	0.108	25.3	25.3	65.0	3.4
0.40	4.0	0.637	2.39	0.126	0.40	0.60	2.39	0.126	17.0	17.0	68.1	3.6
0.60	4.0	0.728	2.74	0.144	0.60	0.40	2.74	0.144	10.3	10.3	70.6	3.7
0.80	4.0	0.819	3.08	0.162	0.80	0.20	3.08	0.162	4.7	4.7	72.6	3.8
1.00	4.0	0.910	3.42	0.181	1.00	0.00	3.42	0.181	0.0	0.0	74.4	3.9
0.00	6.0	0.434	1.63	0.132	0.00	1.00	1.63	0.132	0.0	35.9	59.3	4.7
0.20	6.0	0.521	1.96	0.158	0.20	0.80	1.96	0.158	6.3	25.4	63.2	5.0
0.40	6.0	0.608	2.29	0.185	0.40	0.60	2.29	0.185	11.4	17.1	66.2	5.3
0.60	6.0	0.695	2.61	0.211	0.60	0.40	2.61	0.211	15.5	10.4	68.7	5.5
0.80	6.0	0.782	2.94	0.237	0.80	0.20	2.94	0.237	18.9	4.7	70.6	5.6
1.00	6.0	0.866	3.26	0.264	1.00	0.00	3.26	0.264	21.8	0.0	72.3	5.8

burned to carbon dioxide. The arbitrary values for N_v selected for study were 0.00, 0.20, 0.40, 0.60, 0.80 and 1.00. A sufficiently complete picture of the general relationships was obtained by plotting the results of these six calculations for any arbitrary set of conditions with respect to temperature of preheating or moisture content of the blast air. The necessary oxygen, nitrogen and water vapor and the corresponding combustion products were calculated in the following manner:

Let x be the fraction of moist air which is water vapor and $1\text{-}x$ the fraction which is oxygen and nitrogen. The total oxygen can then be obtained by adding that from each source,

$$\text{Total } O_2 = \frac{0.50x + 0.21\,(1-x)}{0.29x + 0.21}$$

One may then write directly that

$$\frac{\text{mols } N_2}{\text{mols } O_2} = \frac{0.79\,(1-x)}{0.29x + 0.21}$$

$$\frac{\text{mols } H_2O}{\text{mols } O_2} = \frac{x}{0.29x + 0.21}$$

Since N_v is the fraction of carbon which is burned to CO_2 and $1\text{-}N_v$ that fraction which is burned to CO,

$$N_v \times \frac{0.79\,(1-x)}{0.29x + 0.21} = \text{mols of } N_2 \text{ used in air which burns carbon to } CO_2.$$

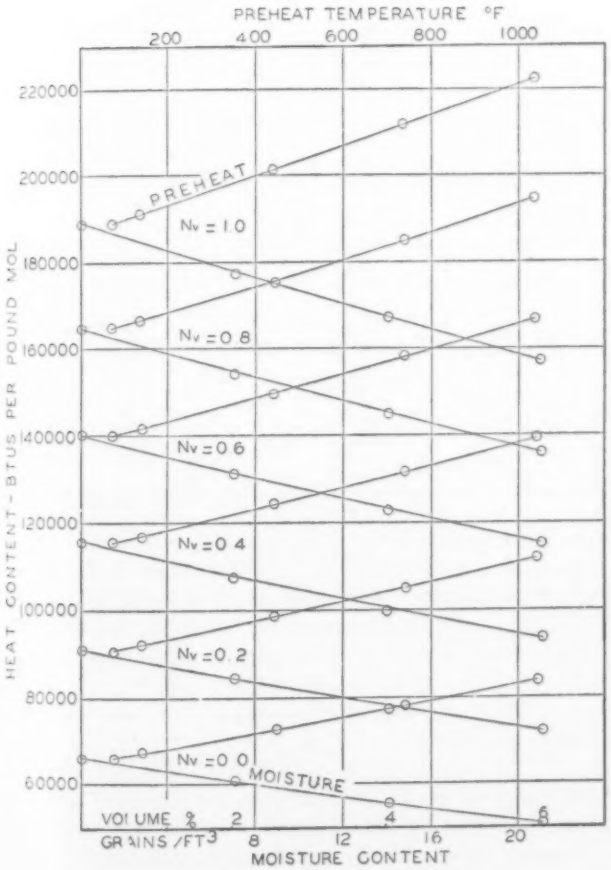


Fig. 1—Heat of combustion of carbon in air under different conditions of preheat and moisture content.

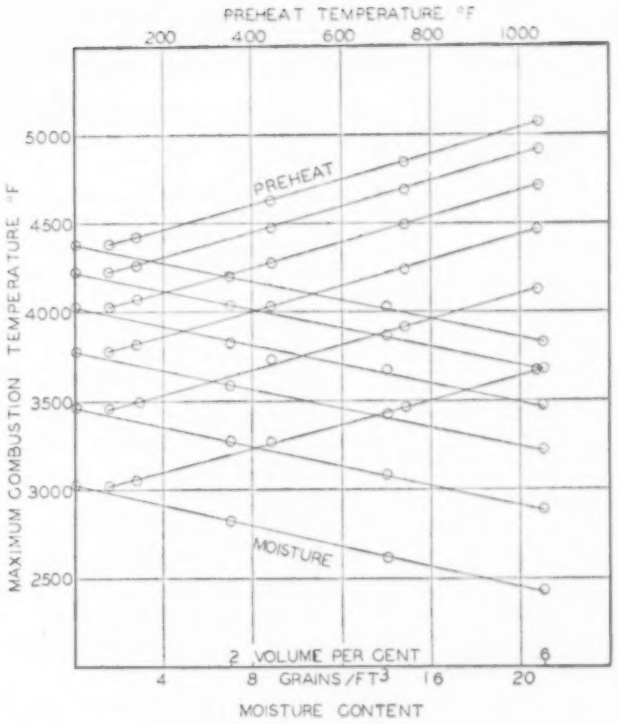


Fig. 2—Effect of preheat and moisture content of blast air of maximum theoretical combustion temperature.

and

$$\frac{1-N_v}{2} \times \frac{0.79\,(1-x)}{0.29x + 0.21} = \text{mols of } N_2 \text{ used in air which burns carbon to CO.}$$

Summing:

$$\frac{1+N_v}{2} \times \frac{0.79\,(1-x)}{0.29 + 0.21} = \text{mols of } N_2 \text{ used in air burning one mol of carbon.}$$

Likewise, the mols of H_2O in the air and also of H_2 in the cupola gas, per mol of carbon burned, is

$$\frac{1+N_v}{2} \times \frac{x}{0.29x + 0.21}$$

The mixtures of combustion air and corresponding combustion products were calculated by these equations and are given for several moisture contents and stages of combustion in Table 4.

The quantities of heat contained in the combustion air at the time of entry into the cupola were calculated from the number of mols of the different gases present as determined from the above equations. Several stages of preheating were taken. The total heats of combustion were found by adding the heat contents of the combustion air, the heat content of carbon, the prorated heats of combustion of carbon to carbon monoxide and carbon dioxide, and subtracting the heat of dissociation of water vapor. The flame temp-

TABLE 5.—HEATS OF COMBUSTION OF CARBON, IN BTU'S WITH AIR HAVING DIFFERENT MOISTURE CONTENTS AND PREHEAT TEMPERATURES

N_v	H_2O, Per cent	Preheat Temperatures, °F						
		75	140	440	740	1040	1340	1640
0.00	0.0	66800	67800	72840	78040	83430	89000	94750
0.20	0.0	91280	92480	98530	104760	111230	117920	124820
0.40	0.0	115760	117160	124210	131490	139040	146840	154890
0.60	0.0	140240	141840	149900	158220	166850	175770	184970
0.80	0.0	164720	166520	175590	184950	194650	204690	215030
1.00	0.0	189200	191200	201380	211680	222460	233610	245100
0.00	2.0	61100	62080	67000	72080	77350	82800	88429
0.20	2.0	84440	85610	91490	97610	103930	110470	117220
0.40	2.0	107780	109140	116030	123140	130520	138150	146020
0.60	2.0	131110	132670	140540	148670	157090	165820	174810
0.80	2.0	154460	156210	165070	174210	183690	193510	203620
1.00	2.0	177800	179770	189590	199750	210180	221180	232420
0.00	4.0	55700	56650	61460	66430	71570	76910	82400
0.20	4.0	77960	79100	84870	90810	97000	103400	110000
0.40	4.0	100220	101560	108300	115230	122440	129900	137590
0.60	4.0	122480	123990	131690	139630	147870	156400	165200
0.80	4.0	144740	146450	155110	164040	173310	182910	192810
1.00	4.0	167000	168900	178510	188440	198740	209400	222400
0.00	6.0	50580	51520	56270	61170	66260	71540	76980
0.20	6.0	71810	72940	78640	84520	90630	96960	103490
0.40	6.0	93050	94370	101020	107890	115020	122400	130020
0.60	6.0	114270	115780	123380	131230	139370	147810	156520
0.80	6.0	135510	137200	145750	154580	163750	173250	183040
1.00	6.0	156740	158620	168120	177930	188110	198670	209550

TABLE 6.—MAXIMUM COMBUSTION TEMPERATURE °F WHEN CARBON IS BURNED WITH AIR HAVING DIFFERENT MOISTURE CONTENTS AND PREHEAT TEMPERATURES

N_v	H_2O, Per cent	Preheat Temperature, °F						
		75	140	440	740	1040	1340	1640
0.00	0.0	3018	3055	3257	3456	3671	3902	4125
0.20	0.0	3454	3495	3739	3914	4135	4352	4590
0.40	0.0	3780	3820	4029	4240	4460	4683	4915
0.60	0.0	4030	4070	4276	4490	4708	4936	—
0.80	0.0	4222	4262	4472	4688	4910	—	—
1.00	0.0	4378	4421	4633	4848	—	—	—
0.00	2.0	2825	2862	3066	3272	3484	4156	3931
0.20	2.0	3271	3316	3514	3720	3933	4491	4389
0.40	2.0	3592	3634	3843	4051	4267	4476	4725
0.60	2.0	3839	3879	4088	4298	4523	4952	4980
0.80	2.0	4040	4082	4293	4503	4726	—	—
1.00	2.0	4201	4241	4451	4665	4888	—	—
0.00	4.0	2609	2675	2872	3080	3294	3511	3740
0.20	4.0	3088	3126	3332	3542	3754	3974	4205
0.40	4.0	3420	3460	3662	3864	4090	4318	4538
0.60	4.0	3666	3710	3918	4122	4342	4569	4801
0.80	4.0	3867	3908	4117	4330	4547	4774	5010
1.00	4.0	4028	4070	4276	4490	4710	4936	—
0.00	6.0	2432	2467	2670	2880	3092	3215	3534
0.20	6.0	2885	2923	3125	3335	3550	3769	3994
0.40	6.0	3217	3252	3457	3667	3863	4106	4340
0.60	6.0	3465	3509	3712	3924	4141	4367	4593
0.80	6.0	3666	3707	3912	4121	4342	4565	4799
1.00	6.0	3828	3867	4070	4282	4502	4730	4960

erature was then calculated by finding, graphically, the temperature at which the sum of the heat contents of the combustion gases was equal to the heat of reaction. The results of several values of *Nv*, several moisture contents, and several stages of preheating are shown in Figs. 1 and 2, and the data are given numerically in Table 5 for the heat of combustion and in Table 6 for the maximum flame temperature.

The lines in Figs 1 and 2 are approximately linear and the slopes are given in Table 7. These slopes differ in a regular manner for the curves in Fig. 1, but the corresponding lines are almost parallel in Fig. 2. For all practical purposes, when the limited temperatures usually used for preheating of the air for cupola combustion are considered, the linear relationship is sufficient. For those conditions of combustion in which preheating approaches 1800 F second degree equations might be used with profit.

The heat of combustion of carbon in Btu as a function of *Nv* is given by the linear equation:

$$\triangle H = 66800 + 124200\ N_v$$

A similar non-linear equation for the maximum flame temperature, °F, is

$$Tm = 3030 + 2200\ N_v - 864\ N_v^2$$

With these equations, and the information given for the effects of preheating and moisture contents on the heat of combustion and flame temperature, expressions for the calculation of these quantities for any condition of combustion, temperature of preheating or moisture content were assembled under the as-

sumption that the several effects are additive when in combination. The resulting formulae are:

$$\triangle H = 66800 + 124200N_v + (17.4 + 17.3N_v)(t - 75) - (772 + 775N_v)\ g$$

$$T_m = 3030 + 2201N_v - 864N_v^2 + 0.71(t - 75) - (27.6 - 2.0N_v)\ g$$

In these equations the terms have the following significance:

$\triangle H$ = heat of reaction in Btu per pound mol of carbon
N_v = fraction of carbon burned to CO_2
t = preheat temperature in °F.
T_m = maximum combustion temperature in °F.
g = grains of water per cubic foot of blast air.

These equations have been checked throughout the range covered in Tables 5 and 6 and found to agree quite closely with the values given. Water vapor, up to 6 per cent, or 21 grains per cubic foot, is almost linear in its effect on combustion temperature. All other curves in Figs. 1 and 2 are concave upward.

There is a critical temperature in cupola operation which corresponds to that propounded by Johnson [11] for the blast furnace. This was estimated at 2700 F and the excess heat content in the combustion gases above this level is given in Table 8. It is from this reserve that heat for melting and superheating must be drawn. In terms of percentage, improvement by preheating or detriment by moisture content is very marked. That a large fraction of the total heat be above this critical temperature is essential for efficient operation. Data given later indicate that temperatures above those represented by $N_v = 0.20$ do not exist generally in the cupola except in those few inches just above the tuyeres where there is combustion to high carbon dioxide content and immediate reduction of the carbon dioxide to carbon monoxide. Figure 3, taken from Trinks,[12] illustrates the relative effectiveness of additional temperature in increasing the rate of heat transfer by radiation.

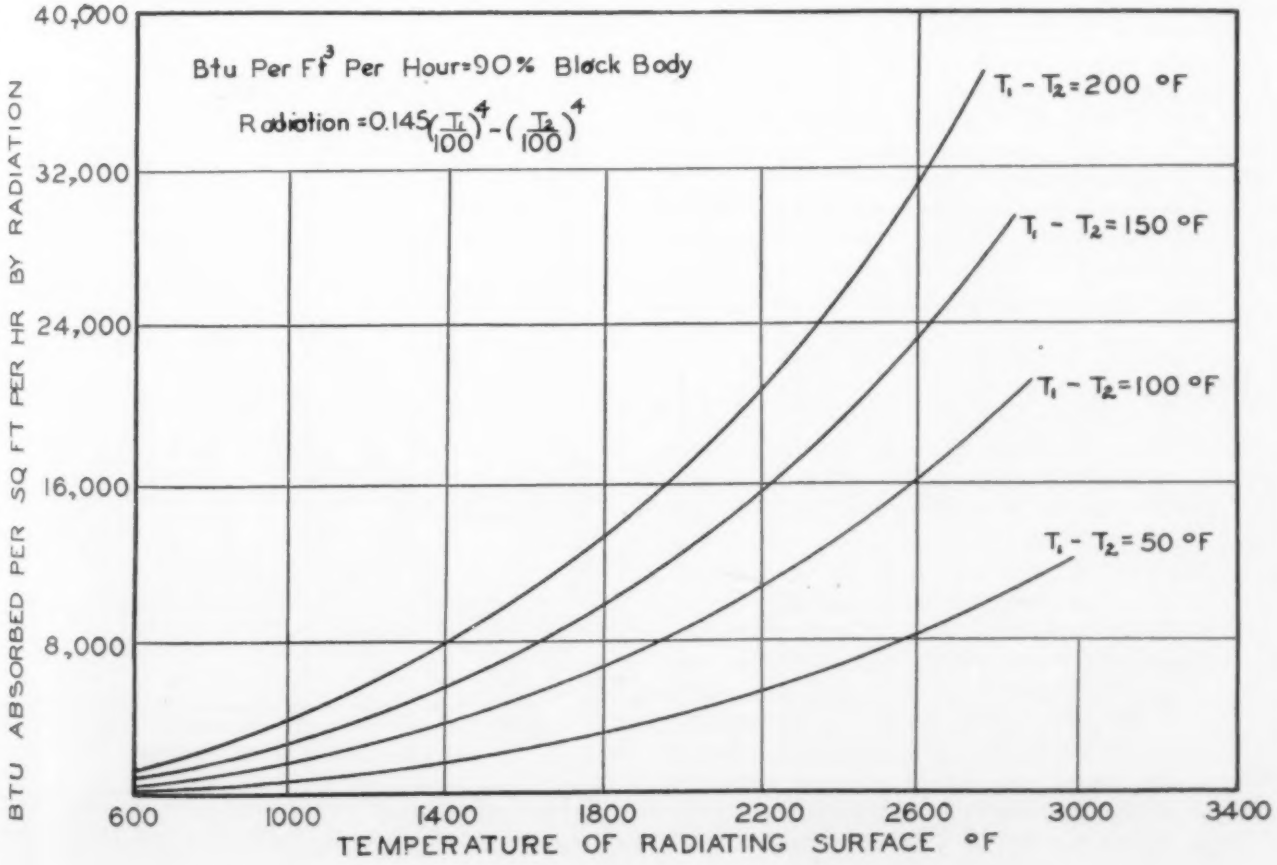
TABLE 7.—ESTIMATED SLOPES OF LINES TREATED AS LINEAR IN FIGS. 1 AND 2.

	Slopes of Lines for Heat of Reaction Btus.		Slopes of Lines for Maximum Combustion Temperature ° F	
N_v	Preheat per °F	H_2O Content /grain/ft³	Preheat per °F	H_2O Content /grain/ft³
0.00	17.37	772	0.719	27.6
0.20	20.84	928	0.69	27.6
0.40	24.31	1082	0.705	27.9
0.60	27.79	1238	0.704	25.9
0.80	31.26	1392	0.709	26.1
1.00	34.73	1546	0.717	26.0
Increment of Slope	17.3	775	0.00	−2.0

Fig. 3—Effect of temperature upon heat transmitted by unit temperature difference.

Equilibrium Requirements

The equilibrium curve for the reaction $C + CO_2 = 2CO$ was taken from a summary by Chipman [10] and is given in Fig. 4. At high temperatures (above



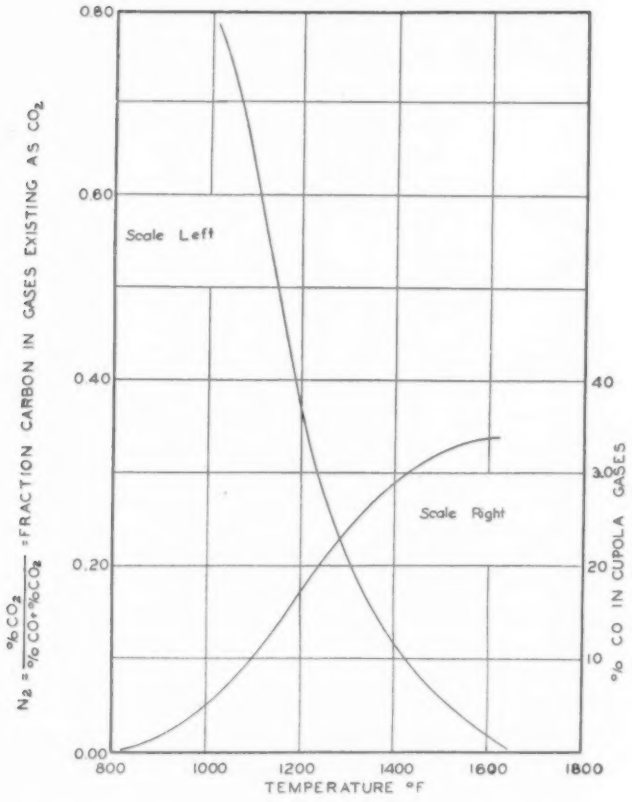


Fig. 4—Equilibrium relationship for reaction 2 CO = C + CO_2 when combustion is with dry air.

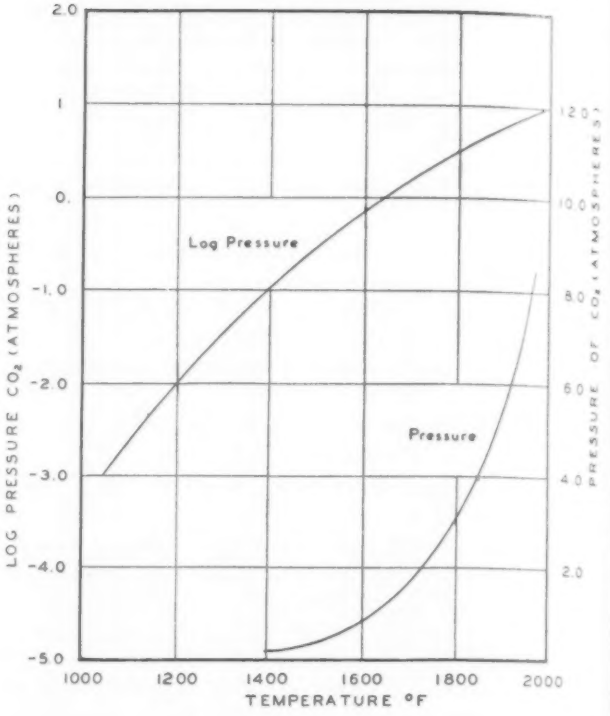


Fig. 6—Decomposition pressure of $CaCO_3$ in atmospheres.

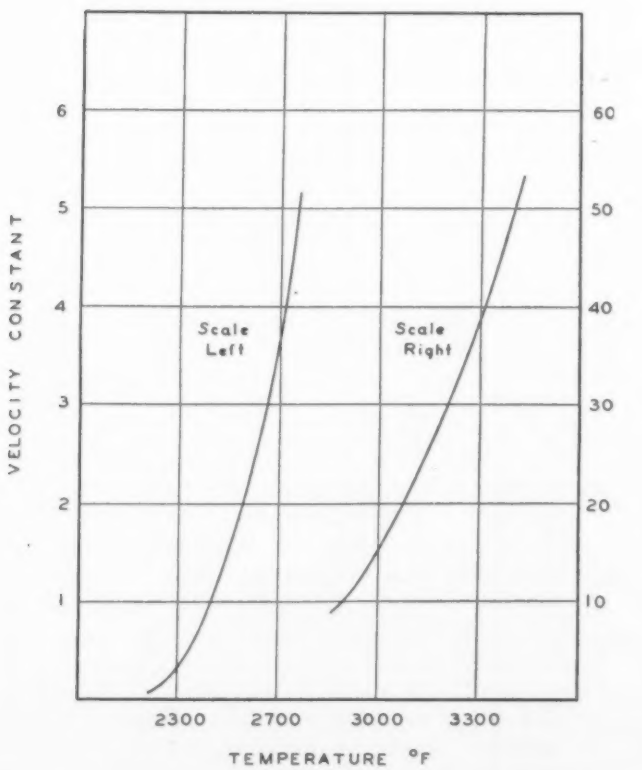


Fig. 5—Velocity constant for reaction CO_2 + C = 2 CO.

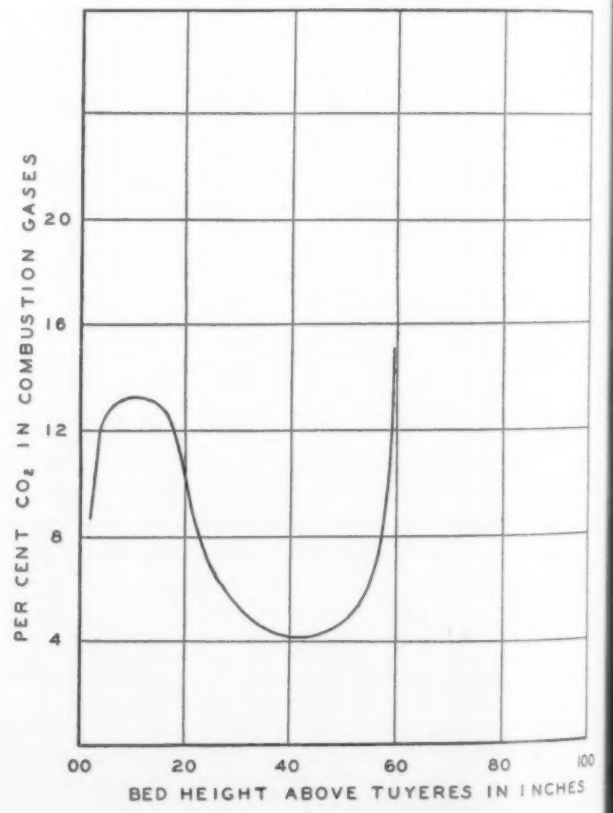


Fig. 7—Analysis of combustion gases as coke bed was burned. Coke 1½ to 2 in. Wind 750 cfm.

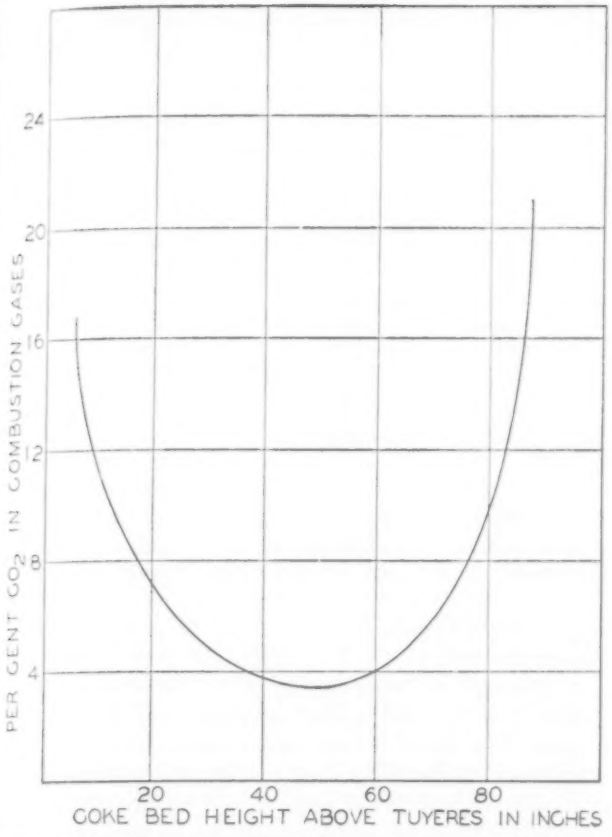


Fig. 8—Analysis of combustion gases as coke bed was burned. Coke 2 to 2½ in. Wind 400 cfm.

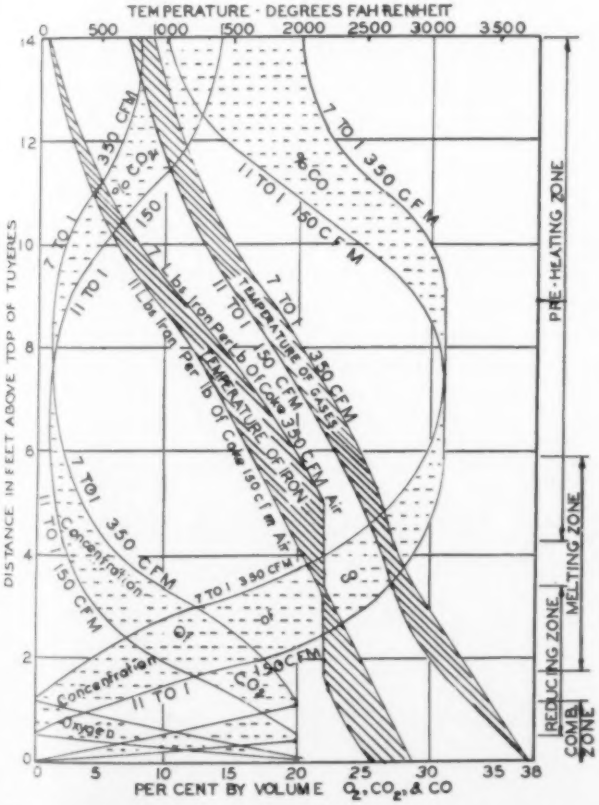


Fig. 9—Relation between gas and iron temperature and per cent oxygen CO, and CO_2 in the various zones of the cupola for the usual range of operations.

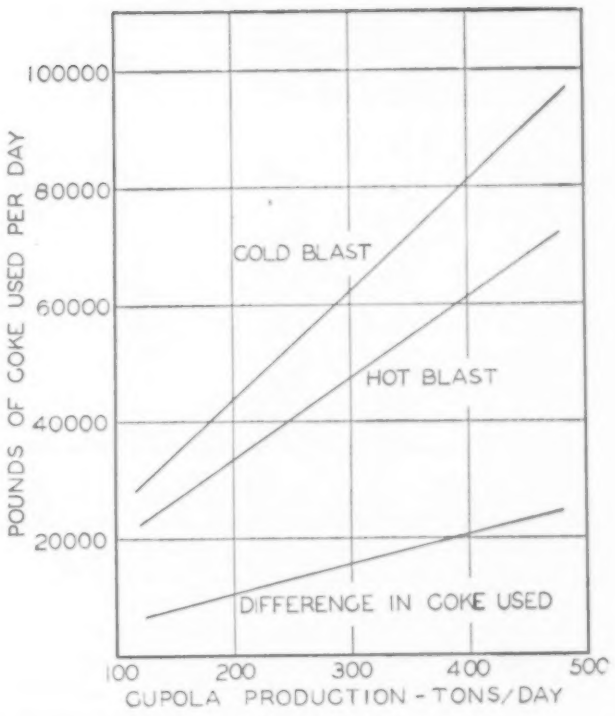


Fig. 10—Relation between cupola production and coke used in hot and cold blast cupolas.

1600 F) the reaction is essentially complete toward the right. In the combustion zone the only reason for incompleteness is the lack of time. Clement, Adams and Haskins [13] have studied the reaction rate at somewhat lower temperatures and with small pieces of coke, but their data was used to extrapolate the reaction rate into the temperature region of interest. This is given in Fig. 5. With such an extrapolation, the curves are only indicative of the high rate existing. The reduction of carbon dioxide to carbon monoxide by the carbon is well recognized as a part of the mechanism of blast furnace combustion with the carbon dioxide entirely disappearing within a short distance from the tuyeres. In gas producers the same action occurs at a slower rate due to the lower temperature existing.

At low temperatures, the reaction proceeds toward the left. Bone, Reeve and Saunders [7] have studied this low temperature reaction particularly with respect to its catalysis with carbon of various forms, iron ores, and other constituents of a blast furnace charge. The reaction takes place between 1300 F. and 500 F with the optimum rate at about 850 F. This deposition of carbon is one of the chief sources of fuel economy in blast furnace operation.

Another reaction which takes place in a cupola is the decomposition of limestone. The equilibrium pressure of carbon dioxide was taken from the International Critical Tables [14] and plotted in Fig. 6. It is quite likely that most of the limestone decomposes at temperatures at which the carbon dioxide will react with carbon to given carbon monoxide. Any carbon dioxide which does not do this will go into the

TABLE 8.—RESERVE HEAT CONTENTS OF COMBUSTION GASES ABOVE 2700° F (3160° R) BTU'S

N_2	H_2O, Per cent	Heat Content at 2700°F	Preheat Temperature °F					
			75	140	440	740	1040	1340
0.00	0.0	59200	7600	8600	13660	18840	24230	29800
0.20	0.0	69200	22080	23280	29330	35560	42030	48720
0.40	0.0	79400	36360	37760	44810	52090	59640	67440
0.60	0.0	89500	50740	52340	60400	68720	77350	86270
0.00	2.0	58200	2900	3880	8800	13580	19150	24600
0.20	2.0	68100	16340	17510	23390	29510	35830	42370
0.40	2.0	78100	29680	31040	37930	45040	52420	60050
0.60	2.0	88100	43010	44670	52440	60570	28990	77720
0.00	4.0	57300	−1600	−650	4160	9130	14270	19610
0.20	4.0	67200	10760	11900	17670	23610	29800	36200
0.40	4.0	76900	23320	24660	31400	38330	45540	53000
0.60	4.0	86700	35780	37290	44990	52930	61170	69700
0.00	6.0	57000	−6420	−5480	−830	4170	9260	14540
0.20	6.0	66700	5110	6240	11940	17820	23930	30260
0.40	6.0	76500	16550	17870	24520	31390	38520	45900
0.60	6.0	86200	28070	29500	37180	45030	53170	61610

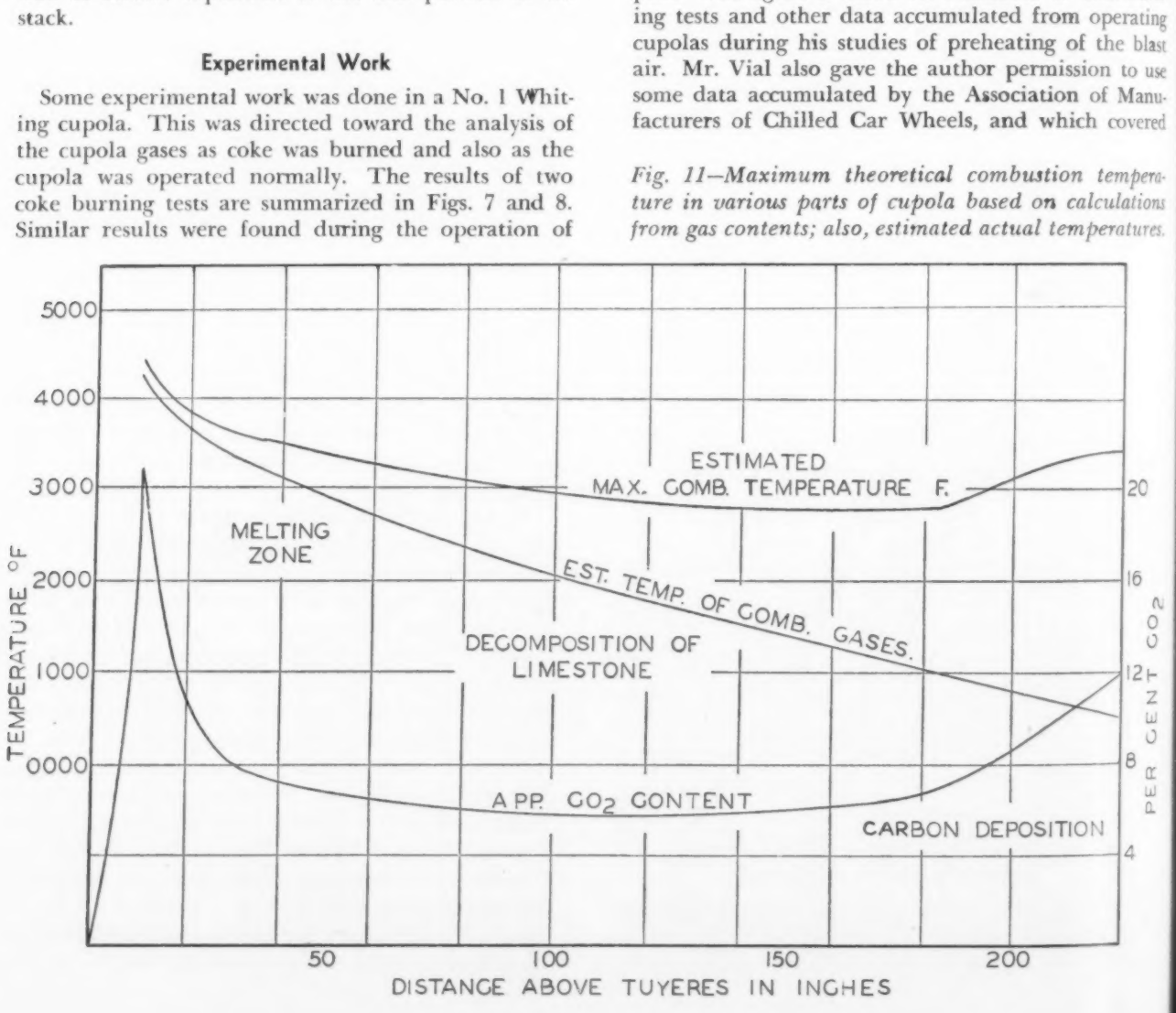
carbon deposition zone and inhibit the reaction by partial satisfaction of equilibrium conditions. That portion of carbon dioxide which does react with carbon increases the concentration of this carbon monoxide, thus requiring a more extensive low temperature reaction. There is, then, in addition to the heat required for decomposition of the limestone (735 Btu/lb.) an additional quantity of heat lost which must be charged to this reaction due to the inhibition of carbon deposition in the cool portion of the stack.

Experimental Work

Some experimental work was done in a No. 1 Whiting cupola. This was directed toward the analysis of the cupola gases as coke was burned and also as the cupola was operated normally. The results of two coke burning tests are summarized in Figs. 7 and 8. Similar results were found during the operation of this cupola, but the results were quite erratic. It was shown that, after a long period between charges, a carbon dioxide content of three per cent quickly became 12 per cent when another charge was added, but that the carbon dioxide content would gradually get lower and become three per cent as the charge became heated.

Through the courtesy of F. K. Vial, Fig. 9 is presented as his conception of conditions within a cupola. The figure is based on a number of coke burning tests and other data accumulated from operating cupolas during his studies of preheating of the blast air. Mr. Vial also gave the author permission to use some data accumulated by the Association of Manufacturers of Chilled Car Wheels, and which covered

Fig. 11—Maximum theoretical combustion temperature in various parts of cupola based on calculations from gas contents; also, estimated actual temperatures.



dimensions, operating items, composition of product, and whether preheating was used. Statistical analysis of these data showed that the taller cupolas were, in general, more economical. Preheating is definitely beneficial as shown in Fig. 10.

General Discussion

Based on the above calculations and data, one may proceed to outline the mechanism and conditions of cupola operation.

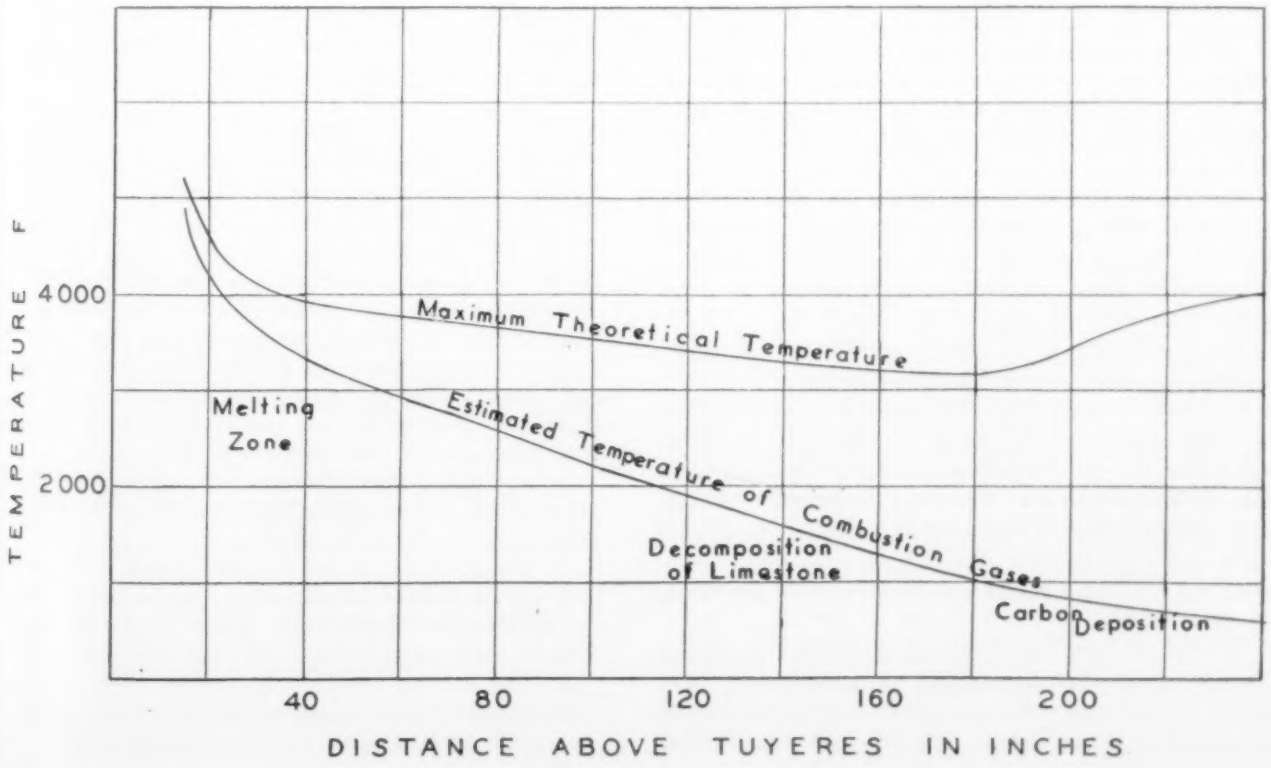
In charging a cupola in the usual manner, a mechanical relationship of the charged materials, coke, scrap and flux, is set up. While this same relationship is repeated in the charging during the period of operation, it is doubtful whether the initial conditions just above the tuyeres can be assumed to remain after the cupola has run for a time.

Consider the coke bed. Charged initially to 50 to 70 in. above the tuyeres it is crushed to a considerable degree by the stack burden. On starting, there is an interval of time before metal begins to accumulate in the well and the coke consumed in this period has been removed largely from the bed. One or more inches per minute covering this interval can be subtracted from the original height of the bed and a 50-in. bed becomes at most a 30-in. bed by the time melting has begun. Melting begins rather slowly and the full rate is not reached for a further interval and the bed may be considered to approximate 25-in. under very favorable circumstances.

But iron only melts as fast as heat is absorbed at the melting point, and the melting point of different portions of a mixed charge varies widely. The surface to volume ratio is of great importance. As these pieces become exposed to gases at temperatures above their melting point, heat is absorbed at a rate commensurate with the temperature difference and the area exposed. Low melting parts tend to melt first and at somewhat higher positions in the stack. Higher melting constituents must penetrate to higher temperature levels before they will melt at the rate charged and large pieces must be heated from still higher temperature sources. The extraction of heat to melt high melting materials chills the gases and lower temperature potentials remain to melt the lower melting items higher in the stack. This tends to localize the melting zone and reduce the extent of scattering created by differences in fusion temperature and particle size.

In Fig. 11 there has been drawn a temperature curve which represents the maximum theoretical temperature in the cupola. It is calculated from gas analyses as summarized by the carbon dioxide content curve. The region of very high temperature is very short and exists between the section containing oxygen and the region of reduction of CO_2 by carbon. The location of this highest temperature zone may vary and may be considered to be changed with wind velocity, type of coke and other factors. The rapid drop of temperature, as the carbon dioxide is reduced, limits the location of rapid melting to within a short distance and since superheating must take place after melting, this must be accomplished rapidly. There is a rather long region in which the maximum theoretical temperature is fairly uniform due to a rather constant gas analysis throughout the cupola.

Fig. 12—Estimated temperatures conditions in cupola when burning coke with 600° F. preheated air.



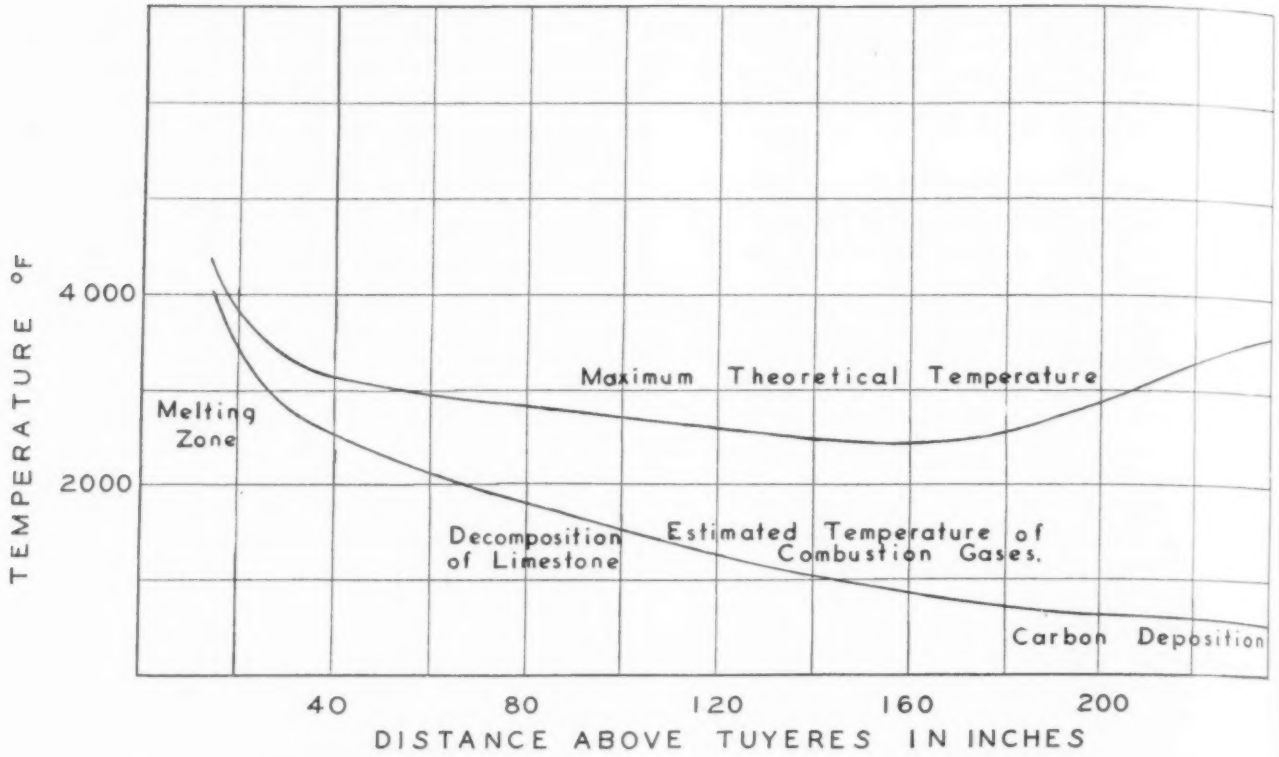


Fig. 13—Estimated temperature conditions in cupola when burning coke with 13.3 grains of water per cu. ft. of air.

While the theoretical temperature curve is drawn for the full height of the cupola, with an attempt to correct proportionally for the temperature of the carbon, an estimate of the actual temperature of the combustion gases, consisting largely of carbon monoxide and nitrogen, would show considerable drop with their exit from the stack at somewhat over 500 F. Such a curve is approximated in Fig. 11. It is important that the temperature of this gas be as high as is practicable so that preheating of the charge preparatory to melting will be done before the metal reaches the melting zone.

By drawing similar curves for the conditions of 600 F preheat and of 4 per cent (13.3 grams per cubic foot) of water vapor, one may get an idea of the importance of these temperature relationships in a cupola. This was attempted in Fig. 12 and 13.

Only under the most adverse conditions will there fail to be a zone in which sufficient temperature exists with which to melt iron. This zone will be extended by preheating and shortened by moisture. In cases where the temperature of the gases is not much above the critical temperature of 2700 F, iron must melt in the oxidizing zone of high carbon dioxide content and there will be but little time for superheating. Cold, oxidized iron must result with its attendant problems in the foundry.

The data show that preheating can overcome the detriment of moisture content on temperature, but this does not mean that the other detrimental effects of moisture are removed. The effect of moisture on castings has been quite extensively discussed, but the paper by Eash and Smith [15] is of particular merit.

Economy in coke is probably linked with carbon deposition as in a blast furnace. This was indicated by the changes in gas analysis found before and after individual charges, as well as the statistical analysis which showed that cupolas with about twenty feet between tuyeres and charging door were, in general, more economical than shorter cupolas. The severe channeling of the gases, where haphazard charging practices are used, largely prevents the economy which might come from a proper appreciation and application of these cupola reactions.

Conclusions

From this general study of cupola reactions, the following conclusions have been drawn:

1. Throughout the major portion of the cupola the carbon dioxide content is quite low, probably less than 6 per cent. In case this is not so, the coke bed is too shallow to permit reduction to carbon monoxide and the iron is melting under oxidizing conditions.

2. In order to provide sufficient temperature in the cupola gases to melt rapidly and under suitable conditions, preheating the air and removal of the moisture from the air are very important.

3. Preheating of the blast may compensate for the heat loss due to moisture but cannot remove the effects of hydrogen on the castings.

4. Temperature of the cupola gases is dependent only on the conditions attending combustion and not on the amount of coke used. When conditions favorable to high temperature exist, coke economy results.

5. The driving power of a cupola, like that of a blast furnace, is dependent on having a large proportion of the heat content of the combustion gases above

a critical operating temperature level which is arbitrarily chosen as 2700 F. This requires a high operating temperature as reserve heat and excess temperature are directly related.

6. It is quite likely that coke economy in a cupola, like that in a blast furnace, is largely dependent on carbon deposition in the cooler (less than 1300 F) portions of the charge. Cupolas should have sufficient height between tuyeres and charging doors to promote this action.

7. The decomposition of limestone liberates carbon dioxide which either inhibits some of the reverse reaction by partially satisfying equilibrium conditions or reacts with carbon to increase the amount of carbon monoxide which must decompose. In poorly operated short cupolas, much of the calcining of limestone may take place near the melting zone, thus causing an extraction of heat which should be used for final preheating.

8. Preheating of blast air is definitely beneficial in producing coke economy. The same may be said with respect to the removal of moisture although no direct data are given.

9. Equations have been developed for the calculation of heat contents of combustion gases and maximum combustion temperatures. From these one may estimate the relative temperatures and heat contents of the combustion gases which will result from various treatments.

10. It is the author's intention to extend this method of calculation of heat of reaction and maximum combustion temperature to cover conditions attending combustion in a blast furnace and, if possible, in an open hearth.

Acknowledgments

Much of the data for this paper has been taken from a report submitted to the Whiting Corporation on some work done at the Armour Research Foundation under the direction of Dr. R. G. Spencer, who was then Chairman of the Metallurgy Department. The University of Utah has given support for a complete recalculation and extension of the material, and John R. Lewis, Head of the Metallurgy Department, has given excellent advice. The Whiting Corporation has given permission to publish. The author wishes to thank all these for their help in making this paper suitable for publication.

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DISCUSSION

Chairman: B. P. MULCAHY, Consultant, Indianapolis

Co-Chairman: H. W. STUART, U. S. Pipe & Foundry Co., Burlington, N. J.

W. R. JAESCHKE[1]: From the importance the author places on flame temperatures, would you not think that that would be an advantageous test to add to the coke specifications?

MR. FLANDERS: I am not sure that the differences in the quality of coke as presented by "Survey of Foundry Coke Characteristics" by D. E. Krause and H. H. Lownie, Jr., of Battelle has a great effect on the difference in flame temperature. There may be some reason for a difference of effective temperature at the melting zone due to the fact that the CO_2 reaction with carbon does not take place quite as completely in the non-reactive cokes as with the reactive cokes, but the rate of reaction at these high temperatures is very rapid. There is a figure in the paper which indicates what the rate may be as extrapolated from data reported by the Bureau of Standards. In the temperature range of 3000 to 4000° F., or even at 5000 to 5500° F., as is attained when carbon is burned to a high percentage of CO_2, the rate of reaction is so rapid that it may not be possible to make accurate tests. I doubt whether any precise measurements of the rate of reaction can be made easily. While my opinion is that such an additional test probably would not show much difference in temperature, I would say, however, that such a study of cupola coke should give valuable information.

MEMBER: Figure 8 shows a high percentage of carbon monoxide in the relatively shallow bed of coke. In other words, when you get down to 15- and 20-in. beds, you have a high percentage of carbon monoxide. Now with the very deep bed it is easy to understand high carbon monoxide percentages because of the possible conversion of CO_2 to CO by the upper parts of the bed. I would like to have your opinion as to why there is such a large percentage of carbon monoxide on a very thin bed where you cannot have that solution effect above?

MR. FLANDER: Our actual results occurred when we were burning the coke bed down in the cupola and we were in the region of 15 or 12 in., or even lower, when we had the high CO_2 content. At a distance just a few inches above, there was still very low CO_2 content. In other words, the distance between a high percentage of CO_2 and the low percentage of CO_2, as you come up above the tuyeres is very short. There are two figures in the paper—one blowing 750 cfm. and another blowing 400 cfm. Several other tests were made with carefully sized coke, the larger coke raises the level of the low CO_2 content somewhat, but not markedly. In other words, with a high wind velocity and the larger coke, the higher CO_2 reaches up a little further into the cupola stack.

Since the rate of reaction is extremely rapid, with small coke, of the order of one-half centimeter, as was used by Clement, Adams & Haskins[12] the distance from CO_2 to practically complete CO is very short indeed.

[1] Whiting Corp., Harvey, Ill.

D. E. KRAUSE[2]: I would like to ask how the gas samples were. Were they taken at the charging door or with a probe of some kind?

MR. FLANDERS: Sampling was done at the charging door with a double cone attached to the end of a ⅛-in. monel metal tube. The design of the double cone, about 1½ in. in diameter with ⅝ in. space between, was such as to partially prevent dirty entry into the tube. A monel metal tube was chosen so that oxidation would not be so severe and give a partial oxidation of CO to CO_2. The position of the sampler was about two feet below the charging door with constant movement to get approximately average samples.

Analysis was done with a Ranarex, continuous flow, indicating unit which was checked as rapidly as possible with an Orsat apparatus. It as found in the No. 1 Whiting Cupola that the position was not very important when simply burning coke. The cupola was fairly tall and the gases had some chance to mix as they rose from the coke bed to the charging door.

J. T. MACKENZIE[3]: Is this statement correct that when you used 750 cfm. you had 1½ to 2 in. of coke and with 400 cfm. you had 2 to 2½ in. of coke? Did you try the variation in blast pressure with the same size coke?

MR. FLANDERS: Dr. MacKenzie asked whether or not we had used a uniform coke with different wind volumes. This was the case. With carefully sized coke within the dimensions given a number of tests were made. The two cases chosen for presentation are representative of the conditions found. However, I shall put another figure (Fig. 14) in the discussion to cover Dr. MacKenzie's question.

DR. MACKENZIE: I think it would add to the paper.

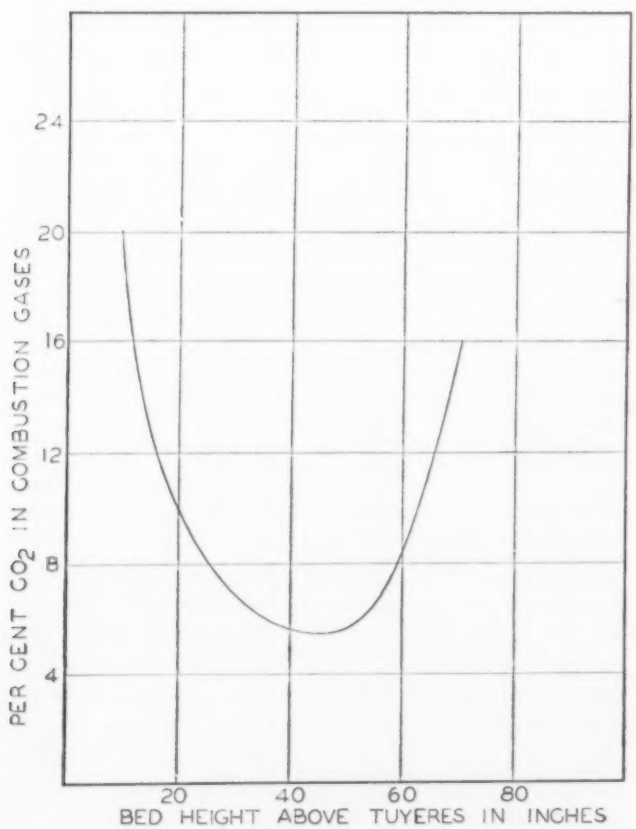


Fig. 14

C. R. HOLZWORTH[4]: Would you recommend the extension of the zone of CO_2 by setting up conditions that will make it higher? That is, would you recommend the use of the second row of tuyeres for that purpose?

[3] Battelle Memorial Institute, Columbus, Ohio
[4] American Cast Iron Pipe Co., Birmingham
[4] Tonawanda Iron, North Tonawanda, N. Y.

MR. FLANDERS: As I understood, the question is: Would I recommend the extension of the zone of CO_2 by setting up conditions so that it will be higher? I rather think not. If the temperature could be made high even when the CO_2 is not high, melting can take place under the conditions of a high CO content or, in other words, a reducing atmosphere, which is one of the most ideal atmosphere in which to melt and hold iron. The high CO_2 region can then be used for the superheat of the iron as it trickles down through the high temperature region to give a better product. There is always a high CO_2 region and it cannot be avoided. If it could, I rather feel it would be a good thing to do. Since it is there and we have to use it, I think it ought to be used for the superheating of the iron rather than the melting. When it is used for the melting there is no location below it at which superheating can take place.

DR. MACKENZIE: Some tests in another paper by Piwowarski seemed to show conclusively that putting a little water in the tuyeres of the cupola did boost the temperature for the time being. It was reported during the war that they were using a little water in the booster for power on the planes. Piwowarski tried to explain that by an increase in the radiation of heat from the steam in the hot gas. I do not know whether that is true or not or whether the author's figures are in error but it certainly does not check with the figures on effect of moisture in the blast. It was long thought that cupolas melted better on a rainy day than they did on a dry day, but the question still seems moot.

MR. FLANDERS: I doubt very much whether it is for the purpose of increasing the temperature. It may have something to do with the rate of heat transfer. Hydrogen does remove heat much faster than any other gas. It has a low molecular weight and high molecular velocity.

In experimentation on a blast furnace it was found that the blast furnace operated best with approximately two grains of water per cubic foot. Whether or not this is true in a cupola, I would not say. Certainly, theoretically, it cannot raise the temperature. In the radiation formula, when the conditions are ideal, the exponent is 4. For the gases, the exponent is closer to 2.8. It may be that the exponent is raised slightly when hydrogen is present, and this would overcome the effect of somewhat reduced temperature resulting from water vapor.

Here is a subject for definite and accurate experimentation, to get the radiation coefficient of the gases of different composition, and I think probably this will ultimately be the analysis of such results, should they be true. There is not enough known about the complex conditions existing in cupola combustion to go far in estimation or guessing.

MR. FLANDERS *(author's closure)*: The questions and interest shown are greatly appreciated.

The mechanism of heat transfer in a cupola is a very difficult one to understand, and the analysis of the various impressions or data about the effect of water vapor on cupola operation must await the acquisition of more information. It may be said with certainty that the introduction of water vapor does reduce the temperature of combustion as well as the available heat above any chosen critical temperature level. What the compensating effects are will not be discussed further.

Water vapor, especially when high, is generally reputed to give a poorer product, and H. A. Schwartz has taken out patents covering the products (malleable iron) resulting from its control. He gives data showing the relationship between water vapor content of the blast air and the hydrogen content of the metal and shows its effects on malleablizing. The graphite flakes make a natural reservoir for the accumulation of any hydrogen that may be present in gray iron, and it may not be as effective in reducing quality as in malleable iron or steel. But the experience with piston rings and the work of Eash and Smith[11] indicate that it is important even in this respect.

It is the general thought of the author that, with provision for sufficient temperature, any reasonable coke can be used successfully as fuel. If, for instance, the combustion of coke to carbon monoxide takes place under such conditions that the temperature produced will melt and superheat iron sufficiently to give it quality and proper fluidity for pouring, the importance of such items as coke reactivity, combustibility, etc., may be very much reduced. There are two primary functions of coke heat and stack porosity. If the most adverse condition of combustion gives

sufficient heat, then stack porosity becomes of prime importance. How important might be the subject of some experimentation. Dr. MacKenzie stated in his discussion of the paper by Krause and Lownie that a mixed coke size, indicating a relatively low porosity, was favorable.

There are two definite ways by which the combustion to carbon monoxide may be made to yield sufficient temperature. These are preheating and oxygen enrichment. The cost of oxygen is such as to make it a questionable competitor with preheating. The effect of preheating is given in the paper. Following is a brief calculation of oxygen enrichment.

Calculations of the amount of oxygen necessary to enrich dry and wet air to any desired total percentage of oxygen can be made by means of the following equations:

For dry air:

$$Od = 0.79\ x + 0.21$$

For wet air:

$$Ow = 0.29\ y + 0.79\ x + 0.21$$

Where Od = °/° of oxygen in dry air.
Ow = °/° of oxygen in moist air.
x = fraction of the enriched air which is added as oxygen.
y = fraction of enriched air which is water vapor.

On the same basis of calculation as is given in the paper the data in the following table result.

Actual combustion temperatures will be somewhat higher than these because of the incomplete reduction of CO_2 to CO above the combustion zone. The cost of the oxygen to make this enrichment per ton of coke (90°/° carbon) at 20 cents per 1000 cubic feet is given in Table A. The CO_2 content of the cupola gases is assumed to be 12°/°. Water vapor will have the same relative effect as in the paper.

TABLE A—CALCULATED COMBUSTION TEMPERATURES WHEN DRY AIR OF DIFFERENT OXYGEN CONTENTS BURNS CARBON TO CARBON MONOXIDE

Blast Air		Combustion Products Mols. per 12 lb. Carton		Maximum Combustion Temperature		Cost of Oxygen per ton of coke (90 °/° C)
°/° O_2	°/° N_2	°/° CO	°/° N_2	°R	°F.	
0.21	0.79	1.00	1.88	3500	3040	$0.00
0.23	0.77	1.00	1.67	3730	3270	0.86
0.25	0.75	1.00	1.50	3920	3460	1.67
0.27	0.73	1.00	1.35	4100	3640	2.20
0.30	0.70	1.00	1.16	4385	3925	3.02
0.35	0.65	1.00	0.93	4815	4355	4.05

12 °/° CO_2 in cupola gases is about equivalent to Nu = 0.40

It seems strange that the fuel wasted in the cupola stack cannot be put to economical use in preheating apparatus, and thus circumvent the necessity for elaborate coke preparation and relieve a serious coke problem for cupola operators.

At the request of Dr. MacKenzie there is also included another figure (Fig. 14) dealing with the combustion of coke, and showing, by comparison with Fig. 7 in the paper, the effect of wind velocity on the height of the combustion zone in a cupola.

In further answer to Mr. Holzworth's question, I would like to recommend the balanced draft mechanism which is being sponsored by the Whiting Corporation. Apparently, in spite of the history of failure with the method, recent control has become such that this method works. The principle is that of maintaining a relatively high CO_2 content of the combustion gases by introducing, in a carefully controlled manner, more air at or above the combustion zone so that the zone of reduction is partially eliminated. In this way a high CO_2 content gas, having a relatively high temperature, approaches the melting zone. Excellent records are claimed for this method. It still leaves the opportunity for the use of waste gases in preheating and still further economies than those claimed.

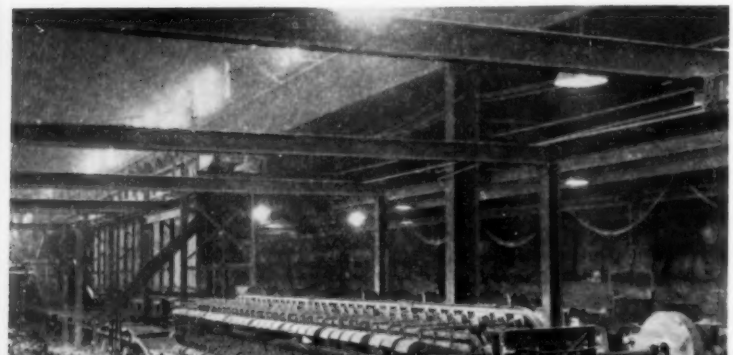
MECHANIZED FOUNDRY DESIGN AND OPERATING PHASES

By C. O. Bartlett
Vice President and Director of Sales
The C. O. Bartlett & Snow Co.
Cleveland

DESIGN AND OPERATION of mechanized foundries embrace many factors. Our observations will be confined, however, only to a few major phases of the general subject of foundry mechanization.

Gray iron foundries, numerically, seem to constitute the largest group currently interested in mechanization or modernization, hence much of this paper relates specifically to that kind of foundry operation. As automotive foundries, because of their high production of repetitive work, offer many outstanding examples of complete mechanization, frequent reference is made in text and illustrations to the layouts of and equipment installed in such foundries.

This should not be interpreted as a lack of interest in the smaller, the jobbing, the steel, the malleable or the non-ferrous foundries. Most of the observations, moreover, are sufficiently general to apply to all types and sizes of foundries. Many an operating or equipment sequence, perfected in some foundry through years of trial and error, will have ready application, with but little further development, in another foundry.



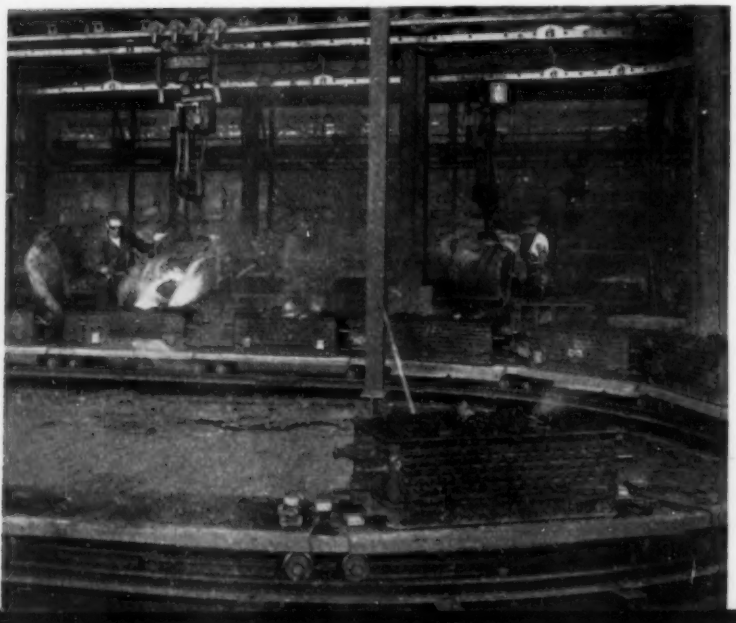
Mold conveyor (above) jointed to flex in vertical as well as horizontal plane, thus permitting cars to move up and down slopes to accommodate various levels.

Mechanized Loop

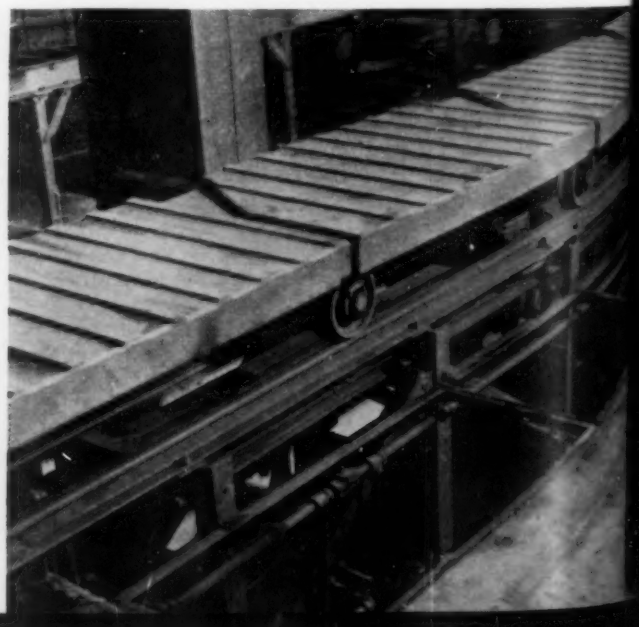
A so-called mechanized loop consists essentially of a line of molding machines, mold conveyors, shakeouts; a conveyor system for returning, screening and storing shakeout sand; mullers, and a conveyor system for distributing prepared sand to molders' overhead hoppers.

Such loops are never identical in any two foundries, and a final layout is agreed upon only after many compromises. No two engineers will make exactly the

Four-wheel mold conveyor cars (below) supported individually and with tops lapping over onto trucks.



Conveyor cars with overlapping tops (below) supported by single, radial action axle, two wheels each.



same approach to a new installation layout and seldom do they select the same location for the sand plants, shakeouts, conveyors and other units. In no case is it ever possible to select, say, catalog layout No. 32, book price it and say "that is it."

Many hours are spent over the drafting board in an attempt to arrive at a layout that will conform to requirements, that can be fitted into the buildings and floor space allocated, and that can be expected to function satisfactorily under the many different operating conditions that prevail in most foundries.

The ideal mechanized foundry has yet to be designed or built. Many valid objections can be raised to almost every layout. Columns, whether in new or old buildings, have a habit of interfering with preferred locations for molding machines, shakeouts, sand plants and conveyors. Column footings, location too frequently unknown, seem always to upset the plans a layout man may prefer. Roof trusses are generally too low for the ideal location of the prepared sand distributing units or for the ideal size or shape of molders' hoppers. Floor space must be reserved for trucking aisles. Already located cupolas, casting cleaning equipment and rooms for core making frequently dictate in advance the only possible location for the loop.

Equipment Funds

The amount of money available has a material effect on any layout or design. Although money has hardly been a limiting factor during the recent war years, the matter of first cost is again with us, and rightfully so. It is well to realize, however, that the combined cost of engineering and installation represents a large part of the total cost of mechanized foundry equipment. This combined cost will remain reasonably constant whether the conveyors be 20 or 30 in. wide, whether a screen be 48 or 60 in. in diameter or whether an elevator has buckets 10x6 in. or 16x8 in. in size.

It should be kept in mind that foundry practices or operations are frequently changed during the service of the original equipment. Changes in production or class of castings or flasks or molding machines frequently mean that the units must handle sand at a rate that may be 50 to 100 per cent greater than originally intended.

Today, uncertain deliveries of essential parts is a compromise factor that can be eliminated only in time. Industrial foundry engineers originate or develop overall layout plans and design, detail, fabricate and install conveyor units, storage bins, screens, dust collecting and ventilating equipment. None of these units can function as a whole, however, until such long-delivery items as motors, speed reducers, pulleys, idlers, chain and belting have been shipped and installed. Current demands for steel indicate that the tonnage of shapes and plates available for foundry mechanization will remain uncertain, at least for some time to come.

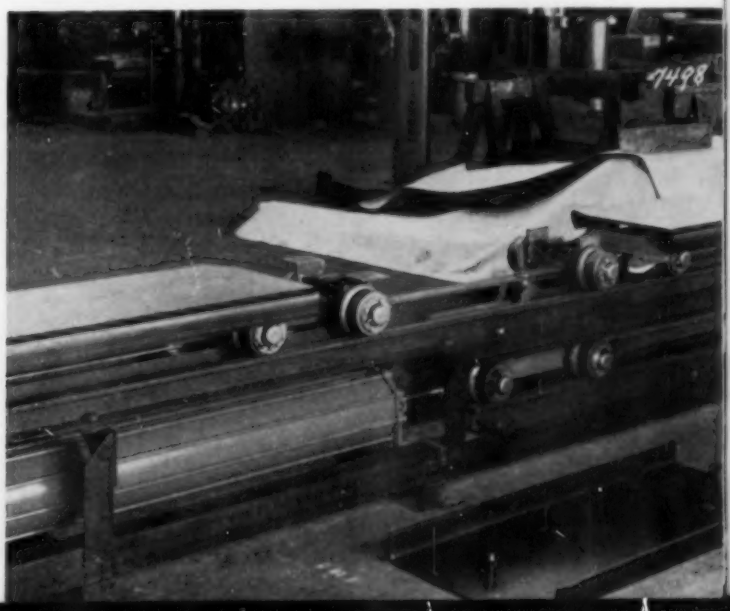


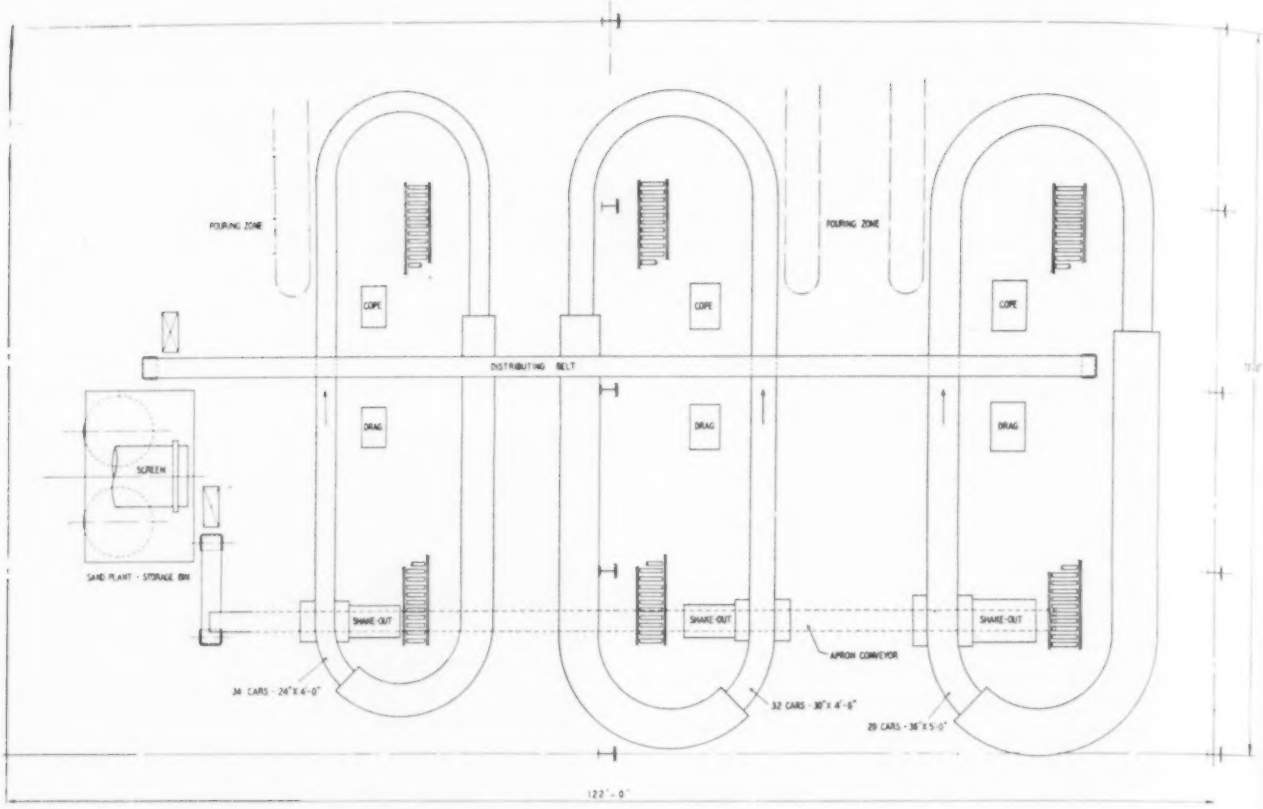
Terminal (above) rectangular type mold conveyor. All cars are shuttled around entire track, the transfer car at other end being used for return to molding side.

Tilting top type of mold conveyor car (below) facilitates shakeout on snap flask work. Car tops may be made of cast iron, steel plates, or gravity-roll equipment.

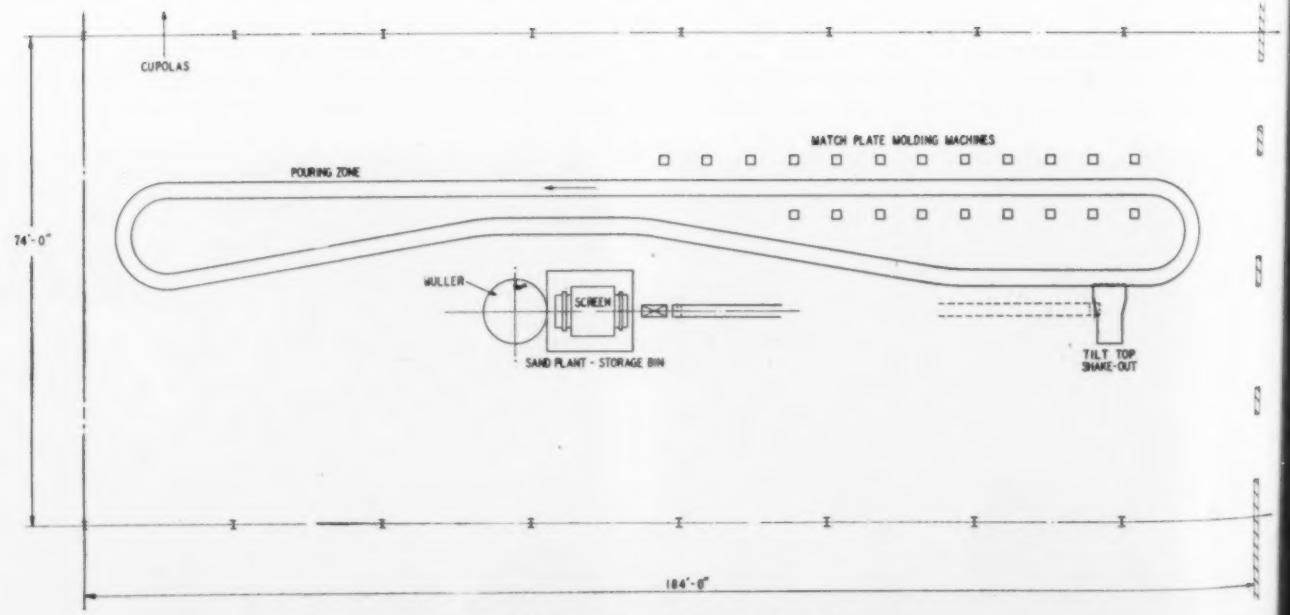


Hydraulic or air operated piston drive (below) for slow speeds and intermittent operations. Piston drives are also used on the rectangular type mold conveyors.

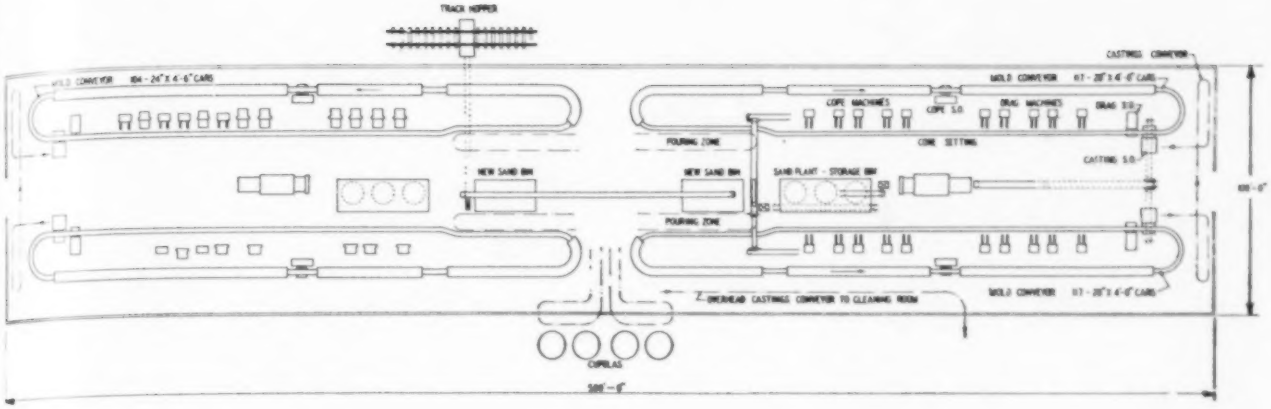


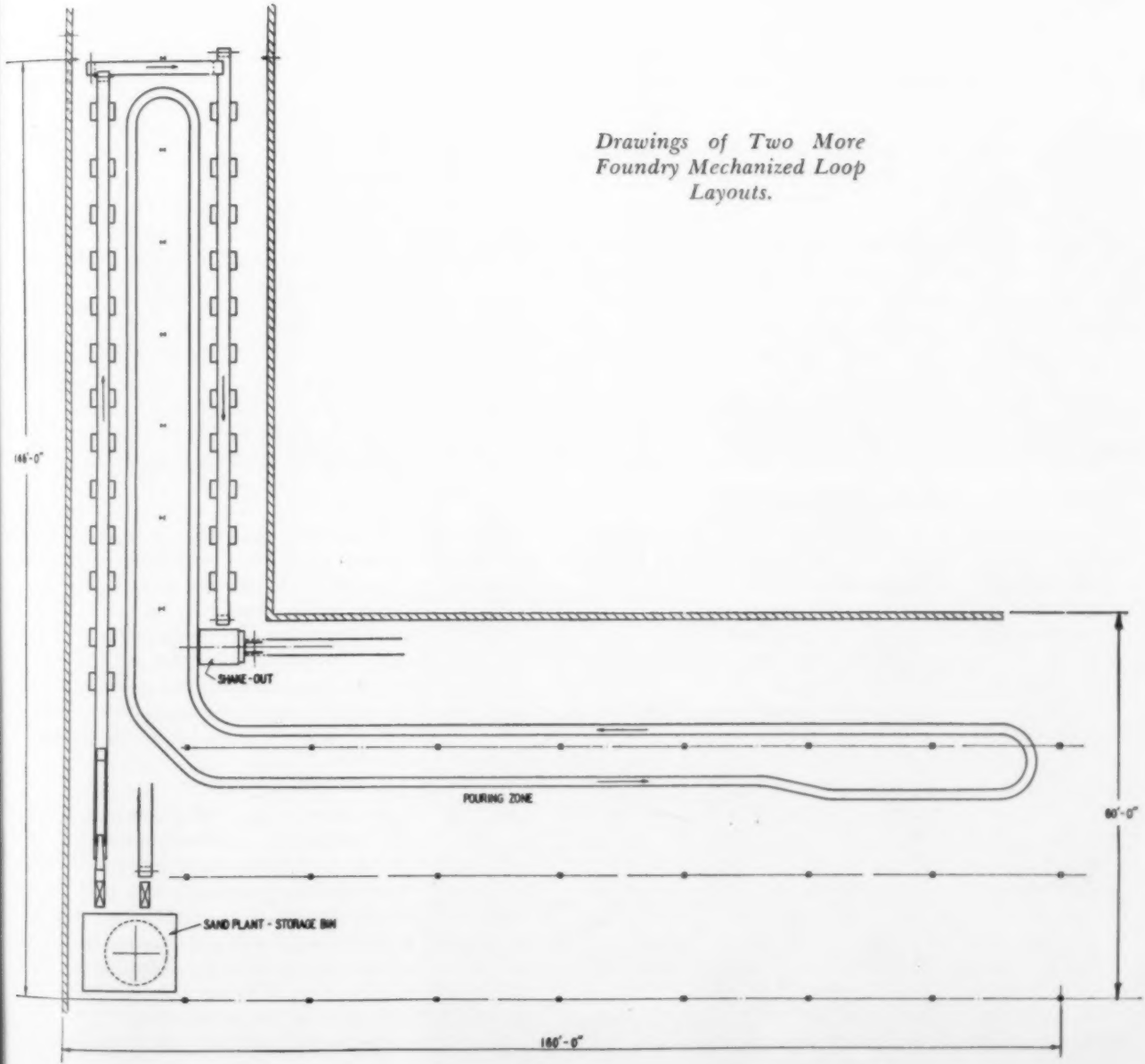


A Design of a Foundry Mechanized Loop Layout.

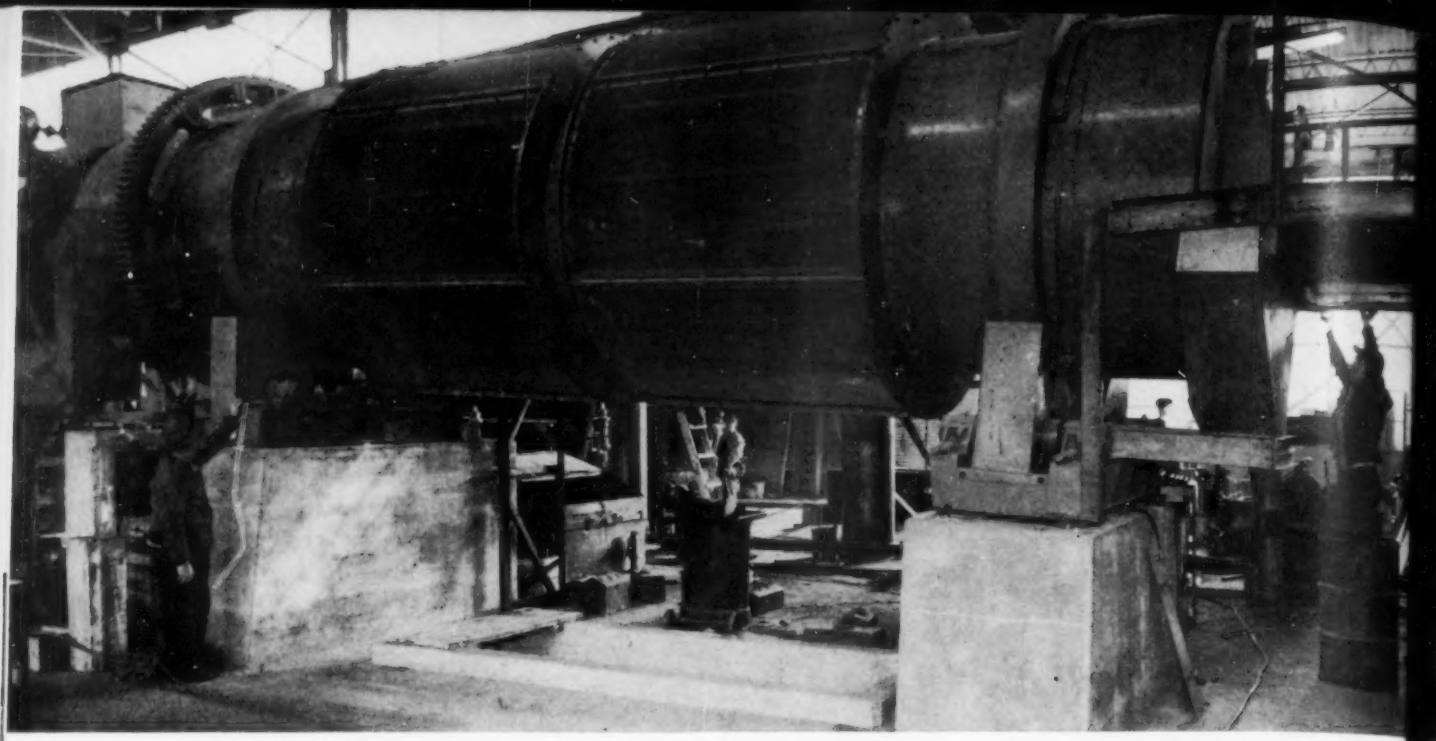


Drawing of Another Mechanized Loop Layout.





Drawings of Two More Foundry Mechanized Loop Layouts.



Revolving cylindrical screen provides excellent lump-breaking action and can be designed for considerable cooling of the screened sand. Location is important.

Nevertheless, more and more foundries, particularly those handling gray iron, are being mechanized or modernized. Many former employees are loathe to return unless working conditions are improved over those prevailing during the 1930's. Partial or complete mechanization offers one method by which working conditions can be greatly improved and productivity maintained at an increased rate without changes in rates of pay. More uniformity in casting quality and lower scrap losses will result from improvements in sand conditioning, from pouring of hotter metal, and from the increased orderliness that will prevail throughout the foundry.

Molding Machines

Mechanization in a foundry starts with moldmaking facilities. Hence, arrangement and operations of molding machines and slingers must be considered as integral with the layout and operations of all other mechanical equipment. Flask sizes, and molding machine production rates, establish the sizes and capacities of all other units.

Molding machine operators and helpers, under suitable conditions, will maintain, day-in and day-out, an average production of some 100 medium sized flasks per hour per pair of jolt-squeeze-strip machines, or of some 60 to 80 larger sized flasks involving roll-over operations. Neither time nor motion can be wasted around such machines. It is well to remember that, in connection with such production, some 20 to 50 tons of flasks and sand must be handled or lifted during each shift by the machine operators and their helpers.

Molders, helpers and core setters should be provided, then, with the best of equipment and facilities to assist them, in every possible way to maintain uniformly high rates of production. Such equipment includes jib or bridge cranes with air hoists; bails that are light in weight and not tricky; flask trunnions, handles and pins designed for quick, smooth operation; accessible air valves; full air pressure at all times for all services; conveniently arranged overhead sand hoppers; floor gratings for removal of strike-off and spill sand, and a steady return of empty flasks.

High-speed molders may be considered as machine operators rather than craftsmen, but as their working conditions improve with clean floors, good lighting, fresh air and ample working space, and as their auxiliary equipment is improved through careful study and coordination their production will increase.

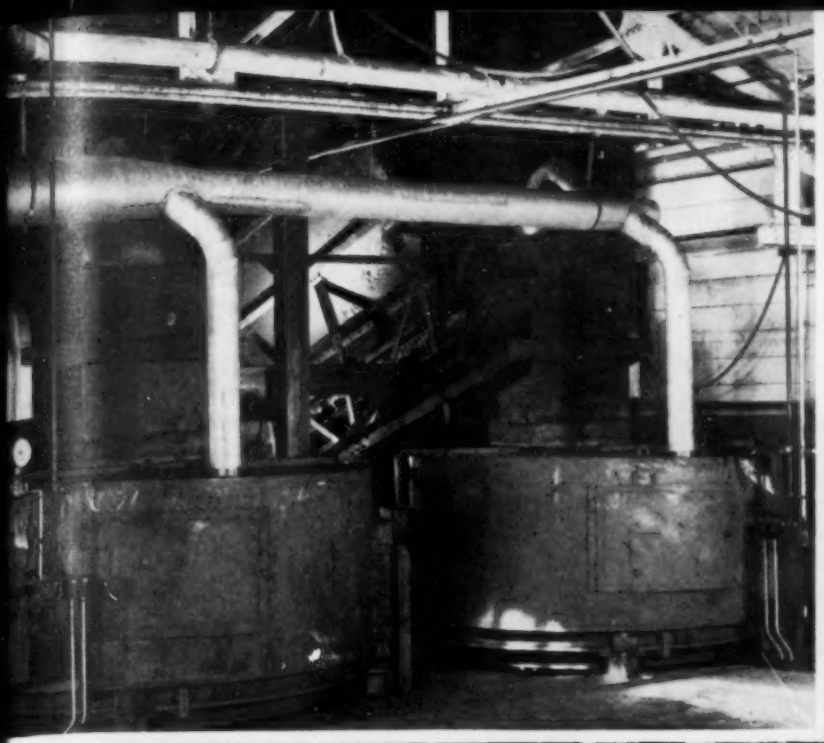
The arrangement and productive capacity of molding machine and slinger set-ups is of prime importance to sand-plant engineers. Decisions must be reached on these points before the design and arrangement of mold conveyors, shakeouts, return flask conveyors, and the entire sand plant with conveyors. Actually, no phase of foundry work can be regarded as an isolated operation when layouts are being prepared for complete mechanization. All phases must be coordinated to function smoothly and in an orderly manner when operating at full rates of casting production.

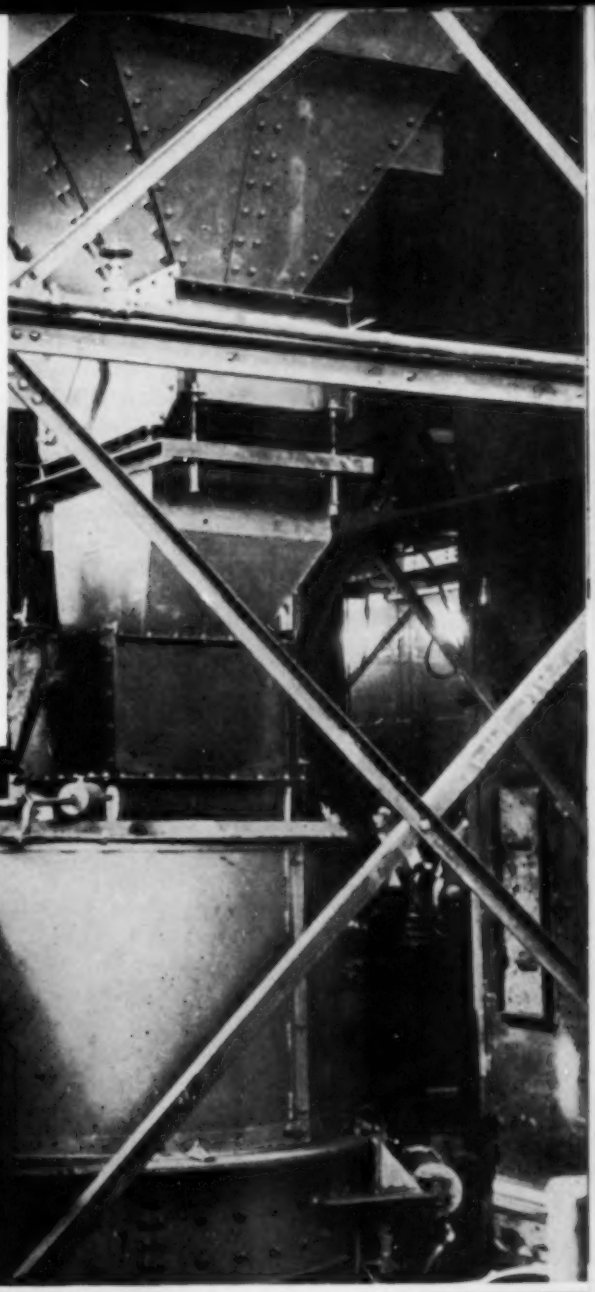
An example of the full value of complete coordination of all units, extending from the cupolas to the shipping dock, is a recently equipped gray iron foundry in which inspected, ready-to-ship castings are loaded into box cars 1½ hr after the iron leaves the cupola.

Mold Conveyors

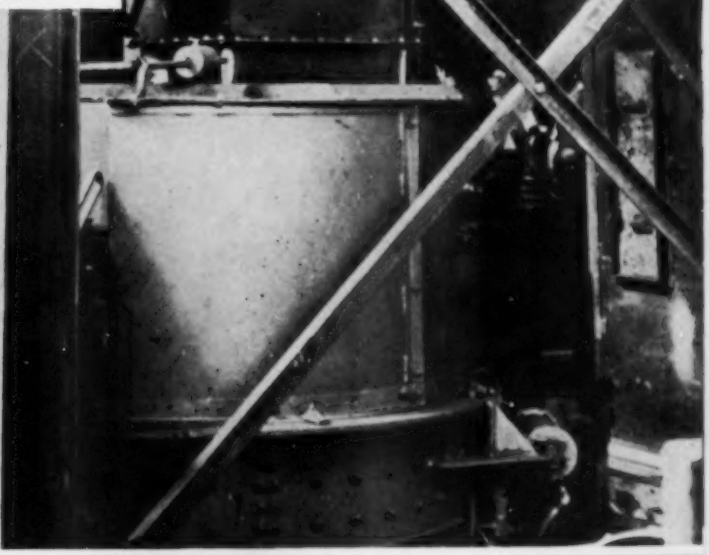
Rammed flasks are set out, cored, closed and poured on either gravity rolls or a continuous, car-type mold conveyor. The use of a car-type mold conveyor localizes and speeds up pouring, centralizes the cooling and shakeout zones, and simplifies ventilation. The conveyor speed is set in accordance with the predetermined rate of molding machine production. Once operating at that speed, whether 6 or 30 ft per min, empty cars are an obvious indication of lost production.

Such a conveyor is truly one of the best yardsticks for











The general arrangement of the entire sandhandling plant is determined by the location of the mullers. If mullers are located above the foundry operating floor level (top), the building roof line must be raised to accommodate them. All bonding materials and sea coal must be elevated for storage in bins or bags on the working platform around the mullers. If the mullers are located at or below the foundry floor line (center and right), pits must be constructed accordingly.

controlling and accurately measuring the productivity of molders. Such facilities provide for the better standardization of work throughout the foundry and enable the establishment of more accurate job ratings. Any car-type mold conveyor represents a dollar-savings investment worthy of careful analysis.

Determining Car Top Size

The size of the car top is determined by the size and the number of flasks to be handled on one car. The overall, or "center-to-center," length of cars is fixed by the shape and size of the tops and the allowable minimum radius of curvature in the track. Although the speed is set by the rate of production, the time required for the cooling of poured flasks is an important factor which affects the conveyor length.

The shape, width and length of such conveyor is always subject to the space required for proper setting and operation of molding machines, floor area available, and the location of building columns. It follows that identical layouts or designs of mold conveyors are not to be found in different foundries; each is tailor-made to suit individual foundry conditions.

When the cars making up a mold conveyor are joined with universal chain, the conveyor will flex in the vertical as well as in the horizontal plane, thus permitting cars to move up and down slopes. The car tops should be at a convenient level for setting out drags. The conveyor track declines before passing in front of the cope machines to provide the same relative height for setting copes and closing. The track then declines to near floor level to reduce the height of flasks for pouring. Once poured, the entire conveyor is dropped completely below floorline to provide clear floor space. Another arrangement of these up-and-down mold conveyors raises the conveyor at the shakeout point, thus minimizing the depth of the customary shakeout pit.

Construction of mold conveyor cars and tops varies with the nature of the work and as new designs may be developed. Each top can be supported individually on four wheels or tops can lap over onto cast iron trucks which are supported by four wheels. Also, tops can overlap and be supported by single, radial action axle using only two wheels per top. They can be designed to move over flat rollers without the use of wheels on tracks. A "sliding-on-rails" type eliminates even the flat rollers.

One development makes use of cars mounted on a single instead of a double track. This type is not continuous in operation and cars are coupled in a series of separate trains. Small locomotives, rather than an endless chain, supply the motive power, and switches are laid out in accordance with needs.

Materials for Car Tops

Car tops themselves may be fabricated of cast iron with bars to facilitate gas venting—heavy steel plates with or without refractory fill, or with gravity rolls. For fast shakeout work on snap flasks, the tilting top offers advantages.

Types of endless chain used in connecting and moving cars vary in design and method of attachment. If bolted to the axles underneath the tops, the chain is usually made up of long pitch, steel side bars with lubricated, steel bushed rollers. Such chain rollers are guided along thrust bars or rail at all curved sections of the track. If the car wheels are straight-faced instead of flanged, continuous double guide bars or rails are provided for the chain rollers along all straight and curved sections of track.

When hinged to the inner side of the car, not underneath, a relatively short-pitch chain is used. This passes around large-diameter sprockets placed at the curved ends of the track. On this type the chain is driven by one of these sprockets. Chains located under the tops are driven by steel dogs that engage chain rollers or other attachments. These dogs are supported in a short length of single or double strand endless chain.

Variable Speed Drives

All continuous drives should provide variable speeds. For rather slow speeds with intermittent operations, the drive may consist of steel dogs mounted on a carriage moved back and forth by means of a hydraulic or air operated long-stroke piston. This piston drive is used on the rectangular type of mold conveyor where movement is limited to one car length at one time. One car is placed on the end transfer carriage, the carriage is moved over and the car moved out onto the back section of track. All cars are thus shuttled around the entire track, using the transfer car at the other end for return to the molding side.

Many troubles associated with the operation of mold conveyors may result from inherent foundry operating conditions rather than from faulty design of equipment. Hot metal runouts, accumulations of sand and rubbish on tracks, or hidden shovels or tools under the cars will result in shutdowns. The mold conveyor is the only piece of equipment in the foundry that handles all flasks, all cores, all poured metal and all sand associated with the production of one loop. It follows that such equipment is entitled to the best of maintenance care and should not be expected to pull through spilled sand accumulations or other obstructions.

When the methods of pouring or other limiting factors preclude the use of mold conveyors of the car type, then several lines of gravity rolls can be arranged to handle the molds and flasks. As gravity roll conveyors are easily moved, any original installation may be changed as needs develop.

Shakeout Installations

Shakeout still is one of the less desirable jobs in many foundries. Dissatisfaction with such work results in more than average flask damage, shutdowns and absenteeism. Proper attention to shakeout arrangement details can eliminate most of these troubles. Experience indicates that shakeouts should be considered as an integral part of sand handling systems, and that careful study should be given to all alternate arrangements before a layout is considered acceptable.

Mechanical shakeouts, although sold as complete units, seldom fit naturally into locations selected. Additional supporting members must be provided over pits. Shakeout sand-collecting hoppers must be fitted under shakeouts and designed with sufficient slope so that the sand will flow onto the conveyors located beneath such hoppers. It is the height required by the hoppers, con-

veyors and transfer points that determines pit depths. The importance of arrangement and details of design of shakeouts should never be overlooked. Alterations and changes are extremely difficult and costly once operations have started.

Real study should also be given at the outset to provision of proper ventilation at every shakeout point. Suitable sidewall or canopy hoods or housings can be designed for most arrangements. The layout man should visualize, however, that such hoods must provide the working space and conditions around the mold conveyor and the shakeout as required for dumping flasks, return of empties and removal of castings—all with or without use of cranes.

Size of flasks and of castings and the numbers to be handled per minute or per hour determine the size, location and number of shakeouts used in connection with any mold conveyor. One shakeout of the mechanical type is frequently sufficient. It is rather common practice to use two, one each for cope and drag. There are distinct advantages in the use of three; one for copes, one for castings and one for drags. Removal of copes shortly after pouring facilitates cooling of the castings, makes for cooler sand and simplifies the return of empty flasks, since copes and drags can thus be handled on separate conveyors. Passing all castings over a separate shakeout removes considerable sand that otherwise would be a source of concern in the cleaning room. In addition, it returns that sand to the system where most of it will be re-used instead of wasted.

Shakeout Sand Conveyors

Usually, consideration is given to layout of shakeout and strike-off sand conveyors only after rather definite locations are assigned to the molding machines, the mold conveyors and the points of shakeout. At this stage the sand-plant engineer is often expected to perform a miracle by arranging, without the aid of pits, penthouses or building alterations, for all shakeout sand to be returned to storage, mulled, and distributed over the molding machines. It is natural that proposal layouts, developed by different engineers, disclose many variations in the arrangement, types and number of conveyor units, as well as in the location and arrangement of the various units of the sand plant itself.

Shakeout sand, with such rods, chills and other pieces of metal that may pass through the shakeout deck, can be handled by any one of several types of conveyors or by a combination of various types. Vibrating feeders or vibrating conveyors offer advantages, if material is to be conveyed for only short distances, if the sand is hot and reasonably dry, or if a desirable minimum pit depth can thus be maintained.

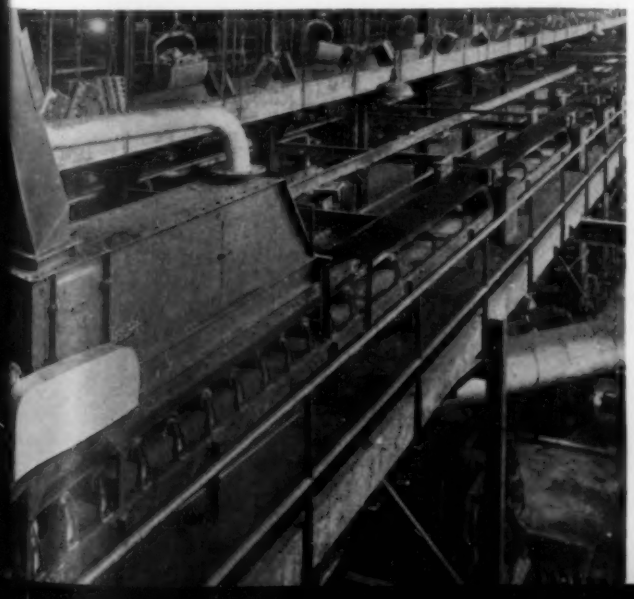
One variation of this all-metal conveyor is the oscillating type which can be used to convey hot materials over relatively long distances. Apron conveyors are commonly used for hot sand, if inclusions of hot metal will burn a belt or if such conveyor must be inclined upward at an angle in excess of 18 degrees.

Whenever conditions permit, belt conveyors are pre-



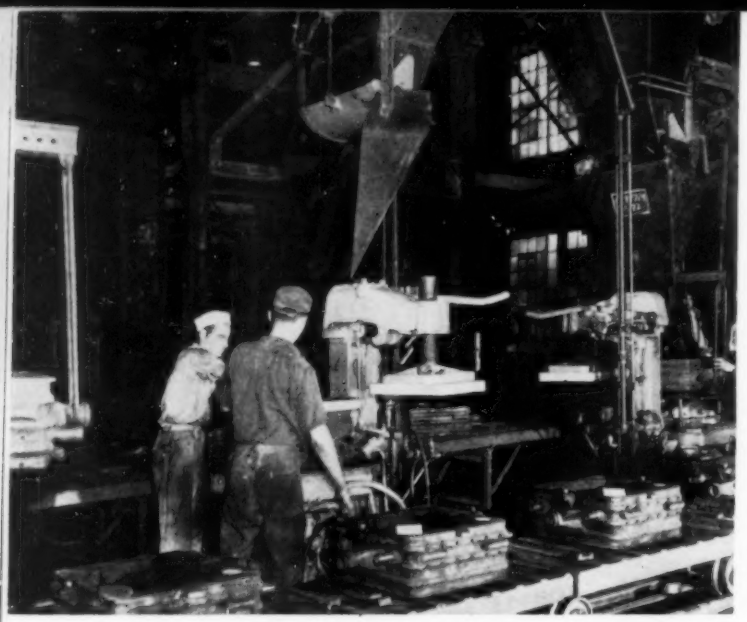
Plows divert prepared sand from belt conveyor into molders' hoppers, and may be hand or air operated.

Belt conveyor distributes prepared sand to molders' hoppers. Belt widths should be not less than 24 in.



Overflow sand may be returned to system through shakeout, or belt conveyor in a pit below machines.





Molder's hopper gates may be operated by hand or air. Vibrating feeders replace the gates in some instances.

ferred. Installation and operating costs for belt conveyors are lower than for aprons, and sand spillage is less. Belts can convey for long distances and a magnetic pulley can easily be installed at the head end to remove tramp iron and other magnetic materials.

The use of heat-resistant belting, fabricated with one or more covers of insulating asbestos, may reduce shutdown time incident to burned belts, but nothing except all-metal conveyors can be expected to stand up when handling sand, rods or castings with temperatures above 400 F. The use of water sprays on belt or apron conveyors for cooling sand has never been satisfactory. It is difficult to balance the amount of water so added with the loading of the conveyor, with the result that the sand remains too hot or becomes too wet.

Cooling Shakeout Sand

An acceptable method for cooling and handling hot shakeout sand and rods is to pass the material directly into a revolving, cylindrical, trunnion-mounted steel shell. Here the proper amount of water to be added for cooling is accurately controlled by the temperature of the air exhausted from the shell. Periodic heavy surge loadings, unavoidable when shaking out large flasks, even out in a rotating shell—the lumps are broken and the sand may be separated from the metal by means of perforated or slotted sections.

All shakeout and most strike-off sand should be screened before storing and mulling. As strike-off sand usually is returned through the same sand conveyor system as shakeout sand, the screen must be selected to handle both tempered and dry sands at a rate that will represent maximum surge loading of both kinds. All screens should be mounted within a relatively tight housing so that air-borne dust can be removed.

Revolving cylindrical or hexagonal screens provide excellent lump-breaking action that results in almost complete recovery of foundry sand. Revolving screens can be designed to provide facilities for considerable air cooling of the screened sand. Such screens can be spider mounted on a central shaft extending through the screen, or trunnion mounted outside the cylinder so that no internal structure interferes with passage through the screen of sand, lumps, rods or gaggers.

From the maintenance man's viewpoint, the location of this screen can be as important as the size and type selected. If the screen is mounted on or near the first-floor level, all sand can be passed through it before handling by a bucket elevator. Thus many shutdowns, normally associated with operation of bucket elevators and screens, can be eliminated. Screened sand is considered ideal for handling in an elevator. Unscreened shakeout sand, with more than occasional lumps, rods and metal that will pass over a magnetic pulley is difficult to handle in bucket elevators. A screen located at floor level is, by comparison, easily accessible for inspection and repair purposes. Furthermore, it comes under the observation of the supervisor in his daily rounds.

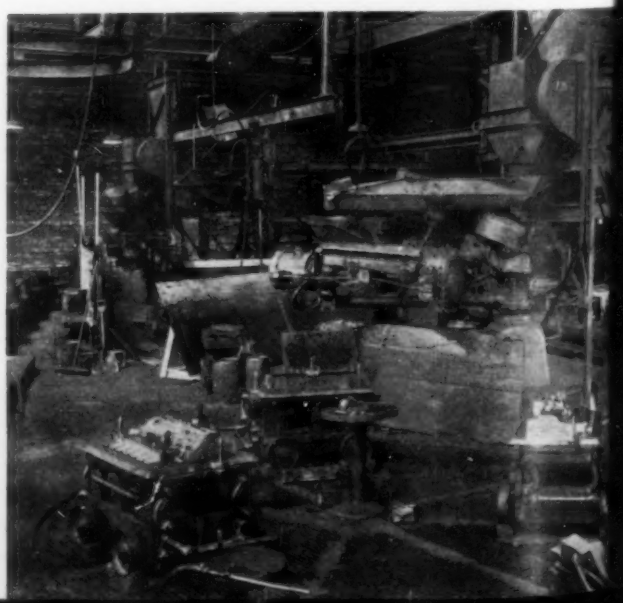
Because of floor space limitations it may not be easy to locate the screen at floor level. Hence, it is often supported on top of the storage bin. In such layouts the shakeout sand passes over a magnetic pulley, up an elevator, through the screen and into storage.

The use of bucket elevators in foundries is well-established practice. Floor space limitations and other building arrangements seldom permit substitution of rather long, inclined belt conveyors. Yet, many maintenance men still associate such trouble sources as plugged chutes, digging out boots, cleaning and replacing buckets and re-splicing belts with the use of bucket elevators. In view of their experience and the intimate knowledge these maintenance men have of operating conditions peculiar to their own foundry, their suggestions and preferences should be considered when selecting elevator sizes, designs and arrangements.

Pit dimensions should be such that adequate working space around at least three sides of every elevator boot is available. Similar working space should be provided at every head end by means of an accessible, full sized service platform.

Wet sand will accumulate on the inside walls of an elevator casing if wet, steamy vapors are allowed to condense on the walls. As such sand builds up and dries out, all of it may drop at one time into the boot, thus plug-

Prepared sand for stationary slinger work may be handled by rotary table feeders or apron conveyors.



ging and stopping the elevator. Shutdowns so caused can be eliminated if all steamy vapors and the air-borne dust are properly exhausted. However, daily checks should be made to insure that exhaust facilities are functioning properly.

Storage bins should provide capacity for storing at least that amount of sand required for one hour of continuous operation of all molding machines being served. Twice such capacity is to be preferred. However, in the layout the capacity and the design of storage bins usually is determined or limited by the arrangement of mullers, height and location of roof trusses, size of penthouse provided or travel of an overhead crane.

Storage Bin Design

Shakeout sand may be returned to storage in a relatively dry condition, but more frequently it is on the steamy, moist side. To prevent sand bridging, outlet openings in the bin bottom should be as wide and long as the arrangement of muller feeding mechanism will permit. To minimize "piping" or the hanging up of sand along bin sides and corners, all sloping sides and valley angles should be as steep as design of the overall layout and available space will permit.

To reduce dusting and steaming, all bins should be covered and ventilated. Such covers will serve as walkways or service platforms around any screen or conveyor that may be supported over the bin. Since sand is an excellent insulating material, normal storage will have no appreciable effect on the temperature of stored sand. If hot sand is returned to the bin, then that same sand will be hot when withdrawn.

Stairway to Bins

Access to the top of most storage bins should be by means of a stairway rather than by ladders. Stairways can usually be designed to provide easy access to such units as distributing belt conveyors, aerators, screens and elevators. Provision for such stairways should be made in the original general layout of the sand plant.

Muller feeding mechanism, usually supported under the storage bins, may consist of apron conveyor feeders, measuring hoppers or scale mounted weigh hoppers. Such hoppers may be mounted in fixed positions over the mullers or on a traveling larry car so that sand from any bin may be fed into any muller.

Use of apron conveyor feeders will enable a lower overall height of bin structure and may enable a more even downward movement of sand across the full width and length of the bin. Size of batch to be apron-fed is regulated by the width and depth of a pull-out gate, rate of speed of the conveyor, and time interval of conveyor operation. With continuous mullers, or pug mill type mixers, such apron feeders operate continuously.

Customer preference usually determines the type of mullers that the sand plant engineer fits into the layout. The number and size of mullers to be used, as well as the expected operating cycle for batch mullers, are matters of vital importance. In computing expected performance, it is well to remember that mulled sand will not weigh 100 lb per cu ft, but usually less than 70 lb. Actual mulling time for one batch of gray iron sand will seldom exceed 1½ min, but the time required for the complete operating cycle must include that required for loading and unloading, which will vary according to type of installation.



For rapid filling in large flask work, sand may be handled to advantage by the apron conveyor feeders.

The use of two medium sized units usually is preferred to that of only one large sized unit. The installation of three units can be justified whenever the capacity of two such units may be considered as borderline. In other words, mullers should have ample capacity so that, at all times, they can supply the molders with all the sand required.

Periodic check-ups should be made of all muller operations. The time required for loading, mulling and discharging should be separately clocked for several cycles. The pounds of sand in several batches, as discharged from the mullers, should be accurately weighed, not just roughly measured or estimated.

Muller location fixes the general arrangement of the entire sand plant. If located above the level of the foundry operating floor, then the building roof line must go up accordingly. If located at or below the floor line, then pits must go down. When located above the floor line, all bond and sea coal must be elevated for storage in auxiliary bins, or in bags on the service or working platform that must be provided around the mullers. Each foundry seems to select a muller arrangement differing from any previously installed.

Continuous type mixers offer advantages in layout and operations when circumstances permit their use. The pug-mill type, first used many years ago, still is preferred in many foundries. A type of more recent design provides mulling action as well as intensive mixing. Sand, bond, sea coal and water are fed continuously into the loading end of such mixers and a steady stream of prepared sand is discharged at the other end.

Prepared Sand Distribution

Prepared sand should be discharged from all batch mullers into surge or temporary holding hoppers so that the mullers can be emptied as quickly as possible and the mulling cycle resumed. Such hoppers are supported directly above a belt or apron conveyor and should be designed to hold not less than one batch of mulled sand.

An adjustable gate is arranged between the continu-

ous skirt plates of the conveyor and at the front of the hoppers. The size of this pull-out gate opening and the speed of the conveyor must be so regulated that each hopper will be emptied before another batch is discharged from the muller. The rate at which sand must be handled by this pull-out conveyor determines the sand-handling capacities for all other conveyors or elevators handling prepared sand.

To improve sand flowability and permeability, an aerator is customarily included in most prepared sand conveyor systems. Irrespective of type, size, design or location, daily inspection and periodic cleanout of such aerators is warranted. Provisions should be made in the layout for adequate and accessible walkways or service platforms on both sides of an aerator.

The design of most prepared sand distributing belt conveyors is rather well standardized. Experience indicates that belt widths of not less than 24 in. can be justified on most installations. A walkway is customarily provided along one side of the conveyor. If the conveyor serves a double row of molders' hoppers, walkways should be provided along both sides. If the walkway plate is widened sufficiently to floor in the area underneath the belt conveyor, it will minimize sand spillage onto molding machine operators and molding work located below the distributing belt.

Sand that passes under the plows and over the head pulley of the distributing conveyor should be returned to the sand system. This overflow sand can be handled in tote boxes, or it may be spilled onto the tops of poured molds and returned through the shakeout. A belt conveyor located in a pit alongside of, and below the molding machines, will provide the most advantageous means for handling such sand.

Such a conveyor should be arranged to return to the system all of the strike-off and spill sand that otherwise would accumulate on the floor around the molding machines and the mold conveyor. The use of a conveyor for the handling of all overflow, strike-off and spill sand contributes much to the good housekeeping conditions that should prevail in every foundry.

Diversion Plow Types

Prepared sand is diverted from any distributing belt conveyor into molders' hoppers by means of plows. The design of hand-operated plows is reasonably well standardized. Single, double or split plows may be used. Plows may be operated by air cylinders instead of by hand. For the remote control of such plows, each air cylinder can be actuated separately through the use of solenoid operated air valves. A recently developed automatic plow makes use of a ribbon screw conveyor arrangement that aerates the sand while removing it from the belt conveyor.

Design of molders' hoppers may involve considerable compromise. The height of molding machines is fixed. The height of the distributing belt conveyor is frequently predetermined by the location of roof trusses. Within such limitations it is often difficult but important to design molders' hoppers with sides that slope at an angle sufficient to prevent hang up of sand and still provide the hopper capacity required for molding machine operations.

Gates at the bottom of molders' hoppers are often of the double undercut type, counterweighted and arranged for hand operation. Use of directional chutes under these gates is frequently necessary. Hopper gates can, of course, be operated by air cylinders and in some installations gates have been replaced by vibrating feeders. When prepared sand is used for stationary slinger work, rotary table feeders are frequently used. Apron conveyors can also be used for feeding slingers. For the rapid filling of large sized flasks, an apron conveyor feeder may serve to advantage.

Acknowledgment

The observations presented in this paper do not have specified reference to the exclusive design of any individual or company, nor are they intended to apply to the equipment of any one concern. Acknowledgment and appreciation is, therefore, extended to all engineers in various firms whose efforts, services and photographs have made possible this discussion of methods for foundry mechanization and modernization.

DISCUSSION

Chairman: E. W. BEACH, Campbell, Wyant and Cannon Co., Muskegon, Mich.

TOM BUTLER[1]: What are the relative merits of your four types of conveyor? Why would you use a pendulum type or a floor type conveyor?

MR. BARTLETT: Types, sizes, weights and numbers of flasks will determine, in a general way, the type of mold conveyor to use. In cases of extreme lengths in a car top, or because of the weight of flasks, a rather heavy cast iron car top with ribs may be used. The width and length of the car top depends upon, naturally, the size of the flask to be handled. A small sized top can be more readily pendulum supported than large tops. This question is not much easier to answer than to answer the question, "What type muller do you prefer to use and why do you use this or that size or make?" Customer preference should have a great deal to do with the type of mold conveyor to be used. The diameter of the wheel depends upon the kind of load you must carry. The larger the diameter, the less friction. The design of the car top itself depends upon the radius of curvature at the ends, and because with that curvature, you get an end cut in your car top if of the overlapping type.

J. O. HUNT[2]: Where can a man find a qualified engineer to lay out just what the author speaks about?

MR. BARTLETT: Such men should find work very quickly. There are many of us looking for that kind of a man.

T. E. MALPASS[3]: We are a small company and have admired your mechanized equipment. We pour between 20 and 25 tons a day. Now, in order to operate profitably, I would assume from this discussion that you must pour continuously and therefore you must have two cupolas, at least, and what I would like to get at, there must be a point where the old way is profitable and there must be a point reached where an engineering firm can say, "Now you can go into a small mechanized unit."

MR. BARTLETT: Your question on the small mechanized foundry is indeed a serious one and none of us seem to know the complete answer. Years ago one yardstick was the use of about 50 tons of sand an hour. On that basis, casting production enabled continuous pouring and there was a reason for rather complete mechanization. During the war years, many non-ferrous and ferrous plants mechanized that were handling as low as 15 to 20 tons of sand an hour. For the use of a continuous mold conveyor, continuous pouring, or a close approach to continuous pouring, is obviously necessary. Mechanization can be used at the shakeouts and for the return of the sand above molder hoppers, irrespective of whether you use continuous mold conveyors or use gravity rolls for the setting out of flasks. Just what the limitation is, I do not know. I offer the suggestion that many a foundry apparently find it justifiable where as little as 15 tons an hour of sand is handled, by

[1] Ford Motor Co., Dearborn, Mich.
[2] Mountain City Foundry & Machine Co., Greenville, S. C.
[3] East Jordan Iron Works, East Jordan, Mich.

simplifying the shakeout of the flasks and returning the sand to the molder hoppers, so they can get increased production and, in general, better working conditions. That is a rather evasive answer but it is not easy to offer in a few words a ready solution to a problem that has been with us for years.

FRANK HIERWARTER[4]: Some time ago we had a similar illustrated discussion by a foundry engineer who said, "There is no foundry too small for some sort of mechanization," and I was wondering if any foundry were to go through the expense of hiring one of these experts to design a foundry and mechanize it, if he happens to guess wrong, who is going to pay the bill?

MR. BARTLETT: The customer always pays, but, in addition to that, it is YOUR customer who has to pay eventually. The public always pays finally. We in the foundry engineering business try our best, but we are not perfect. It is not easy to justify spending over and above a certain amount and that is more or less determined by the nature of your daily production. You can buy yourself into the equipment-poor class just the same as you can operate yourself out of business because of lack of equipment. Both are possible. This brings us back to the point that I tried to emphasize. All foundry mechanization is a matter of compromise between what you would like to do and what your funds enable you to do; compromise between what the foundry engineer, the plant engineer, the cupola operators and the production superintendents would like to install and what their floor area, their building limitations and available funds will enable them to install.

CHAIRMAN BEACH: The line of demarcation between the foundry that can be mechanized and the one that must stick to old-fashioned methods depends on the size of the castings produced and the daily (or hourly) tonnage. If any of you are interested in small foundry mechanization, particularly in sand handling, I would refer you to a paper entitled, "Mechanical Sand Handling for Low-Tonnage Foundries," TRANSACTIONS, AFA, vol. 43, p. 415 (1935), presented at the Ontario Convention. In it you will find illustrations of sand handling in a small plant, which will probably suit many of you whose tonnage is low. With the thought in mind that the question of mechanical aids applicable to the small shop owner might come up—as it always does during a meeting of this section—I am pleased to tell you that the operators of the plant pictured in the paper, extend an invitation if you are interested, to visit their plant. In conversation with S. C. Massari, Technical Director of AFA, he assures me that the AFA is very much interested in the needs of the low tonnage membership group, and that matters of interest to them is to be given attention. I feel certain that at a future meeting of this section, ways and means for installation of labor-saving equipment (or methods) within the bounds of reasonable investment, will be presented for your consideration.

C. W. FERGUSON[5]: We operate a mechanized foundry producing stove plate. Should the magnetic separator be installed before the rotary screen or after?

[4] Newark Brass Foundry, Kearney, N. J.
[5] Durban Falkirk Iron Co. Ltd., Durban, South Africa

MR. BARTLETT: Locate one both before and after. There is a reason for both.

MR. FERGUSON: Should we have continuous mullers or batch mullers?

MR. BARTLETT: If your molding sand is sufficiently similar in character and the flask work is sufficiently similar in character that you use the same kind of sand day after day, without changing your bond and your tempering, a continuous muller system may serve you to advantage. You will have to balance against that the good points of the other types. With a continuous muller, you will have one unit of sufficient capacity to take care of your entire needs. If anything happens to that one unit, your production is down. With batch mullers, you can vary the bond, sea coal and tempering at will and more easily than you can with the continuous operation.

MR. HIERWARTER: Can you tell me anything about the use of these magnesium buckets for elevators where you have trouble in discharging?

MR. BARTLETT: We have not had an opportunity to do what we would like to do and what you would like to see done with them.

MR. HIERWARTER: In your experience, what is maximum belt speed to use per minute for an elevator for successful unloading of shakeout sand, with some water added?

MR. BARTLETT: On centrifugal bucket elevators you may have speeds ranging from 250 fpm. up to 300 fpm. We would prefer to stay in the 250 fpm. class rather than going to the higher speed, although there are plenty operating at the higher speeds.

W. R. JENNINGS[6]: We have a lot of interest evidenced because of the necessity of cleaning up and not having the foundry in the condition that it has always been reputed to be in. On this matter of economy, when you go to a hospital, they may ask you how your credit is, but you still have to have the operation. Many of us must face this operation on the basis that it is something we need to do. We must, of course, face the problem of economy in operation; we cannot stay in business without it.

We have several small foundries. Some of them we have mechanized with different variations within the limitations of that particular foundry, keeping in mind the economy of that operation. Are you running your own shop? If you are running your own shop and somebody else does not tell you what production you are going to get out, then some of this equipment will help you. If you cannot get over the number of molds you are getting now, no matter what kind of equipment you put in, it will not pay dividends. Some of us have been in that situation. Unless we have that American way of producing more with this equipment, the equipment is going to do us no good.

You made a good point when you said have room in your pits. Drain your pits; put a slope to them so you can flush them out with a hose. Do not expect to get down there and sweep them out. I like to have these engineers do something to help me. I would like to have my gangways so I could flush them.

[6] John Deere Tractor Co., Waterloo, Iowa

GAS ABSORPTION PHENOMENA AND DEGASIFICATION OF CAST MONEL*

By
Bernard N. Ames ** and Noah A. Kahn **

ABSTRACT

A study of the effect of various deoxidation treatments of cast monel metal melted in the indirect and resistor furnaces was made. Magnesium is shown to be inadequate as a degasser in melting monel metal in the indirect arc furnace. Titanium, lithium and zirconium deoxidation yielded satisfactory results. The increased solubility rate of certain gases upon melting nickel-copper in this unit is believed to be due due to ionization of the furnace atmosphere. A possible explanation as to the phenomena involved is offered. The comparative beneficial effects on soundness while utilizing an air blast during melting in this unit is shown. The detrimental effects of nitrogen and hydrogen in cast monel metal and the additions of titanium, lithium, or zirconium to prevent nitrogen porosity and of titanium to minimize hydrogen porosity are also shown. Comparative effects between the keel block test bar and another test bar in reflecting metal quality are presented.

THIS INVESTIGATION was initiated due to the high percentage of hydrostatic pressure losses experienced in melting monel metal in the indirect arc furnace by the shipyard foundry with which the authors are associated. When material requirements on Navy Department standard valve trim plans were revised from cast manganese bronze and the composition bronzes to cast monel, a considerable work load in the melting of this alloy was created. Thus, it was imperative that a melting procedure be devised for this unit which would produce monel castings of consistently high quality and for investigating the basic nature of the difficulty encountered.

Practical and Theoretical Considerations

Very little published data on the solubilities of gases in cast nickel-copper alloys are available. Zapfee and Sims,[1] Sieverts[2] and Martin[3] show that for nickel there is a large decrease in hydrogen solubility at the solidification point. In a paper by Hensel and Scott,[4] Ziegler is quoted as reporting the following solubility values for nitrogen in molten nickel: 1405 C.—0.004 per cent N; 1550 C.—0.023 per cent N; 1650 C.—0.051 per cent N.

It is evident from this work that nitrogen has a high solubility in nickel and that there is a sharp decrease in solubility at the solidification point. Duma[5] placed the maximum solubility of carbon in 70-30 copper-nickel alloy at 0.04 per cent at room temperature. He showed that by adding 0.08 to 0.21 per cent titanium to this alloy no evidence of graphite flakes was present but rather crystals of titanium carbide, and that the resulting structure yielded higher strength, hardness and machinability, with a very slight loss in ductility. He concluded that the improved properties were due in no small part to the greater freedom from graphitic fissures. It was reported by Mishima[6] and by Thompson and Barclay[7] that in cupro-nickel alloys (75 Cu-25 Ni), considerable carbon is absorbed by the nickel in the melting operation. On annealing above 700 C. precipitation of graphitic carbon occurs in the grain boundaries, forming an intergranular network and making the alloy brittle. The precipitation was observed microscopically, only when the alloys contained above 0.04 per cent carbon.

Bose[8] made a study of the effects of manganese on the annealing brittleness of 75-25 copper-nickel alloys containing 0.08-0.14 per cent carbon. Melts with manganese additions ranging from 0.10 to 5.0 per cent were made. Carbon additions were made by means of a nickel alloy containing two per cent carbon. Chemical analyses of the alloys showed that in the cast condition they contained little or no free carbon, but after annealing for 2½ hr. at 800 C., followed by furnace cooling, they contained 0.044 to 0.089 per cent graphitic carbon. Tensile and izod tests indicated that annealing brittleness was virtually eliminated by the addition of 0.10 per cent manganese, although manganese up to one per cent was formed to have a negligible effect on the amount of carbon absorbed by the alloy or the amount of graphite precipitated on annealing. The presence of manganese the author assumes influences the carbon precipitation so that its distribution is not intergranular.

* Any views or opinions contained herein are the personal views of the authors and do not reflect the official opinion of the Navy Dept.

** Metallurgist and Metallurgical Supt. respectively, Material Laboratory, New York Naval Shipyard.

It is generally agreed that carbon, nitrogen, hydrogen and boron, sometimes referred to as the metalloids, are capable of forming interstitial types of liquid and solid solutions with the transition elements of which nickel is one. Hagg[9] found that these alloys are usually formed when the following conditions are satisfied:

1. The ratio of the atomic diameter of the interstitial atom to that of the solvent atoms must be less than 0.59. If the diameter ratio is larger than 0.59 the alloys rarely form.

2. The larger solvent atom must be one of the transition elements, i.e., one of the polyvalent metals such as nickel, titanium, zirconium, etc.

Carbon monoxide or carbon-oxide reactions were not considered factors in causing porosity in cast monel. Heats melted with as high as a 2 per cent CuO addition showed inconsistent results upon deoxidation with magnesium. Air or oxygen bled into the barrel only served to minimize rather than eliminate porosity conditions. In addition, the fact that gas solubilities invariably obey Sieverts' Law, i.e., vary in solubility with the square root of the partial pressure, makes it difficult to conceive of the solubility of a polyatomic gas, as such.

Foundry Procedure

The general procedure utilized by the New York Naval Shipyard foundry in the production of monel in the indirect arc furnace was briefly as follows: Charge commercial ingot and foundry returns into a well preheated barrel, superheat to 2850 F, add ferromanganese and metallic silicon three to five minutes prior to tapping, tap at approximately 2900 F and then deoxidize with 1½ oz of magnesium per 100 lb. of molten metal. Procedures were tried with various types of covers, by bleeding air and oxygen into the barrel or by introducing CuO into the melt. However, no consistent results from the standpoint of radiographically sound castings or test bars yielded satisfactory fractures and physical properties could be achieved.

It was observed that in those heats which exhibited poor physical properties and gas porosity, there was usually a white cloud formation in the barrel of the furnace at the transition from solid to liquid which choked the arc and obscured the vision. J. C. Vaughn and John Chipman[10] noted the same phenomena while melting in an induction unit in a study of the solubility of nitrogen in molten iron-silicon alloys and tentatively ascribed it to the formation of silicon nitride.

Resistor Type Furnace Heats

Comparative heats from a resistor type furnace[11] showed satisfactory and consistent results utilizing the same procedure as described above for the arc furnace. This unit is essentially of the same physical characteristics, dimensionally, as the indirect-arc furnace and utilizes the same type of refractory lining. However, radiant heat is supplied by graphite resistors in lieu of an arc. Thus it became apparent that the solution to the problem was inherent in the furnace atmosphere as a result of arc melting.

Table 1.—Constituents of Charges for Test Heats

Heat No.	Furnace	Total Charge (Lb)	Ingot (Lb)	Returns (Lb)	Metallic Silicon	Fe-Mn
F2209	Indirect Arc	450	450	—	1 lb 4 oz	1 lb 4 oz
F2245	Resistor	300	300	—	—	—
6446	Resistor	450	135	315	1 lb 15 oz	13 oz
6447	Indirect Arc	450	135	315	1 lb 15 oz	13 oz
6553	Indirect Arc	450	135	315	8 oz	—
6836	Indirect Arc	450	450	—	8 oz	—
6837	Indirect Arc	450	450	—	8 oz	—
6843	Indirect Arc	450	450	—	8 oz	—
6933	Indirect Arc	450	450	—	8 oz	—
6585	Indirect Arc	450	450	—	8 oz	—
F2297	Indirect Arc	300	300	—	5 oz	—
6629	Resistor	450	450	—	8 oz	—
F2416	Indirect Arc	450	450	—	1 lb 13 oz	1 lb 11 oz
F2475	Indirect Arc	350	350	—	2 lb 2 oz	1 lb 4 oz
F2460	Indirect Arc	450	450	—	2 lb 11 oz	1 lb 11 oz

Ionization of Furnace Atmosphere

When melting in the indirect-arc furnace, the temperatures involved and the arc itself cause partial ionization of the furnace atmosphere. In the process of ionization, sufficient energy is absorbed so that a portion of the molecules present break down into free electrons and positively charged particles or ions. As a result the gas becomes conducting. This was clearly established during some development work with a 2-phase arc furnace[12] when cross-arcing between opposite phases occurred during operation.

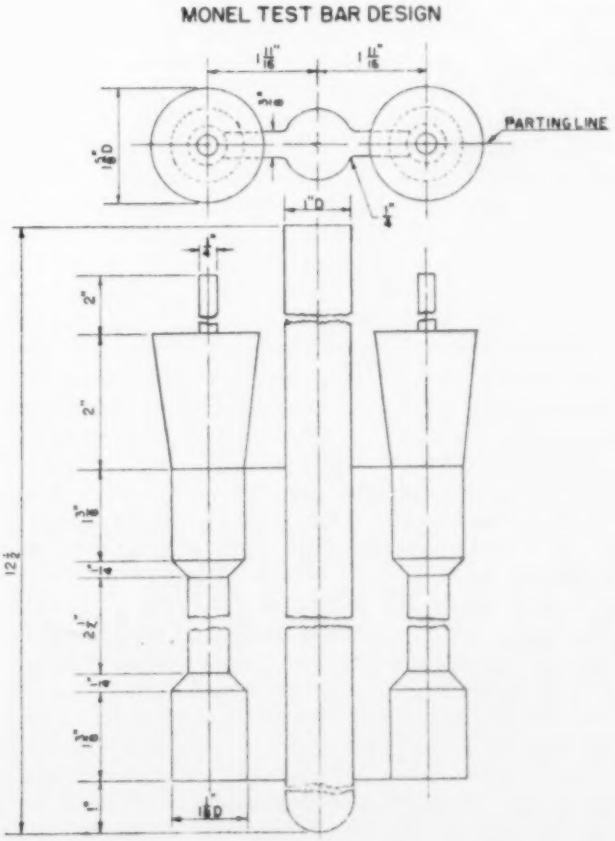


Fig. 1—Sketch of monel test bar design.

In addition, the possibility exists that the electrostatic charge on the ionized gas may have some bearing on its increased sensitivity to absorption by molten metal. In general, the phenomenon that occurs in melting monel metal in the indirect arc furnace is analagous to the mechanism which explains the fact that nascent gases are much more soluble in molten metal than are molecular gases. The latter first having to acquire sufficient energy for dissociation to conform with Sieverts' Law.

Approach to the Problem

In view of the sparsity of published data on the effects of hydrogen and nitrogen in cast monel and in the light of the considerations outlined above, it was obvious that the effects of these two gases on cast monel had to be investigated in arriving at any solution to the problem. It was decided, therefore, as a first step, to utilize various deoxidizers such as titanium and zirconium, two transition elements which are known to be strong nitride formers. In fact, the possibility cannot be discounted, in light of the relationship shown above by Hagg [13] and others, that certain of the transition elements such as titanium and zirconium might form stable hydrides at temperatures below that of steelmaking.

Foundry Practice—All test heats were melted in either the indirect-arc furnace or the resistor type furnace. The constituents of all the test heat charges and the type furnace utilized are shown in Table 1 for all the test heats. Commercial ingot and in some instances part of the output of a production heat were used in the tests. The melting procedures were essentially as outlined before and separate taps were made and treated with the various deoxidizers except in cases which will be specifically noted. Final analysis for all the test heats are shown in Table 2. The sand utilized for making the test bar molds and plug molds was synthetic in nature of following composition:

250 lbs AFA 60 silica sand
250 lbs Monel heap sand
14 qts Bentonite
2 qts Mogul
3.0–3.5% Moisture

All molds were silica washed and skindried.

In carrying out the first stage of experimentation, test heats were melted in the indirect-arc furnace and comparisons of the physical properties of magnesium deoxidized test bars and titanium treated test bars were made. Comparative heats were run in the resistor furnace showing the relative effect on the physical properties by deoxidizing with magnesium and titanium.

A study was made in several test heats of the relative degree of gas porosity evidenced after deoxidation and/or degasification with several types of deoxidizers. Plugs 3 in. in diameter and 7 in. long, sectioned down the center line and etched, were utilized for this purpose. The degree of piping and of gas porosity evident in the etched surface was used as an indication of the relative degree of gas absorbed during melting and rejected during solidication. Physical properties of test bars poured from the same ladle as its corresponding plug were also obtained and compared with the etched plugs. Photographs of the fractured test bars were made in some instances to show the nature of the fracture and to correlate it with the physical properties of the test bars and the degree of piping and gas porosity exhibited by the plugs.

Test heats were melted to show the comparative effect of an air blast on gas absorption during melting. Test heats were run with and without air blasts to show the relative effect on sectioned keel block risers and plugs and on the physical properties of test bars.

Comparisons of physical properties were also made between the conventional keel block test bar (see Navy Department Specification 49B3F) and the test bar shown in Fig. 1. The latter is hereafter designated in this paper as the regular test bar. These comparisons were made in order to indicate the relative ability of these bars to reflect metal quality before degasification and/or deoxidation.

Further studies of the effect of titanium additions on specific types of gas porosity such as nitrogen and hydrogen were attempted. Test heats were inoculated with calcium nitride and magnesium nitride and the relative effects produced by these additions and by subsequent deoxidation with titanium were shown by means of etched plugs and physical properties. Test heats were also inoculated with hydrogen by inoculating the melt with calcium hydride. Here again the effects produced by the calcium hydride additions and by subsequent fixation with titanium were studied as indicated above for nitrogen.

Complete furnace log operating data and tapping and pouring temperatures were kept. Metallographic studies of all heats were made and in some instances comparative micro-radiographs were prepared.

Effect of Various Treatments on Properties

The effect of the no deoxidation, magnesium deoxidation, titanium fixation and a combination of magnesium deoxidation and titanium fixation on the physical properties of a cast monel heat (F2209) melted in the indirect-arc electric rocking furnace are shown in Table 3. Keel block test bars were utilized. It will be noted that no deoxidation yielded unsatisfactory physical properties, magnesium deoxidation yielded slightly better but still unsatisfactory properties and the properties obtained upon titanium fixation and magnesium deoxidation plus titanium fixation were well above the minimum requirements of Navy Department Specification 46Mlg for monel castings.

A comparative heat (F2245) melted in the resistor furnace yielded satisfactory physical properties (Table 3) upon treatment with both magnesium and a combination of titanium and magnesium. In addition two duplicate test heats (6446 and 6447) were melted in the indirect-arc furnace and the resistor furnace, respectively, to show the comparative physical properties obtained on a keel block test bar deoxidized with magnesium alone and then treated with a combination of magnesium and titanium. These results are also shown in Table 3 and confirm the results obtained above. An ingot which was considered unsatisfactory due to a very high inclusion count was utilized for this experiment.

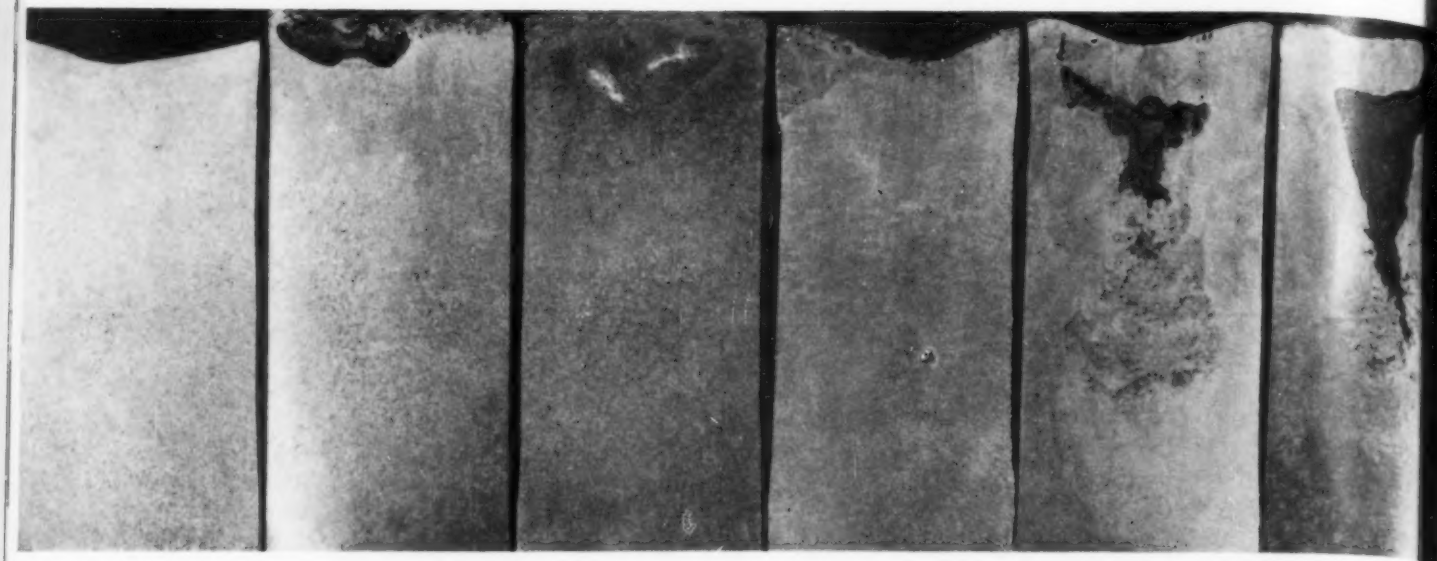
Effect of Deoxidizers in Degasification

Figure 2 illustrates the gas porosity effect produced in six sectioned and etched plugs melted in the indirect-arc furnace (Heat No. 6553) by no deoxidation, magnesium deoxidation above 2750 F, magnesium deoxidation at 2600 F, aluminum deoxidation, ferro-aluminum-titanium fixation and a combination of ferro-aluminum-titanium and magnesium deoxidation. It will be noted that this heat utilized approximately 30 per cent ingot material and 70 per cent foundry returns and that an air blast was used to prevent the formation of the "smoke condensate" in the furnace barrel.

The above experiment was repeated in Heat No.

TABLE 2.—CHEMICAL ANALYSES OF MONEL TEST HEATS

Heat	Sample	Elements, Per Cent												N
No.	No.	C	Mn	Si	Ni	Cu	Cr	Pb	Al	Fe	Mg	Ti	S	(Total)
	1	.15	1.00	1.65	63.38	32.06	.05	.00	.00	1.67	.03	.013	.003	.004
	2	.14	1.04	1.62	63.12	32.19	.05	.00	.00	1.77	.05	.013	.010	.003
F2209	3	.15	.99	1.54	62.93	32.07	.06	.00	.00	2.17	.04	.05	.008	.005
	4	.14	.96	1.63	63.00	32.18	.06	.00	.00	1.93	.05	.05	—	
	1	.08	.96	1.78	64.57	29.72	.07	.00	.00	2.70	.12	.005	.034	.005
F2245	2	.14	.96	1.78	64.54	29.70	.10	.00	.00	2.74	.04	.05	.034	
	1	.07	1.06	1.47	64.26	31.03	.05	.00	.00	1.94	.12	.013	.008	
6446	2	.08	1.08	1.47	64.05	30.67	.07	.00	.00	2.41	.10	.05	.008	
	1	.12	1.06	1.54	64.48	30.89	.07	.00	.01	1.76	.05	.013	.008	
6447	2	.08	1.03	1.52	64.27	30.99	.04	.00	.00	2.07	.08	.05	.008	
	1	.14	1.09	1.69	64.34	30.35	.05	.03	.00	2.29	.00	.02	.018	.006
	2	.12	1.01	1.68	64.10	30.52	.05	.03	.00	2.26	.21	.02	.018	.007
	3	.11	1.02	1.68	63.86	30.61	.05	.02	.00	2.37	.26	.02	.018	
6553	4	.12	1.03	1.64	64.29	30.46	.06	.02	.03	2.33	.00	.02	.018	
	5	.09	1.04	1.68	64.08	30.41	.09	.02	.00	2.53	.00	.06	.018	.005
	6	.09	1.04	1.69	63.93	30.47	.06	.02	.00	2.52	.12	.06	.018	
	2	.18	.97	1.87	64.49	29.52	—	.00	.01	2.90	.01	.009	.014	
	3	.18	.94	1.84	64.47	29.58	—	.00	.01	2.92	.01	.009	.014	
6836	4	.18	1.02	1.99	64.16	29.62	—	.00	.01	2.92	.09	.10	.014	
	5	.17	1.00	1.95	64.88	29.81	—	.00	.01	3.04	.06	.08	.014	
6837	2	.18	.99	2.06	64.79	29.41	—	.00	—	2.52	.00	.04	.014	
	2	.16	.94	1.86	64.59	29.59	—	.00	.01	2.80	.00	.010		
	5	.16	.98	1.84	64.77	29.63	—	.00	.01	2.38	.19	—		
	7	.16	.96	1.86	65.10	29.22	—	.00	.01	2.40	.25	—		
6843	9	.15	.97	1.86	64.62	29.55	—	.00	.01	2.80	.00	—		
	11	.15	.93	1.91	64.24	29.88	—	.00	.01	2.80	.17	—		
	13	.15	1.00	1.86	64.28	29.78	—	.00	.00	2.10	.00	—		
	2	.16	.90	1.82	65.98	28.96	.07	.00	.01	2.60	.08	.010	.023	
	4	.17	.91	1.82	65.42	29.30	.07	.00	.00	2.08	.15	.08	.023	
	6	.16	.90	1.85	65.41	29.33	.10	.00	.00	2.18	.00	.08	.023	
6933	8	.18	.87	1.82	65.19	29.29	.10	.00	.00	2.40	.00	.08	.023	
	10	.17	.86	1.80	65.72	29.38	.06	.00	.00	2.00	.00	.009	.023	
	12	.17	.91	1.78	63.89	31.00	.07	.00	.00	2.08	.00	.010	.023	
	14	.18	.90	1.80	65.47	29.28	.07	.00	.00	2.00	.29	.010	.023	
	1	.14	.99	1.82	65.12	29.30	—	.00	—	2.47	.01	.01	.032	
6585	2	.16	.96	1.78	65.17	29.21	—	.00	—	2.60	.01	.01	.032	
	3	.16	.98	1.77	65.03	29.22	—	.00	—	2.58	.11	.05	.032	
	1	.16	.98	1.82	64.76	29.33	.09	.00	.00	2.84	.01	.01	.039	
F2297	2	.15	.95	1.85	64.94	29.18	.07	.00	.00	2.84	.01	.01	.039	
	3	.18	1.09	1.82	64.22	29.28	.06	.00	.00	3.20	.11	.04	.039	
	1	.17	1.01	1.82	64.98	29.29	—	.00	.02	2.60	.006	.02	.017	
	2	.16	1.01	1.82	65.16	29.34	—	.00	.00	2.44	.008	.02	.017	
	3	.16	.99	1.82	64.82	29.37	—	.00	.02	2.70	.04	.015	.017	
6629	4	.17	1.02	1.82	64.89	29.47	—	.00	.00	2.44	.08	.017	.017	
	5	.15	.99	1.85	64.94	29.38	—	.00	.00	2.50	.06	.059	.017	
	6	.16	1.04	1.82	64.76	29.47	—	.00	.00	2.60	.04	.04	.017	
	2	.15	.90	1.52	63.75	32.39	.04	.00	.00	1.22	.01	.015		
	4	.17	.89	1.52	63.35	32.37	.04	.00	.00	1.63	.01	.016		
F2416	6	.17	.88	1.54	63.21	32.42	.04	.00	.00	1.54	.18	.017		
	8	.16	.88	1.54	62.15	33.39	.03	.00	.00	1.65	.15	.05		
	1	.13	.93	1.59	64.36	31.31	.04	.00	.01	1.61	.00	.02		
F2475	4	.13	.92	1.58	63.86	31.79	.04	.00	.02	1.64	.07	.02		
	6	.12	.93	1.56	63.62	31.61	.04	.00	.02	2.01	.12	.09		
	MIA	.17	1.04	1.74	63.43	31.98	.00	.00	.01	1.58	.02	.02		
	MH1A	.16	1.08	1.73	63.29	32.06	.01	.00	.00	1.63	.01	.02		
	MH2A	.17	1.04	1.66	63.68	31.71	.01	.00	.01	1.69	.00	.02		
F2460	**MH3A**	.17	1.11	1.76	63.27	31.95	.01	.00	.02	1.68	.00	.02		
	MHT4A	.18	1.09	1.94	63.16	31.54	.01	.00	.02	1.70	.01	.111		
	MHT5A	.17	1.08	1.77	62.03	32.13	.01	.00	.00	2.60	.01	.240		
	MHT6A	.18	1.10	1.87	60.99	32.30	.01	.00	.01	2.94	.01	.609		



	Ast Cast No deoxidation	Magnesium (above 2750 F) 3½ oz/100 lb	Magnesium (2600 F) 3½ oz/100 lb	Aluminum 3½ oz/100 lb	Fe-Al-Ti 7½ oz/100 lb	Mg and Fe-Al-Ti 1½ oz Mg/100 lb 4 oz FeAlTi/100 lb
			PHYSICAL PROPERTIES			
Y.P., psi	35,375	39,000	Defective	37,375	38,750	38,125
T.S., psi	76,000	62,750	bar	79,500	82,000	81,250
El., % in 2 in.	37.0	19.0	–	44.5	40.5	40.5

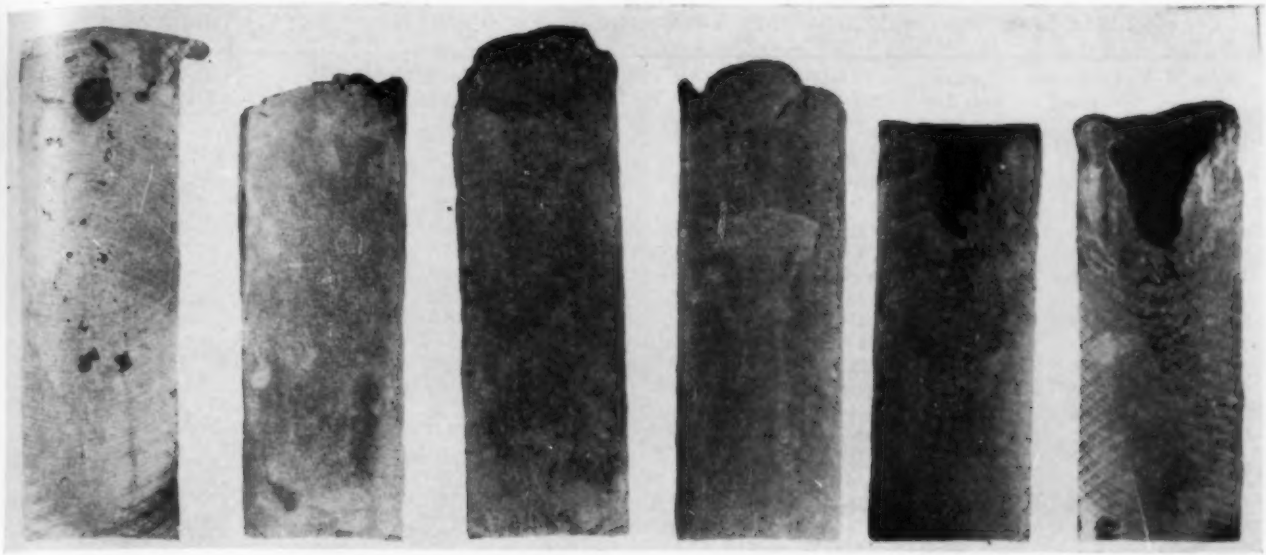
Fig. 2—Sectioned and etched monel plugs, Heat No. 6553, melted in indirect arc furnace with an air blast and treated with various deoxidizers.

Fig. 4—Sectioned and etched monel plugs, Heat No. 6933, melted in indirect arc furnace without an air blast and treated with various deoxidizers.

				PHYSICAL PROPERTIES			
Y.P., psi	31,000	39,500	40,000	40,250	39,000	41,250	39,000
T.S., psi	42,750	84,750	70,375	67,875	73,500	88,000	39,000
El., % in 2 in.	12.0	43.0	25.0	18.0	24.0	39.0	–

As Cast No deoxidation	Mg and Fe-Al-Ti 4 oz Mg/100 lb 4 oz FeAlTi/100 lb	Ca_3N_2 and Fe-Al-Ti 1 oz Ca_3N_2/ 100 lb 3 oz FeAlTi/100 lb	Additional Fe-Al-Ti to Plug No. 3 3 oz/100 lb	Ni-Zr 4 oz/100 lb	Li-Cu 1½ oz/100 lb	Ni-Mg 5 oz/100 lb





As Cast No deoxidation	Magnesium (2900 F) 5 oz/100 lb	Magnesium (2660 F) 5 oz/100 lb	Aluminum 4 oz/100 lb	Fe-Al-Ti 4 oz/100 lb	Mg and Fe-Al-Ti 3 oz Mg/100 lb 4 oz FeAlTi/100 lb
PHYSICAL PROPERTIES					
Y.P., psi 29,500	33,500	38,500	32,500	40,750	37,500
T.S., psi 42,500	39,500	56,250	57,500	82,250	75,500
El., % in 2 in. 16.5	9.5	12.5	21.5	42.0	26.5

Fig. 3—Sectioned and etched monel plugs, Heat No. 6843, melted in indirect arc furnace without an air blast and treated with various deoxidizers.

Fig. 5—Typical photomicrographs showing degree of porosity exhibited in the as-polished condition at 100× after treatment with various deoxiders. Heat No. 6843. A, no deoxidation; B, magnesium deoxidation above 2750 F; G, magnesium deoxidation at 2600 F; D, aluminum deoxidation; E, Fe-Al-Ti deoxidation; and Fe-Al-Ti plus Mg deoxidation.

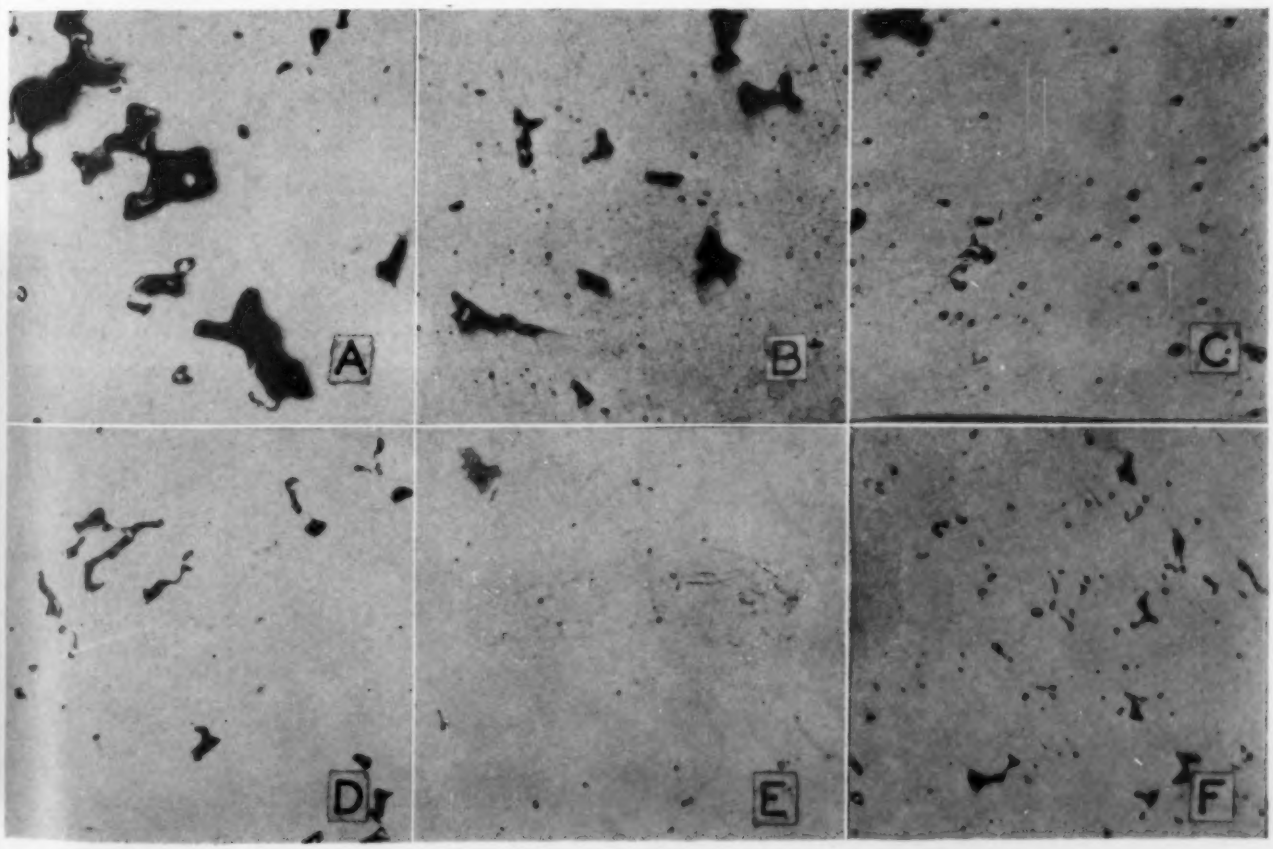


TABLE 3.—EFFECT OF DEOXIDATION TREATMENT ON PHYSICAL PROPERTIES OF CAST MONEL

Heat No.	Specimen No.	Type of Test Bar	Deoxidizer Per 100 lb Metal	Yield Pt. PSI	Ultimate PSI	El (%) in 2 In.	Tapping Temp. °F	Pouring Temp. °F
	1A	Keel	None	27,250	39,875	18.5	2850	2790
	1B	Keel	None	26,750	36,000	17.0	2850	2790
	2A	Keel	1½ Oz-Mg	27,500	46,125	22.5	2890	2790
	2B	Keel	1½ Oz-Mg	28,625	51,000	26.5	2890	2790
F2209	3A	Keel	4 Oz-Fe-Al-Ti	34,750	74,375	40.0	2930	2790
(Indirect	3B	Keel	4 Oz-Fe-Al-Ti	33,250	73,750	45.5	2930	2790
Arc)			4 Oz-Fe-Al-Ti					
Furnace	4A	Keel	1½ Oz-Mg	36,375	77,500	46.5	2960	2790
			4 Oz-Fe-Al-Ti					
	4B	Keel	1½ Oz-Mg	36,875	79,125	43.0	2960	2790
	1A	Keel	1½ Oz-Mg	—	77,250	37.5	2840	2740
F2245	1B	Keel	1½ Oz-Mg	33,000	77,250	34.0	2840	2740
(Resistor)			4 Oz-Fe-Al-Ti					
	2A	Keel	1½ Oz-Mg	—	81,500	38.5	2840	2720
			4 Oz-Fe-Al-Ti					
	2B	Keel	1½ Oz-Mg	40,375	82,125	35.0	2840	2720
6446	1A	Keel	1½Oz-Mg	36,000	68,125	25.5	2840	2750
(Resistor)			4 Oz-Fe-Al-Ti					
	2A	Keel	1½ Oz-Mg	34,500	73,500	33.5	2840	2740
6447	1A	Keel	1½ Oz-Mg	24,750	40,500	20.0	2940	2850
(Indirect			4 Oz-Fe-Al-Ti					
Arc)	2A	Keel	1½ Oz-Mg	34,500	72,250	43.0	2940	2860

6843 with the exception that an all ingot charge was utilized and that no air blast was used. The sectioned and etched plugs are shown in Fig. 3. Another test heat was melted (Heat No. 6933) in which the effect of various other types of deoxidizers on gas porosity was investigated. Figure 4 illustrates the effect of no treatment, ferro-aluminum-titanium plus magnesium deoxidation, calcium nitride plus ferro-aluminum-titanium, additional treatment of the latter with ferro-aluminum-titanium, nickel-zirconium treatment, lithium-copper treatment and nickel magnesium deoxidation. An all ingot charge was utilized and the heat was melted without an air blast.

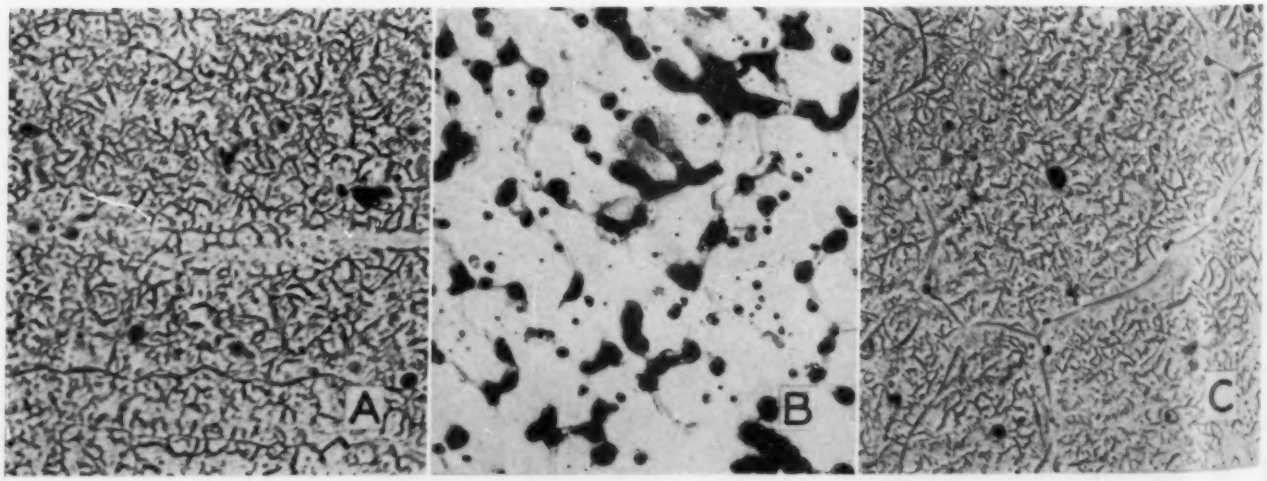
A regular type test bar was poured with each of the plugs from Heat No. 6533, 6843 and 6933 described above. The physical properties obtained are tabulated below the photographs of the sectioned and etched plugs in Figs. 2, 3 and 4 respectively.

Experimental Observations

From Figs. 2, 3 and 4 the following salient facts are apparent:

1. All magnesium deoxidized heats showed considerable porosity, slight bleeding and poor physical properties particularly in the heat melted without an air blast. Magnesium additions were made both at high temperatures and the lower temperatures due to the possibility of the formation of a stable magnesium nitride at the lower temperature. However, there were no significant differences in either the physical properties or in the degree of porosity as evidenced by the etched plugs.

Fig. 6—Photomicrographs of representative microstructures of cast monel, Heat No. 6553, after treatment with various deoxiders. Etch—50% HNO_3 and 50% acetic acid. Mag.—100×. A, no deoxidation; B, Mg deoxidation above 2750 F; C, Fe-Al-Ti deoxidation.



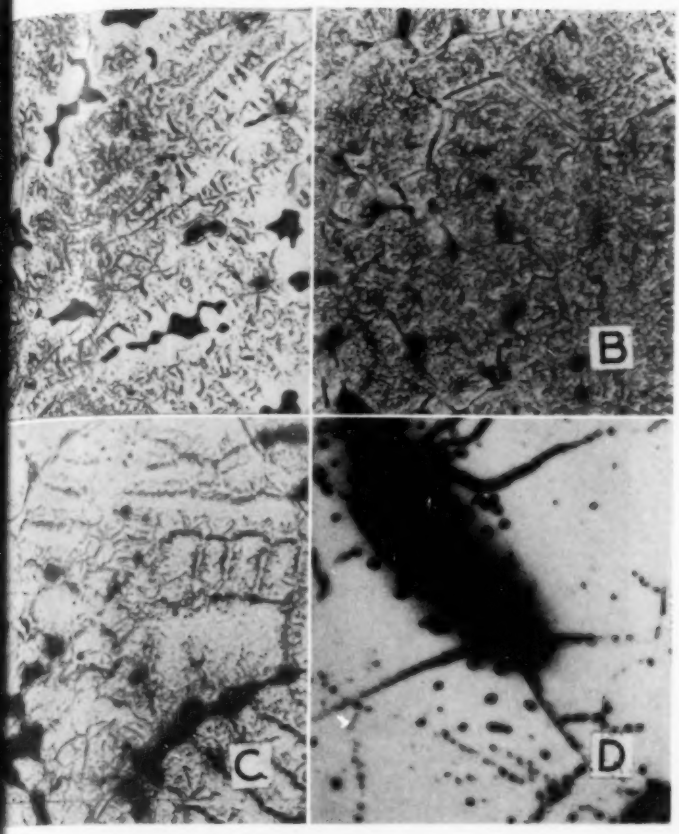
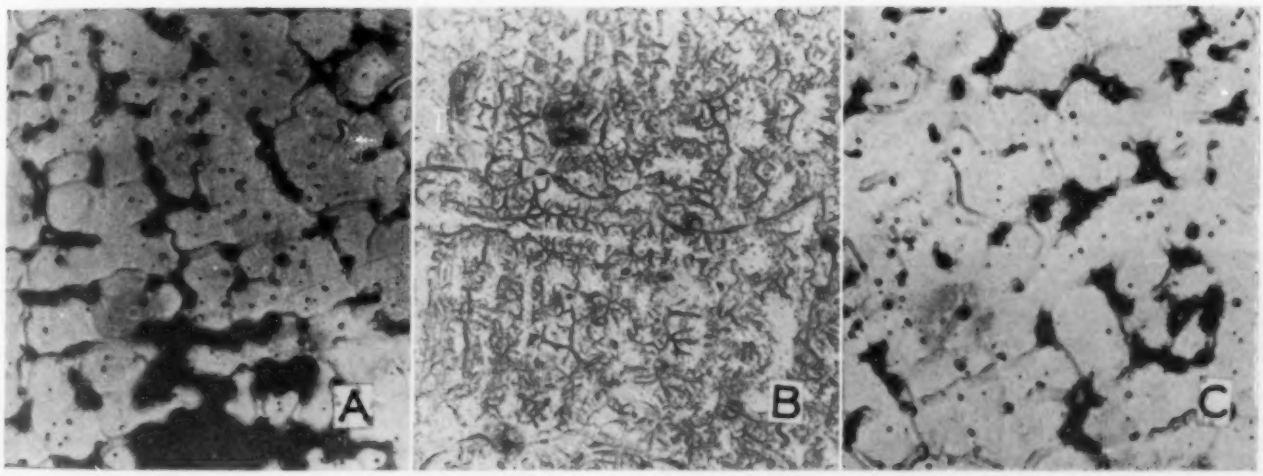
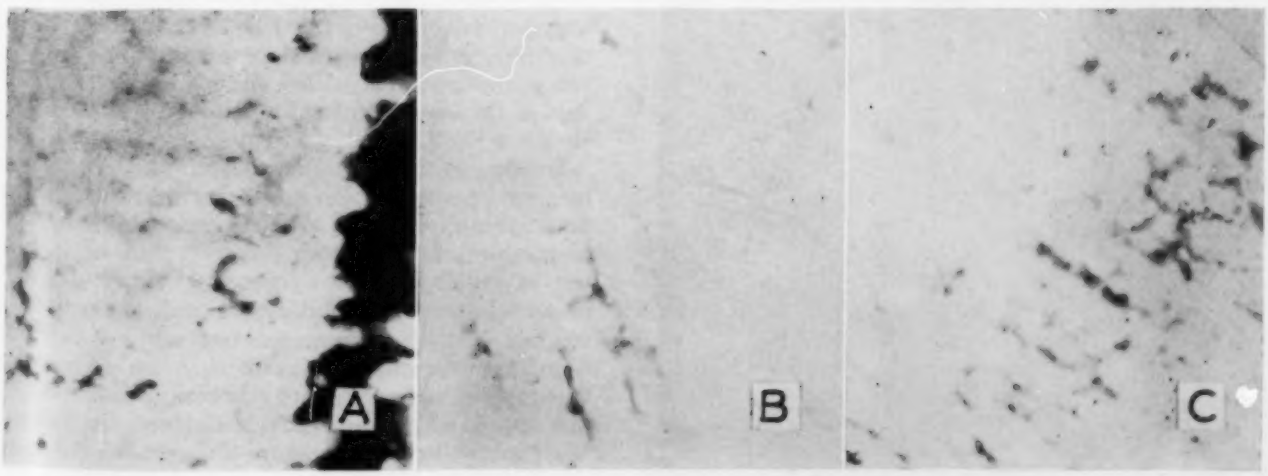


Fig. 7—Photomicrographs of aluminum, zirconium and lithium-deoxidized bars. Etch—50% HNO_3 and 50% acetic acid. Mag.—A, B, C—100×, D—1700×. A, aluminum deoxidation, Heat No. 6843; B, Ni-Zr deoxidation, Heat No. 6933; C, Li-Cu deoxidation, Heat No. 66933; D, titanium cyano-nitride inclusions, Heat No. 6533.

Fig. 8—Photomicrographs of cast monel specimens, Heat No. 6843. Etch—50% HNO_3 and 50% acetic acid. Mag.—100×. A, magnesium deoxidation above 2750 F; B, Fe-Al-Ti deoxidation; C, Mg and Fe-Al-Ti deoxidation.

Fig. 9—Microradiographs of cast monel specimens, Heat No. 6843. Mag.—100×. A, magnesium deoxidation above 2750 F; B, Fe-Al-Ti deoxidation; C, Mg and Fe-Al-Ti deoxidation.





2. Plugs treated with titanium, lithium and zirconium, all of which are strong nitride formers, showed good shrinkage and sound metal. The presence of the deep pipe is significant in showing absence of sufficient quantities of absorbed gas which if present exerts an internal gas pressure upon solidification, resisting normal shrinkage. The physical properties obtained after treatment with these elements were satisfactory and the fractures were sound and silky.

3. Deoxidation with aluminum showed fairly sound metal in the plug with satisfactory physical properties in the heat where an air blast was used and a "back-up" and porosity in the plug with corresponding poor physical properties in the heat where no air blast was used.

4. When no deoxidation treatment was utilized, the plug and test bar poured from the heat where an air blast was used showed some pinhole porosity and good physical properties while the plugs and test bars poured from the two heats melted without an air blast, showed gross porosity and poor properties.

Microstructures

Figure 5 verifies results shown for etched plugs and physical properties obtained from regular test bars. The photomicrographs taken from transverse sections of the tension specimens from Heat No. 6843 show gross porosity in the non-deoxidized condition, considerable porosity in the magnesium deoxidized bars, some porosity in the aluminum deoxidized bar, a relatively sound structure in the titanium treated bar and a slightly more porous condition for the titanium and magnesium deoxidized bar.

Fig. 10—Sectioned and etched monel keel block risers showing effect of air blast on gas absorption during melting and its rejection during solidification. As cast, no deoxidation. Left, Heat No. 6585 subjected to air blast, right, Heat No. F2297, not subjected to blast.

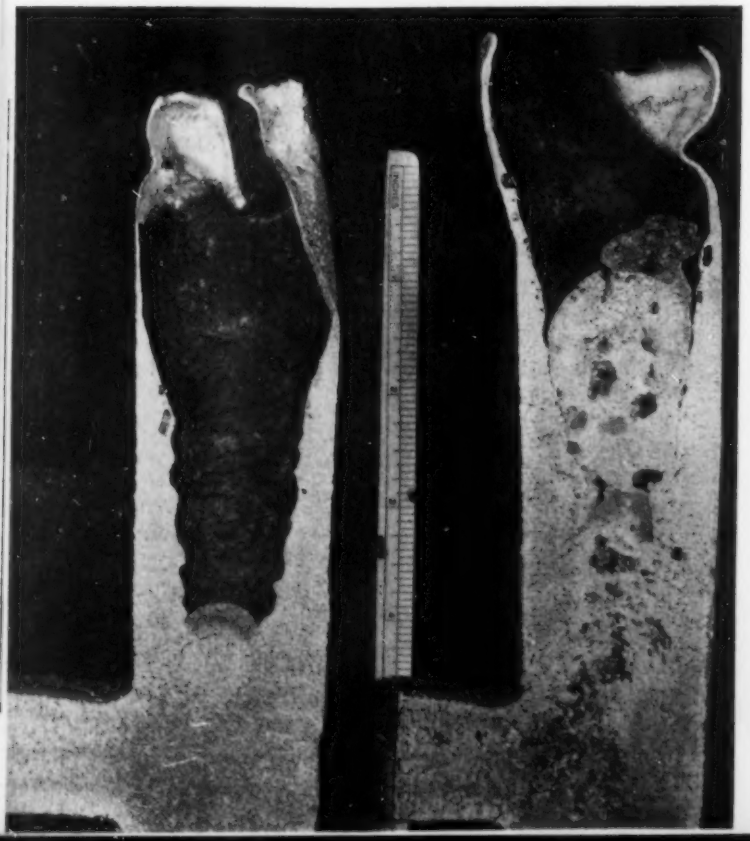




Fig. 11—Sectioned and etched monel keel block risers showing effect of air blast on gas absorption during melting and its rejection during solidification. As cast, no deoxidation. Left, Heat No. 6836, subjected to air blast, right, Heat No. 6837, not subjected to blast.

Figure 6 illustrates the significant differences in microstructure between the magnesium deoxidized bar and the bars which had no deoxidation and a titanium treatment. It will be noted that the magnesium deoxidized bar exhibits the typical solid solution microstructure for cast monel while the latter two show a structure akin to a modified alloy. Photomicrographs of the aluminum, zirconium and lithium treated bars show (Fig. 7) the same tendency as reported for titanium. The lithium treated bar, however, appeared to show more evidence of dendrite formation.

Plugs and test bars poured from the lithium treated metal showed excellent fluidity. Production castings were satisfactorily poured as low as 2550 F. In the bars treated with titanium the presence originally of some soluble nitrogen is established by the identification of the insoluble titanium cyano-nitride inclusion shown in Fig. 7.

In Fig. 8, the photomicrograph which represents magnesium deoxidation of Heat No. 6843 (no air blast) illustrates again the gross, interdendritic type of porosity resulting from this treatment while melting in the indirect-arc furnace. A microradiograph of an adjacent section illustrates this perhaps more forcibly (Fig. 9). Figure 8 shows again the typical sound structure obtained with titanium deoxidation and may be compared with the corresponding microradiograph of an adjacent section which shows a fairly sound structure. Figure 8 also shows the struc-

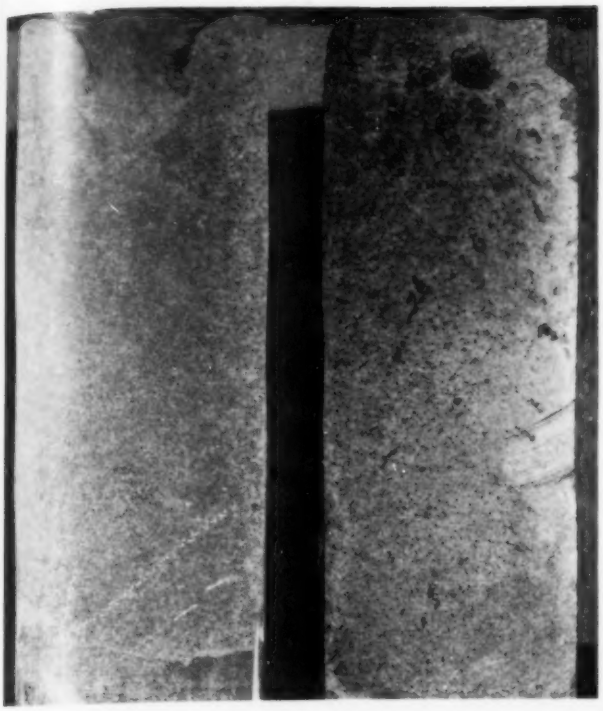


Fig. 12—Sectioned and etched monel plugs showing effect of air blast on gas absorption during melting and its rejection during solidification. As cast, no deoxidation. Left, Heat No. 6836, subjected to air blast, and right, Heat No. 6837, not subjected to air blast.

ture obtained after treatment with titanium and magnesium. More porosity is in evidence than with straight titanium fixation. This is verified by the comparative microradiograph shown in Fig. 9.

Results of Deoxidation Treatments

The conclusions following are indicated by the generally poor results shown for all treatments except titanium, zirconium and lithium upon melting in the indirect-arc furnace, the fact that in the air blast heats the results with magnesium deoxidization were inferior to those shown for no deoxidization or aluminum deoxidization, and the favorable results obtained with magneusium deoxidization in the resistor furnace would seem to indicate:

1. Gas absorption by monel metal in the indirect-arc furnace is greater than in the resistor furnace. It is believed that this increased rate of gas solubility is caused by ionization of the furnace atmosphere by the arc.

2. The utilization of the air blast during melting in the indirect-arc furnace tends to suppress gas absorption during melting. It is believed that this suppression is due primarily to the decrease in the percentage ionization of the furnace atmosphere.

3. When appreciable amounts of the metalloid gases are in solution, magnesium acts as either a nucleating agent or tends toward compound formation with these gases which are unstable at the temperatures involved, thus causing rejection of the gas upon solidification.

Effect of Air Blast During Melting

The tendency of the air blast during melting in the indirect-arc furnace to suppress gas absorption and final rejection upon solidification, as discussed above, is shown more effectively in Figs. 10, 11 and 12. Figure 10 shows longitudinally sectioned and etched keel block risers from heats No. 6585 and F2297. No deoxidation was effected and an air blast was utilized on the former heat only. This experiment was repeated in heats No. 6836 and 6837. Here again no deoxidation was effected and an air blast was utilized on the former heat only. Figure 11 shows one sectioned and etched keel block risen from each heat and Fig. 12 shows photomacrographs of one sectioned plug from each heat. It will be noted that gross porosity is generally greater and the amount of "back-up," indicative of rejected gas upon solidification, is also greater in those heats in which no air blast was used.

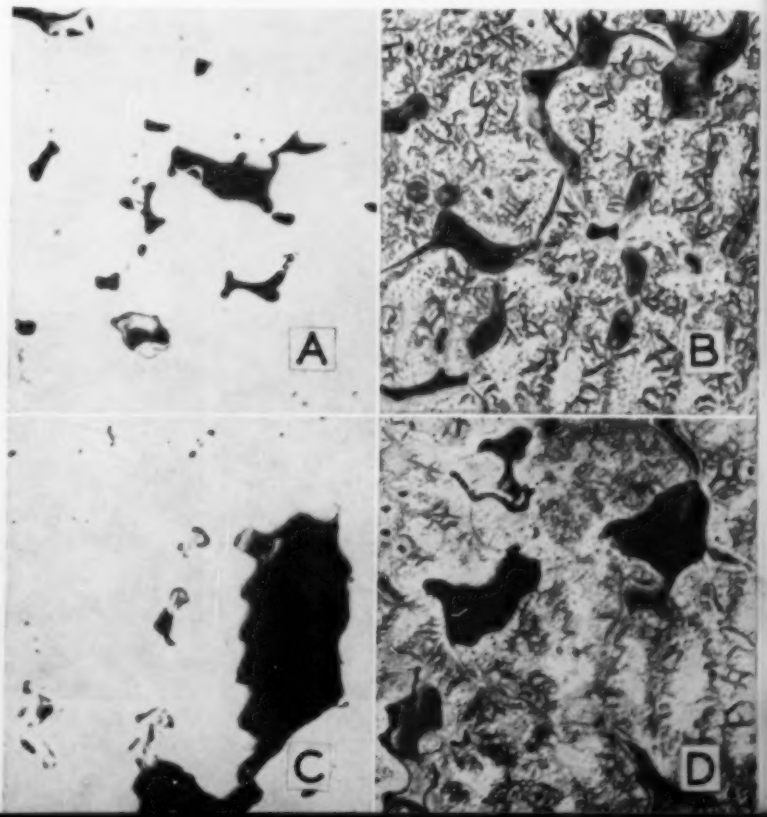
It will be noted in Table 4 that:

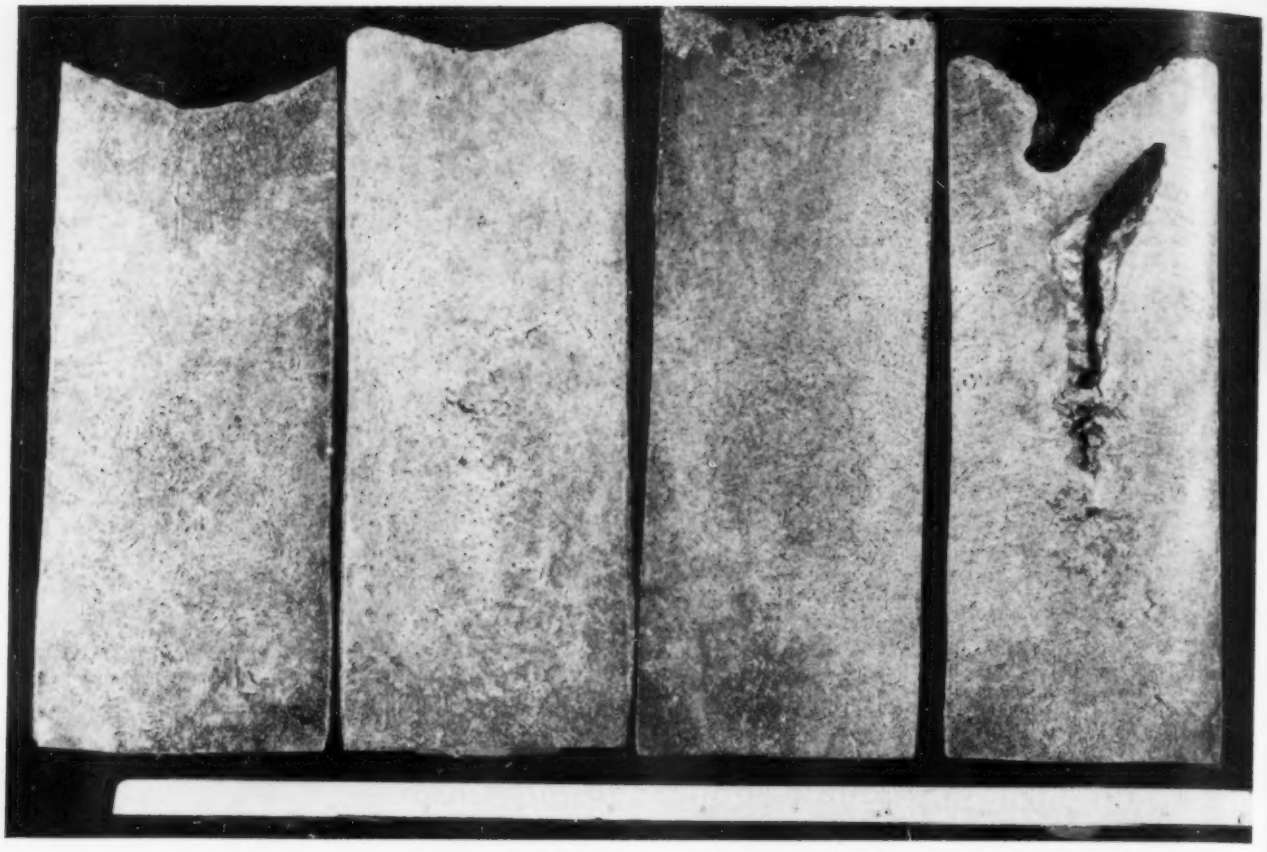
1. In melting in the indirect-arc furnace with an air blast the non-deoxidized regular test bars were barely below the minimum requirements for Navy Department Specification No. 46Mig (nickel-copper castings) while the non-deoxidized keel block test bars showed very poor physical properties.

2. In melting in the indirect-arc furnace without an air blast both test bars showed poor physical properties, the keel block properties being inferior.

3. In melting in the resistor furnace both the keel block and regular test bars yielded comparable and

Fig. 13—Comparative microstructures showing effect of air blast during melting. B and D etched with 50% HNO_3 and 50% acetic acid. Mag.—100×. A and B, Heat No. 6836, aid blast used, no deoxidation; C and D, Heat No. 6837, no air blast used, no deoxidation.





As Cast No deoxidation	Ca_3N_2 1½ oz/100 lb	Mg addition to Nitrogen inoculated metal, 2 oz/100 lb	Ti addition to Mg and N inoculated metal, 4 oz FeAlTi/100 lb

Fig. 14—Effect of various deoxidizers on soundness of cast monel inoculated with a nitride. Heat No. F2416.

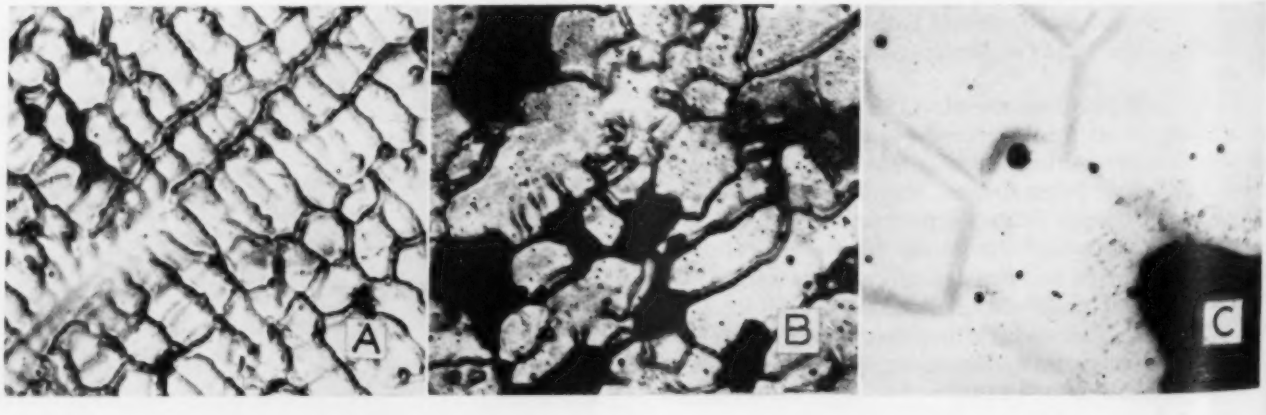
satisfactory properties when non-deoxidized, deoxidized with magnesium and deoxidized with a combination of magnesium plus titanium.

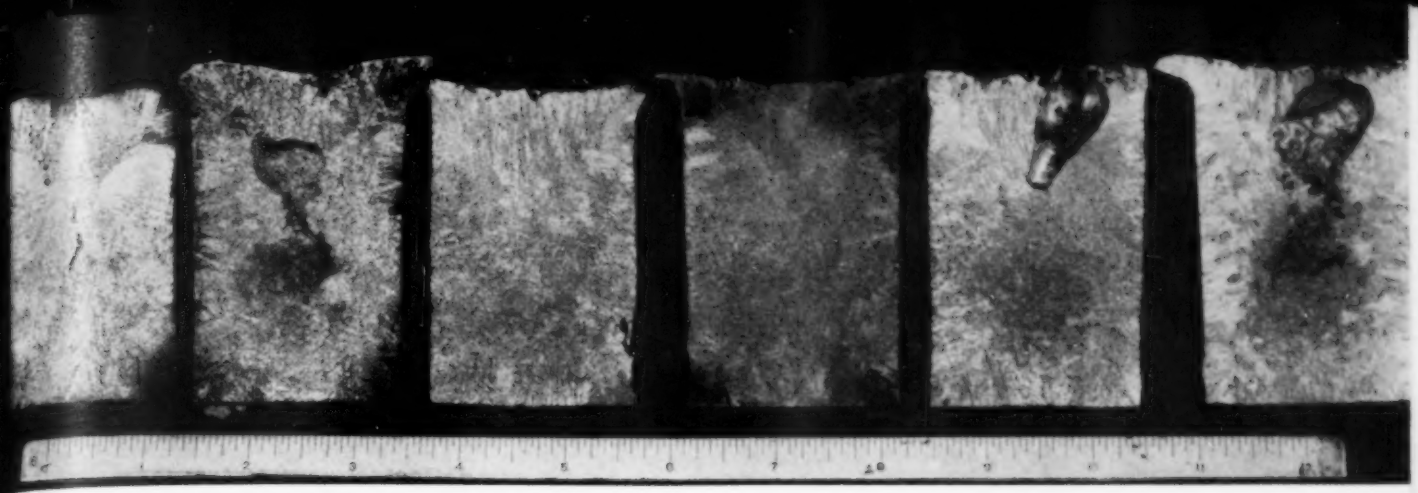
These results support previous observations and also indicate that the keel block test bar is more sensitive in reflecting gas conditions in the metal produced by furnace atmospheres. The microstructures shown in Fig. 13 also show a greater degree of porosity in the non-deoxidized bars melted without an air blast.

Effect of Nitrogen Additions

In test heat No. F2416, a 1½ oz addition of calcium nitride per 100 lb. of metal was made in order to study the effects produced by nitrogen with its subsequent

Fig. 15—Photomicrographs of cast monel specimens. Heat No. F2416. Etch—50% HNO_3 and 50% acetic acid. Mag.—A and B, 100×; C, 500×. A, no deoxidation; and B and C, Ca_3N_2 treated.





As Cast No deoxidation	Fe-Al-Ti 4 oz/100 lb	Mg_3N_2 Total N added (0.045%)	Additional Mg_3N_2 added (Total 0.198%)	Fe-Al-Ti to nitrogen inoculated metal (0.335% Ti)	Additional Fe-Al-Ti added (0.705% Total)

Fig. 17—Effect of titanium on soundness of cast monel inoculated with nitrogen. Heat No. F2475.

treatment with magnesium and ferro-aluminum-titanium. Table 5 shows the physical properties obtained from a monel heat which had been inoculated with calcium nitride. It will be noted that the non-deoxidized as cast regular bar showed satisfactory properties, the Ca_3N_2 inoculated showed inferior properties, magnesium deoxidation of the Ca_3N_2 treated metal showed slightly lower properties while a titanium addition to the Ca_3N_2 treated and magnesium deoxidized metal showed satisfactory properties.

Corresponding plugs sectioned and etched, and cast with the above bars are shown in Fig. 14. It will be noted that as cast, non-deoxidized plug showed some pinhole porosity and no shrinkage. The satisfactory physical properties shown by the corresponding regular bar can be attributed, as shown before, to design or fast cooling rate of the metal which tends to keep absorbed gas in solution upon solidification. The plug inoculated with Ca_3N_2 showed an increase in pinhole porosity, no shrinkage, and bulged considerably at the base. This bulging or swelling can be attributed to the increase in internal gas pressure upon

Fig. 16—Photomicrographs of cast monel specimens. Heat No. F2416. Etch—50% HNO_3 and 50% acetic acid. Mag.—A and C, 100×; B and D, 500×. A and B, Mg deoxidation of Ca_3N_2-treated monel; C, Fe-Al-Ti deoxidation of Mg and Ca_3N_2-treated monel; D, Fe-Al-Ti deoxidation of Mg and Ca_3N_2-treated monel showing TiCN crystals.

Fig. 18—Photomicrographs of cast monel treated with Mg_3N_2 and subsequently deoxidized with Fe-Al-Ti. Etched. Mag.—A, B, D, 100×; C, 500×. A, no deoxidation; B and C, Mg_3N_2-treated monel (0.198% N_2 by weight added); D, Fe-Al-Ti deoxidation of Mg_3N_2-treated monel.

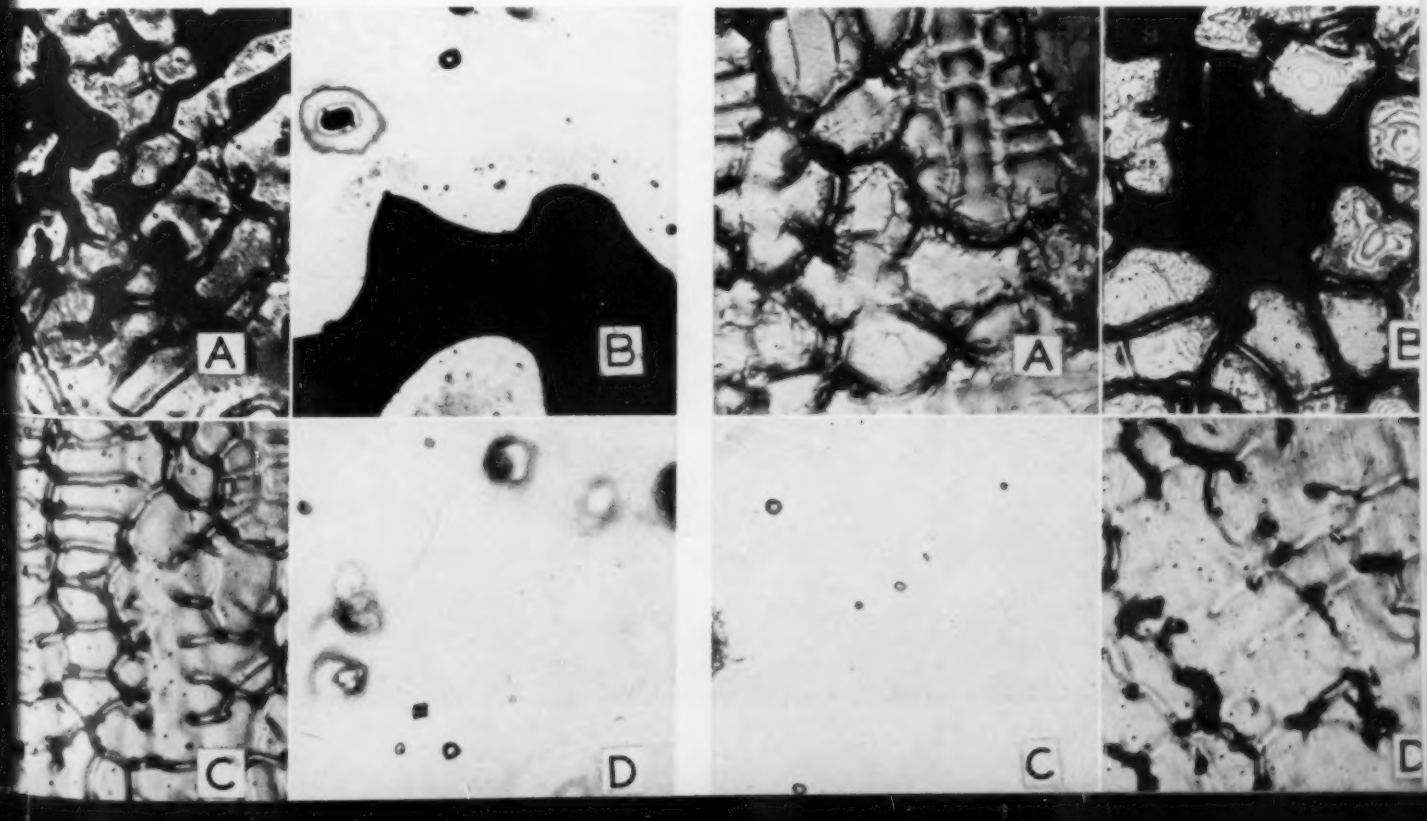


TABLE 4.—EFFECT OF VARIOUS DEOXIDIZERS ON THE PHYSICAL PROPERTIES OF CAST MONEL INOCULATED WITH A NITRIDE. (HEAT NO. F2416)

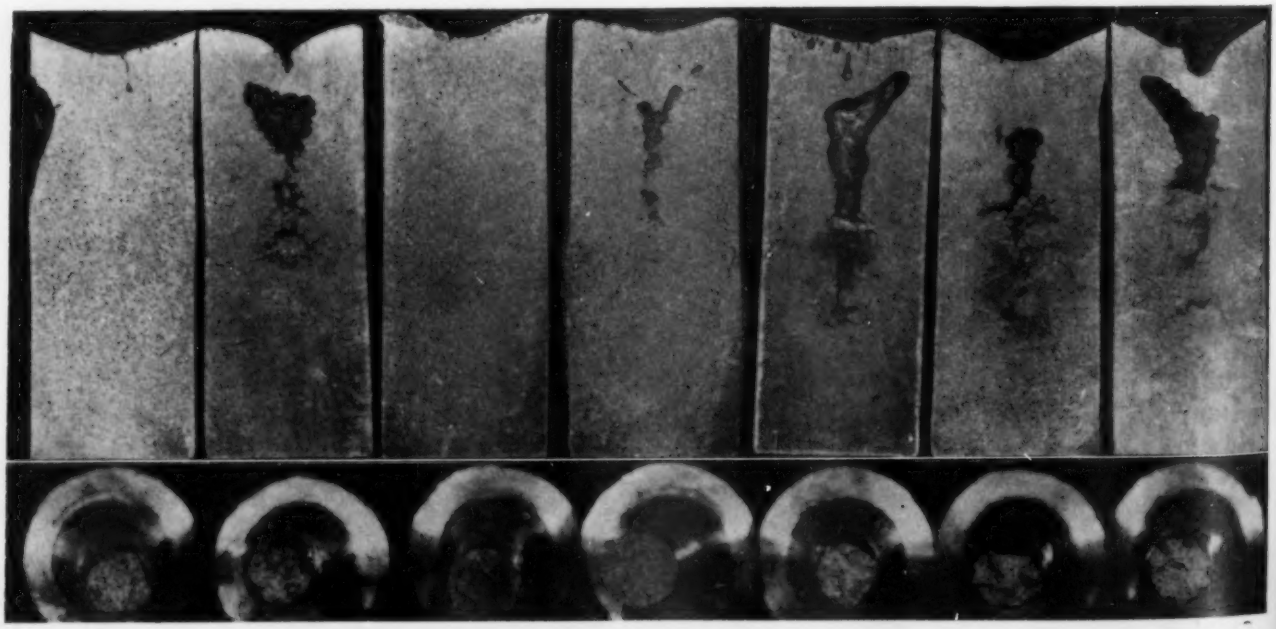
Spec. No.	Treatment	Deoxidizer	Yield Point Psi	Ultimate Psi	Elongation (per cent) in 2 in.	Tapping Temp. °F.	Pouring Temp. °F
2	NONE	AS cast (No Deoxidation)	33,000	66,500	34.5	2950	2730
4	1½ Oz. of Ca_2N_2 per 100 lb of molten metal plunged into ladle	NONE	32,000	55,250	20.0		2680
6		2 oz magesium per 100 lb. molten metal	32,750	51,250	13.5		2640
8		4 Oz Fe-Al-Ti per 100 lb. of molten metal plunged into Mg deoxided metal	37,500	71,500	26.0		2540

TABLE 5.—COMPARISON OF PHYSICAL PROPERTIES BETWEEN KEEL BLOCK TEST BAR & REGULAR BAR.

Heat No.	Furnace	Air Blast	Type of Test Bar	Deoxidizer per 100 lb.	Specimen No.	Yield Point, psi	Ultimate Strength, psi	Elongation per cent in 2 in.
6885	INDIRECT ARC	USED	Regular	NONE	1	35,500	62,625	21.5
			Keel	NONE	2	29,000	37,000	9.0
			Regular	4 Oz—Fe-Al-Ti 1½ Oz—Mg	3	38,750	83,325	42.0
F2297	INDIRECT ARC	NONE	Regular	NONE	1	28,000	35,125	6.0
			Keel	NONE	2	DEFECTIVE BARS		
			Regular	4 Oz—Fe-Al-Ti 1½ Oz—Mg	3	41,250	77,000	26.5
6836	INDIRECT ARC	USED	Regular	NONE	2	37,750	63,750	24.0
			Keel	NONE	3	33,500	48,750	15.0
6837	INDIRECT ARC	NONE	Regular	NONE	2	31,500	50,500	12.0
			Keel	NONE	3	32,750	42,000	13.0
6629	RESISTOR	NONE	Regular	NONE	1	40,750	81,500	29.5
			Keel	NONE	2	33,500	80,750	35.0
			Regular	1½ Oz—Mg	3	40,500	82,000	38.0
			Keel	1½ Oz—Mg	4	42,250	80,375	35.0
			Regular	4 Oz—Fe-Al-Ti 1½ Oz—Mg	5	40,000	82,500	40.5
			Keel	4 Oz—Fe-Al-Ti 1½ Oz—MG	6	38,000	78,125	37.5

Fig. 19—Effect of hydrogen additions on soundness of cast monel and its subsequent treatment with titanium

As Cast No deoxidation	CaH_2 added (.00149%) H_2	CaH_2 added (.0059%) (Total H_2)	CaH_2 added (.0071% (Total H_2)	Fe-Al-Ti (.0933% Ti)	Fe-Al-Ti (.3733% Ti) (Total)	Fe-Al-Ti (.59% Ti) (Total)
PHYSICAL PROPERTIES						
Y.P., psi 41,500	40,250	40,000	35,750	40,500	42,000	43,000
T.S., psi 82,000	82,500	64,000	49,000	85,250	75,000	86,125
El., % in 2 in. 38.5	40.0	19.5	14.0	40.0	34.0	23.0



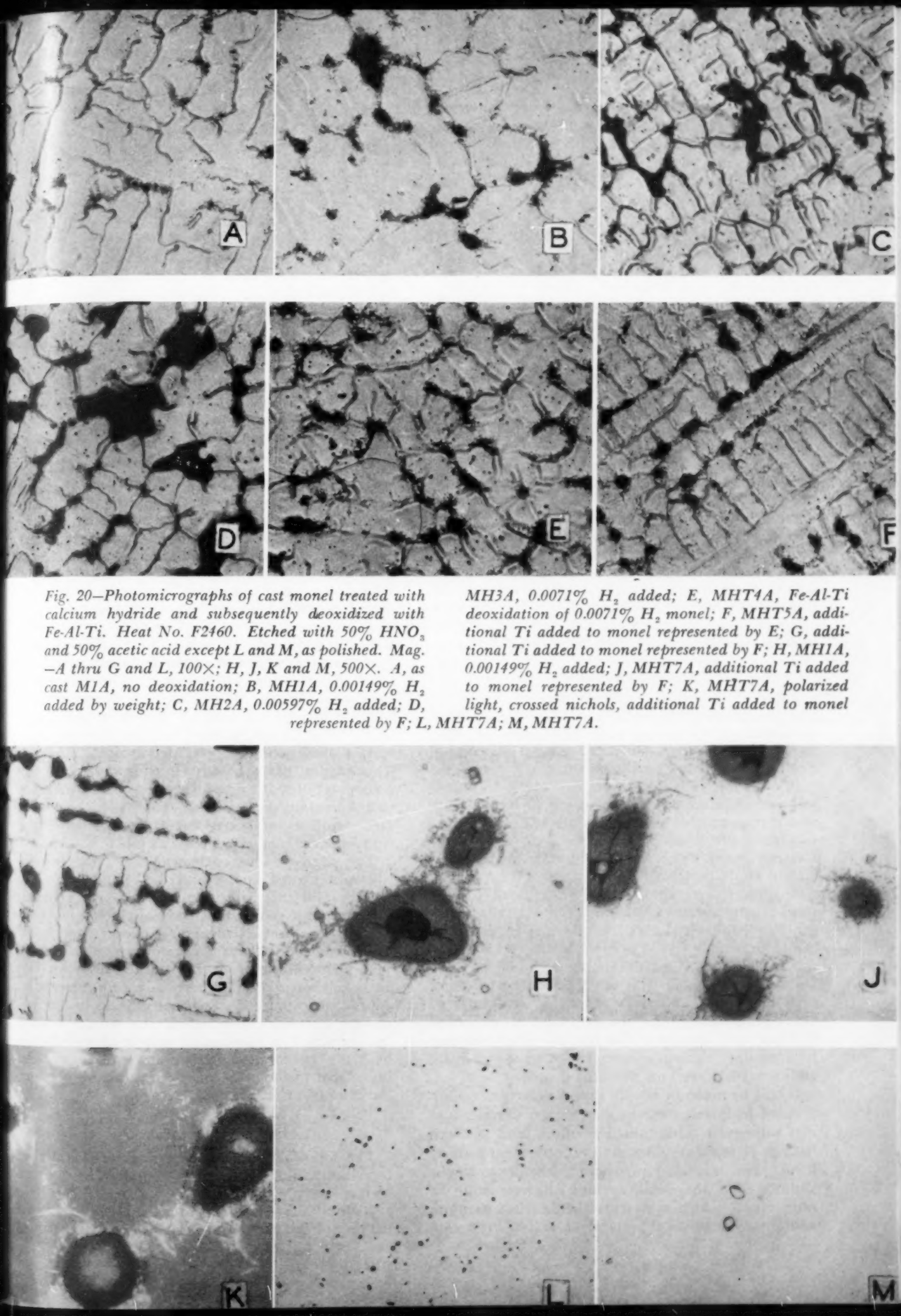


Fig. 20—Photomicrographs of cast monel treated with calcium hydride and subsequently deoxidized with Fe-Al-Ti. Heat No. F2460. Etched with 50% HNO_3 and 50% acetic acid except L and M, as polished. Mag. —A thru G and L, 100×; H, J, K and M, 500×. A, as cast M1A, no deoxidation; B, MH1A, 0.00149% H_2 added by weight; C, MH2A, 0.00597% H_2 added; D, MH3A, 0.0071% H_2 added; E, MHT4A, Fe-Al-Ti deoxidation of 0.0071% H_2 monel; F, MHT5A, additional Ti added to monel represented by E; G, additional Ti added to monel represented by F; H, MH1A, 0.00149% H_2 added; J, MHT7A, additional Ti added to monel represented by F; K, MHT7A, polarized light, crossed nichols, additional Ti added to monel represented by F; L, MHT7A; M, MHT7A.

solidification of the metal.

Magnesium deoxidation of the Ca_3N_2 treated metal showed gross porosity at the top of the plug with a corresponding reduction in the swelling effect. Titanium treatment of the Ca_3N_2 treated and magnesium deoxidized metal exhibited piping in the plug and sound metal. The microstructures shown in Figs. 15 and 16 confirm the results shown in the photomacrographs of the etched plugs and in the physical properties obtained.

Another test heat (F-2475) was run in which magnesium nitride was introduced. Figure 17 shows six sectioned and etched plugs from this heat. The first received no deoxidation; the second was treated with ferro-aluminum-titanium. In the third, approximately 0.095 per cent nitrogen by weight was added to the molten metal in the form of magnesium nitride (26.6 per cent N_2). To the fourth, an additional 0.103 per cent nitrogen was added to the metal remaining after the third plug was cast making a total nitrogen addition of 0.198 per cent. Twenty-four ounces of Fe-Al-Ti/100 lb. were plunged into the remaining metal and the fifth plug was cast. An additional 28 oz of Fe-Al-Ti/100 lb. were added to the remaining metal for the sixth plug.

Small plugs were cast without any corresponding bars in order to treat the smallest volume of metal in an effort to achieve nitrogen bleeding. To this extent, the experiment failed but the general effects noted above for the calcium nitride additions were indicated here. The microstructures shown in Fig. 18 confirm the results obtained on the etched plugs.

Calcium Hydride Added

Effect of Hydrogen Additions in Cast Monel—In heat No. F2460 successsive additions of calcium hydride were made to the molten metal in the furnace. The metal was then treated with increasing amounts of titanium. Plugs and regular test bars were cast after each addition. Photomacrographs of the sectioned and etched plugs, together with the physical results and photographs of the test bar fractures are shown in Fig. 19. The first plug represents the control and was not deoxidized. To the second plug, approximately 0.0014 per cent hydrogen by weight was introduced into the remaining metal in the furnace. The third plug represents a total of 0.0059 per cent hydrogen by weight introduced, while in the fourth a total of approximately 0.0071 per cent hydrogen by weight was introduced. The fifth represents treatment of the "fourth plug metal" with approximately 0.0933 per cent titanium. The sixth represents treatment of "fifth plug metal" with an approximate additional 0.28 per cent titanium. The seventh plug represents treatment of "sixth plug metal" with an approximate additional 0.22 per cent titanium.

It will be noted in Fig. 19 that the increasing additions of hydrogen serve to increase gas porosity and that subsequent additions of titanium tend to eliminate it. Note the swelling, sand fusion and gas porosity in plug No. 4, which represents the heaviest hydrogen addition and the shrinkage and microstructure of plug No. 7 which represents the heaviest titanium addition. The physical properties obtained from regular test bars and an examination of the test bar fractures show this condition perhaps even more forcibly. The heaviest hydrogen addition showed poor physical properties while the first titanium addition yielded unsatisfactory properties.

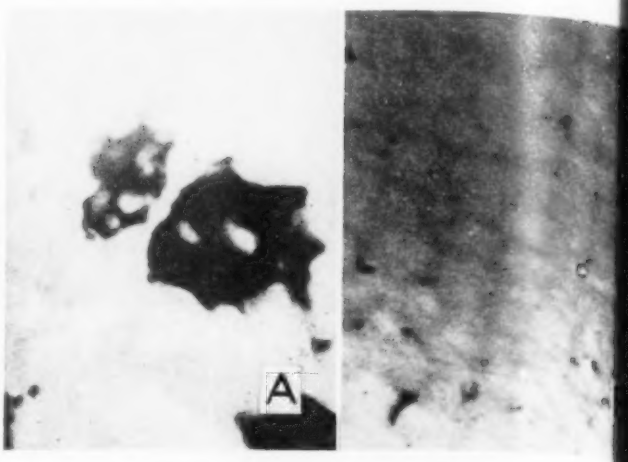


Fig. 21—Microradiographs of cast monel treated with calcium hydride and deoxidized with Fe-Al-Ti. Mag.—100×. Left, MH3A, 0.0071% added by weight; right, MH7A, titanium deoxidation of MH3A metal.

The microstructures shown in Fig. 20 confirm the results obtained from the etched plugs and test bars. The increasing degree of porosity with increasing hydrogen additions and the sound structures obtained by subsequent treatment of the hydrogen inoculated metal with titanium will be noted. With the heavier titanium additions, the tendency for solidification in a dendritic pattern is marked. The black areas shown in A of Fig. 20, for example, are not voids but structure, as evidenced by a comparison of A, B and E. This constituent has been observed in cast monel generally but not to the extent it appears with the heavier titanium additions. It is believed that it is a complex carbon compound but positive identification of this constituent cannot be given at this time.

The degree of porosity evident in the heaviest hydrogen treated sample and the relative soundness after degasification with titanium is shown in Fig. 21.

It has been shown that both nitrogen and hydrogen can detrimentally affect the soundness and quality of cast monel and that titanium can make these effects innocuous. However, it has not been shown that either gas was specifically responsible for the original difficulty encountered in normal melting in the indirect-arc furnace. Nevertheless, due to the extreme care exercised in eliminating sources of hydrogen in both the resistor and indirect-arc furnace, it is believed that in the problem under study, nitrogen was the chief offender.

Conclusions and Summary

The following specific conclusions can be derived from the results of the experimental evidence shown above:

1. Ionization of a furnace atmosphere during the melting of cast monel in the indirect-arc furnace can

materially accelerate the amount of gas absorbed and subsequently rejected during solidification.

2. Magnesium, the utilization of which is normal practice in many commercial foundries melting monel, will not function effectively as a degasser in indirect-arc furnace practice.

3. Titanium, lithium, and zirconium will eliminate nitrogen porosity in cast monel by the formation of stable nitrides.

4. Titanium will suppress hydrogen porosity in cast monel.

5. The keel block test bar is a more sensitive indicator of metal quality than the regular test bar.

Acknowledgment

The authors wish to express their appreciation to the following sections of the Material Laboratory, New York Naval Shipyard: To the staff of the Foundry Control and Development Section for assistance in obtaining the data, the Chemical Section for the chemical analyses and the Radiographic Section for preparing the microradiographs. Thanks is also due Mr. G. C. Johann, Acting Master Molder of the shipyard foundry for his foundry assistance.

Grateful acknowledgment is also made to the Titanium Alloy Mfg. Co. for supplying the magnesium nitride; and to Dr. S. F. Urban, of that organization for his helpful suggestions and criticisms during discussions of various phases of this investigation and to Dr. S. F. Urban, Mr. George Comstock and Mr. R. S. Stewart for their review of this paper. The authors are also greatly indebted to the Navy Department for sponsoring the work and for permission to publish the results.

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DISCUSSION

Chairman: G. P. HALLIWELL, H. Kramer & Co., Chicago

Co-Chairman: B. M. LORING, Naval Research Laboratory, Washington, D. C.

DR. G. H. CLAMER[1]: Why should the keel-block test bar be more sensitive to gas absorption phenomena with monel metal and less sensitive with the 85-5-5-5 alloy?

MR. AMES: The reason why the keel-block test bar is more sensitive in reflecting gas conditions in monel is because the section thicknesses are much greater, and the gas is not held in solution so readily. The other type bar (Fig. 1) which cools at a relatively fast rate, absorbed gas in solid solution. We have had no experience with this type (Fig. 1) of bar on the 85-5-5-5 alloy.

MEMBER: What is the effect of the return charge, i.e., recharging scrap, on physical properties?

MR. AMES: We normally run 85 per cent return scrap. In production of monel metal we run 75 per cent returns without ill effects on the physical properties of the metal.

MEMBER: What would be the limits of sulphurs in the metal?

MR. AMES: We limit monel to 0.03 per cent or less sulphur because sulphur is detrimental to casting properties. A sulphide considerably detrimental to physical properties begins to form when the sulphur content exceeds this limit. We try to keep lead out completely to 0.000 per cent.

DR. LORING: The authors have made an important investigation which should be of great assistance to foundries casting the monel alloys. Evidently, these alloys may be subject to distributed shrinkage like that of the tin bronzes, or to an external shrinkage like that of manganese bronze, depending upon the method of deoxidation. Such phenomena contribute to many of the difficulties in producing castings.

The conclusion that ionization or dissociation of the furnace atmosphere during melting causes increased gas absorption appears to be plausible. It is surprising sometimes how long dissociated gases will stay dissociated. The catalytic effect of the material in the wall of the reaction chamber and the temperature of the furnace are important factors. The importance of these factors could call for different types of gas reactions.

[1] Ajax Metal Co., Philadelphia

NON-DESTRUCTIVE TESTING OF STEEL CASTINGS

Report of the
Steel Division Committee
of A.F.A.

THE DEVELOPMENTS in the field of non-destructive testing during the year April, 1946 to April, 1947 have not been extensive and there have been but few outstanding contributions to the literature.

Perhaps the most interesting development is that a joint Committee has been formed between the various branches of the Army and Navy with the addition of other Governmental bodies for the purpose of reviewing all non-destructive procedures, specifications and standards and the formulation of unified requirements. It is our understanding that this Committee has effected considerable progress and that perhaps during this coming year Industry will be advised of the results of some of their efforts. It is the opinion of your Committee that such an effort is definitely worthwhile and it is our hope that the various industry committees will have an opportunity to review and comment on their findings prior to their adoption by the government bodies.

During February, 1947 the Standards Committee of American Society of Testing Materials approved of the Radiographic Standards for Steel Castings that was adopted by Committee E-7, ASTM. These standards are the same as those prepared and issued by the Bureau of Ships on July 1, 1942. The text material has been changed somewhat to conform to commercial requirements.

New Methods Tried

Supersonic testing has moved forward during the year and it is believed that most of those that are interested in non-destructive testing have seen demonstrations and read authoritative articles on its use. A few steel casting companies have or are using the Sperry Company's reflectoscope, however, as yet reports on the use for steel castings has not been issued.

Your Non-Destructive Testing Committee reviewed its activities again as they did last year. The Committee reaffirmed that a report should be made yearly on the developments in the non-destructive testing field as applied to steel castings. And also while no meeting of the Committee were held during the past year it is believed advisable that the Committee be continued with the thought that they may be able to be effective when the joint government committee makes public its recommendations on non-destructive testing.

In line with the policy established during past years, this report presents a review of only those articles the subject of which is related to the non-destructive testing of steel castings. These articles are listed as an appendix to this report.

COMMITTEE ON NON-DESTRUCTIVE TESTING

C. W. Briggs, *Chairman*	Werner Finster
R. A. Gezelius, *Vice Chairman*	R. H. Frank
Phillip De Huff, Jr.	J .W. Juppenlatz
A. P. Spooner	L. W. Ball

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Articles on the Non-Destructive Testing of Steel Castings

1. C. Frear and R. Lyons, "Nondestructive Inspection of Castings," TRANSACTIONS, American Foundryman Association, vol. 54, pp. 186–199 (1946). A general review.

2. "X-ray Checks Casting Quality," *The Foundry*, June, 1946: pp. 94–95, 262. Description of the installation of 1000 KV X-ray unit in the steel foundry at the Bethlehem plant of the Bethlehem Steel Company.

3. R. A. Gezelius and C. W. Briggs, *Instructions for Radium Radiography*, Radium Chemical Company, New York. 1946–43 pages. A completely revised booklet on radium industrial radiography for castings and welds.

4. F. H. Keating, "Steel Castings," *Iron and Steel*, vol. 19, Nov. 21, 1946, pp. 725–729. Experimental work on their production and radiography for service at high pressure is described.

5. Anon, "Industrial Possibilities of the 20-Million Volt Betatron," *Steel*, Nov. 25, 1946, pp. 68–69, 92. Instrument is a source of very high frequency X-rays, and has deep penetrating power, sharp focus and speed required to inspect heavy sections of steel. Increased initial cost compensated by speed of action.

6. D. H. Wise, "The Utility of Radium in Complex Radiographic Studies," *Industrial Radiography and Non-Destructive Testing*, Fall, 1946, pp. 23–26. A primary advantage of the radium capsule is the ease of handling it because of its small size compared to X-ray machines. Radium eliminates need for complicated set-ups and often can be used without dismantling equipment to be tested.

Articles on Technique Related to the Non-Destructive Testing of Steel Castings

1. T. Piper, "Radiographic and Fluoroscopic Interpretation of Casting Irregularities," *Iron Age*, May 2, 1946: pp. 46–49. Terminology and radiographic appearances of defects as established by the West Coast Aircraft War Production Group.

2. R. Le Grand, "Non-Destructive Testing Methods," *American Machinist*, May 23, 1946; pp. 119–141. An extensive review of all methods of non-destructive testing.

3. L. Cardenas, "Two-Million Volt Industrial Radiography," *Iron Age*, June 13, 1946, pp. 52–55. Describes the first two-million volt industrial plant installation and the materials and structures on which it is applied.

4. H. R. Clauser, "Supersonic Inspection of Materials," *Materials and Methods*, August 1946, pp. 379–384. Steel sections to 28 ft. thick can be inspected for internal defects by supersonic methods. Difficulties are encountered in testing rough or curved surfaces or pieces with non-uniform cross-sections.

5. *The Application of the Betatron*, Printed by Allis-Chalmers Manufacturing Company, Milwaukee 1, Wisconsin. Fall, 1946: 48 pages. A compilation of articles reprinted from technical journals on the application of the Betatron to practical radiography.

6. E. I. Salkovitz, "An Integrating Photometer To Measure Porosity in Castings," *Industrial Radiography*, Winter number, 1946–47. Describes an integrating photometer for measuring the degree of porosity indicated in a radiograph of the casting.

CORRELATION OF STRUCTURE AND PROPERTIES OF 85-5-5-5 ALLOY TEST BARS

By
J. G. Kura* and L. W. Eastwood*

ABSTRACT

Microradiographic techniques were employed in this study on six different test-bar designs. Gas content of melt has no noticeable effect upon size, shape, and distribution of lead particles. In general, for a given quality and test-bar design, the amount of microporosity decreases with decreasing pouring temperature. Microporosity is accentuated by gas evolution and decreases markedly with decreasing temperature. Shrinkage tends to increase with decreasing pouring temperature.

The effect of test-bar design on the amount, size, shape, and distribution of the microporosity and the size, shape, and distribution of the lead is apparently a function of (1) rate of cooling and (2) manner of feeding. The test section of the bar which solidifies most slowly has the most microporosity.

Localized shrinkage is produced by lack of proper feeding. The microradiograph should not be depended upon to reveal shrinkage; whereas, the fracture is a reliable criterion for this defect.

The actual effect of melt quality, test-bar design, and pouring temperature on the mechanical properties obtained will depend upon the net effect of (1) microporosity, (2) lead particle size and distribution, and (3) tendency to form localized shrinkage.

The three main structural features which mainly determine the tensile properties of 85-5-5-5 alloy are:

1. *Quantity and distribution of microporosity.*
2. *Size, shape, and distribution of the lead particles.*
3. *Occurrence of localized shrinkage.*

Introduction

A CONSIDERABLE STUDY HAS BEEN MADE of test bars of 85-5-5-5 alloy for the Non-Ferrous Ingot Metal Institute. This study included an investigation of the relation of melting practice to melt quality and the method of measuring this melt quality by the employment of test bars.[1] As part of this investigation, it has been necessary to accumulate certain basic information to facilitate proper understanding of the data. This led to a correlation of melt quality, test-bar design, and pouring temperature with the fracture, test-bar properties, and structure as revealed by microradiography.

Some effort was expended in an attempt to apply metallographic technique to the study of the structure of 85-5-5-5 alloy. The problem was quite difficult, however, because the structure varies considerably from one part of the cross section to another, making it desirable to reproduce the entire cross section of a test bar. This meant that low magnification of about 15 diameters was desirable. However, at this magnification, many difficulties were encountered with the metallographic technique because of (1) the relief developed on the surface, (2) the tearing out of the lead particles, and (3) the inability to distinguish between porosity and lead because both appeared dark on the print at this magnification.

It was decided, therefore, that the microradiographic technique might offer possibilities, and this was investigated and developed until it was a highly satisfactory means of reproducing the structure of the entire cross section of the test bar. It is not necessary to describe the entire procedure in detail, but the successive steps in the preparation of the microradiographs are as follows:

1. The preparation of representative thin sections of the metal by machining, and then grinding these sections on a surface grinder to about 0.0005 in. in thickness.

2. The exposure of these thin sections to the X-rays from a molybdenum target, employing a fine-grained film.

3. The development of the X-ray film and enlarging the image about 15 times.

The general effects of test-bar designs, melt quality, and pouring temperature on the test-bar properties have been described elsewhere.[1] A good correlation has now been obtained between these three factors and the structure, fracture, and test-bar properties. While several score of radiographs have been made, it is sufficient to illustrate this correlation by the sixteen typical microradiographs, Fig. 1 to 12 and 19 to 22, inclusive.

The interpretation of the microradiographs is simple since the lead is represented on the prints as dark specks, the porosity by white irregular areas, and the solid-solution alpha phase as a half-tone background. In contrast to a metallographic polished section, the microradiographic specimen is of finite thickness, and, therefore, the size and shape of the lead particles and porosity are clearly delineated.

The microradiographic structure of six different

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TABLE 1.—DATA ON TEST BARS ILLUSTRATED BY MICRORADIOGRAPHS, FIGS. 1 TO 12, INCLUSIVE

Figure No.	Magnification	Heat No.	Section Thickness, Inch	Test-Bar Design	Pouring Temp., °F.	Mechanical Properties: Tensile Strength, psi.	Mechanical Properties: Elongation, Per Cent	Fracture* Rating	Melt Quality	Method† of Melting
1	X15	1671F	0.0064	L.T.B.-2	2220	29,600	18.5	2BG	Very low	$F_1L_2C_1(G)$
2	X15	1671I	0.0063	"	2155	34,100	25.1	2AG	"	"
3	X15	1671PL	0.0062	"	2035	31,100	17.5	2AG[1]	"	"
4	X15	A-1715C	0.0045	L.T.B.-2	2240	39,200	43.2	2C(C)	High	$F_1L_2C_1$
5	X15	A-1715I	0.0064	"	2140	39,300	44.6	2C(C)	"	"
6	X15	A-1715OL	0.0058	"	2035	37,000	27.9	2BG(S)	"	"
7	X15	A-1715BL	0.0059	Hor. 5/8 in. web-Webbert	2245	36,600	36.8	2CG	High	$F_1L_2C_1$
8	X15	A-1715L	0.0043	"	2105	38,100	41.0	2CG	"	"
9	X15	A-1715NL	0.0066	"	2040	38,800	38.4	2CG	"	"
10	X15	A-1715EL	0.0060	Mod. 5/8 in. web-Webbert	2230	38,300	37.6	2CG	High	$F_1L_2C_1$
11	X15	A-1715H	0.0050	"	2145	36,900	39.6	2CG	"	"
12	X15	A-1715P	0.0057	"	2030	39,100	33.0	2CG	"	"

* Fracture ratings as follows:
2AG—Large amount of coarse coppery areas, generally distributed, indicating heavy microporosity.
2BG—Some coppery areas, generally distributed, indicating some microporosity.
2CG—Small amount of fine coppery areas, generally distributed, indicating a small amount of microporosity.
2C(C)—Small amount of fine coppery areas, not generally distributed, indicating a small amount of microporosity.
2BG(S)—Some coppery areas, generally distributed, indicating some microporosity with localized shrinkage.
[1]—Some localized shrinkage may have been present.
† F_1—High-frequency induction furnace.
L_2—Clay-graphite lining.
C_1—Method of charging consisted of forming a molten heel, reducing the power, and adding 1 ingot or piece of scrap at a time until the entire charge was completely melted.
(G)—Glass charged with the metal to maintain a glass-slag cover at all times.

test-bar designs was investigated, but only three of the designs, the Modified 5/8-in. web-Webbert, the Horizontal 5/8-in. web-Webbert, and the L.T.B.-2, will be illustrated in some detail here because they represent moderately slow, intermediate, and rapid solidification rates of the test section. Sketches of these three test-bar designs are shown by Fig. 13. Complete drawings of these designs have been shown elsewhere.[1]

Data pertaining to each of the first twelve microradiographs are listed in Table 1. The microradiographs are listed in four groups of three each. The first group of three shows the effect of pouring temperature on the structure of the L.T.B.-2 test bar poured from a melt of very poor quality; namely, high gas content.[1] The second group shows a similar series representing the same test-bar design poured from a melt of high quality, or low gas content. This and the following two groups represent nine test bars poured from the same high-quality melt, and the nine microradiographs taken together show the effects of test-bar design and a comparison of the effects of pouring temperature on the structure of test bars of the three different designs.

Effect of Melt Quality

A comparison of Figs. 1, 2, and 3 with Figs. 4, 5, and 6, respectively, clearly shows the effect of melt quality, since the only variable represented by the two groups of figures is the difference in the gas content of the melts from which the two sets of bars were poured. It will be noted that the amount of microporosity is much greater in the bars poured from a gassy melt, as represented by Figs. 1, 2, and 3. This accentuation of microporosity with increasing gas content of the melt is important because the lower tensile properties accompanying the larger amount of microporosity make it possible to use test bars as a measure of melt quality or gas content. Without this effect, test bars as a measure of melt quality in the foundry would be practically useless.

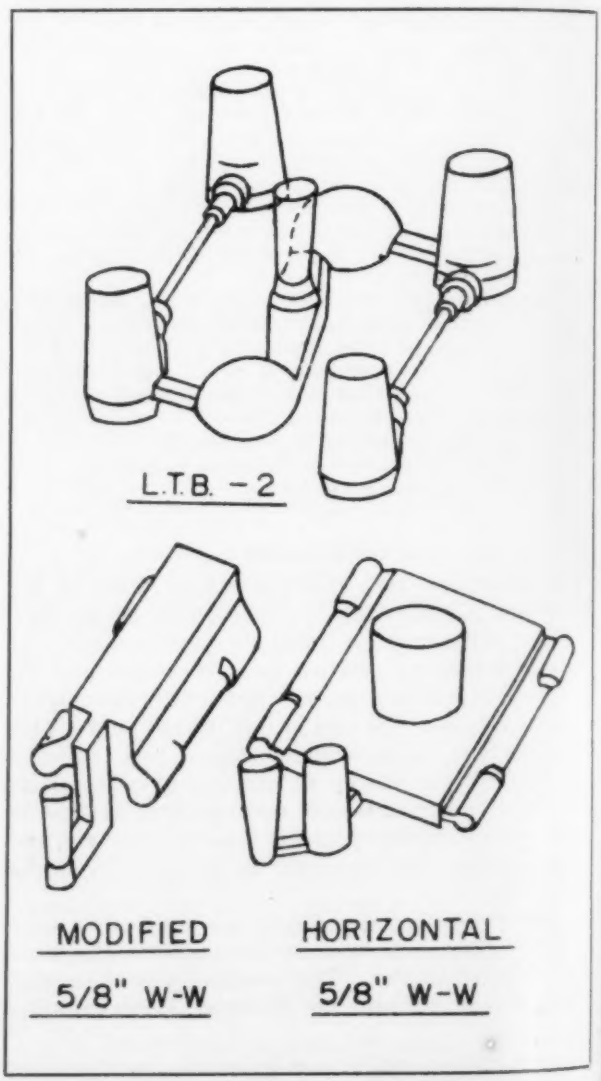


Fig. 13—Sketches of three test-bar designs studied.

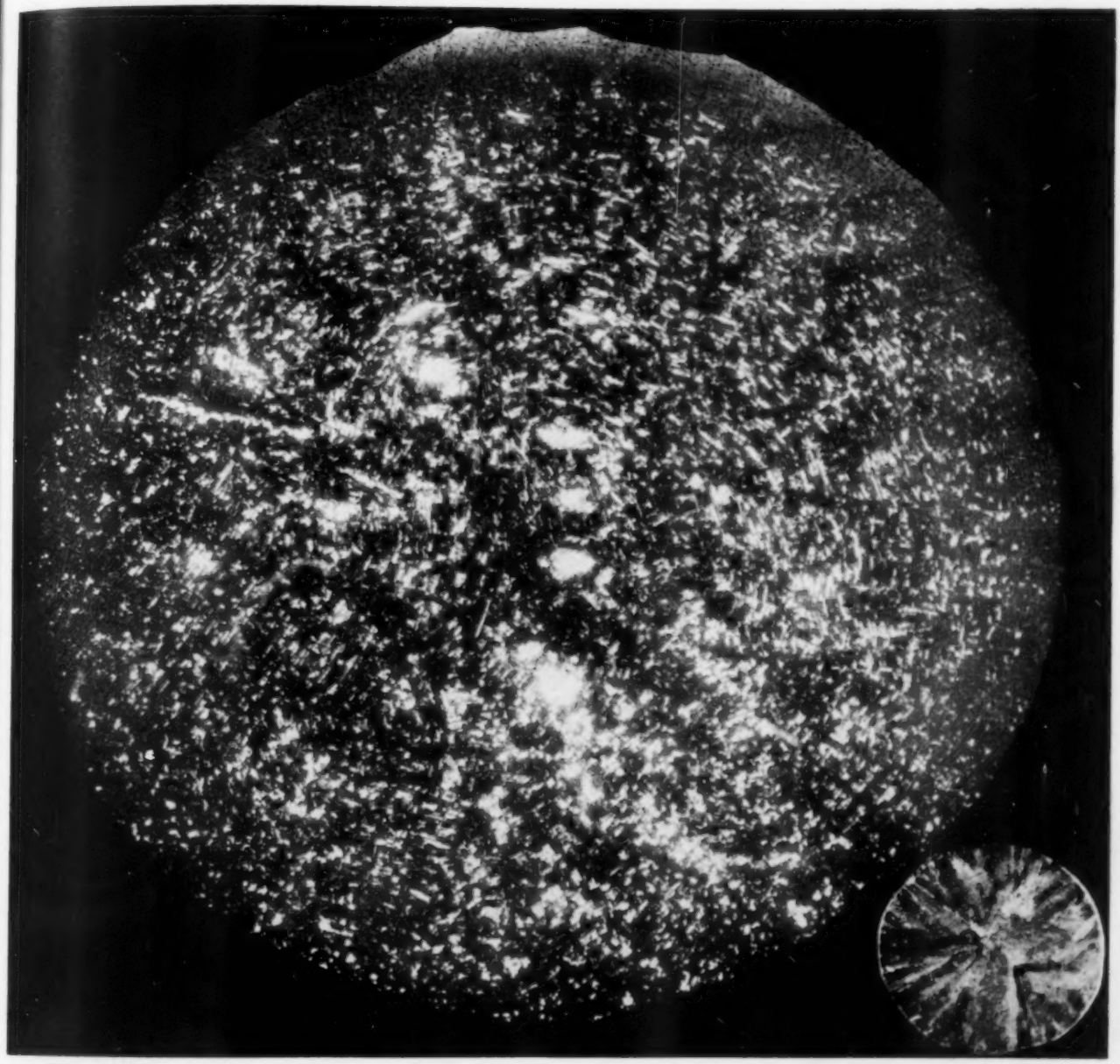


Fig. 1

In Figs. 1, 2, and 3 (See Table 1 for pertinent data) a large volume of microporosity (light areas) occurs in all of these bars because of the high gas content of the melt. This is clearly illustrated by the comparison with Figs. 4, 5, and 6, representing an exactly similar series poured from a melt of lower gas content. As compared with the bar shown by Fig. 1, the lower pouring temperature employed for the bar illustrated by Fig. 2 has produced finer microporosity. As a result, the elongation value is higher. Because of the still lower pouring temperature, Fig. 3 shows less but coarser porosity and, in addition, the lead particles (dark specks) are more angular. Consequently, the elongation value is lower than that of the bar represented by Fig. 2.

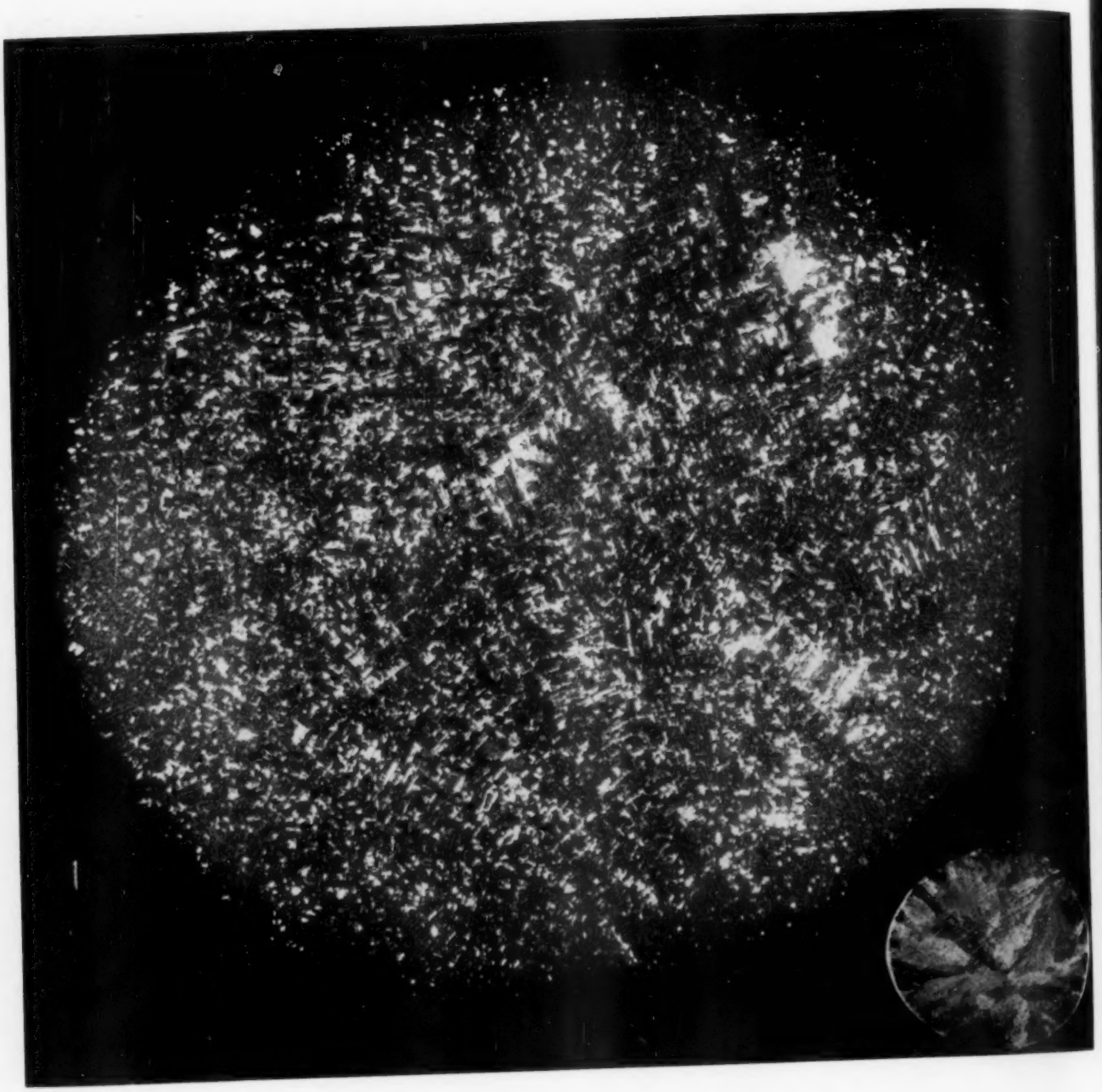


Fig. 2

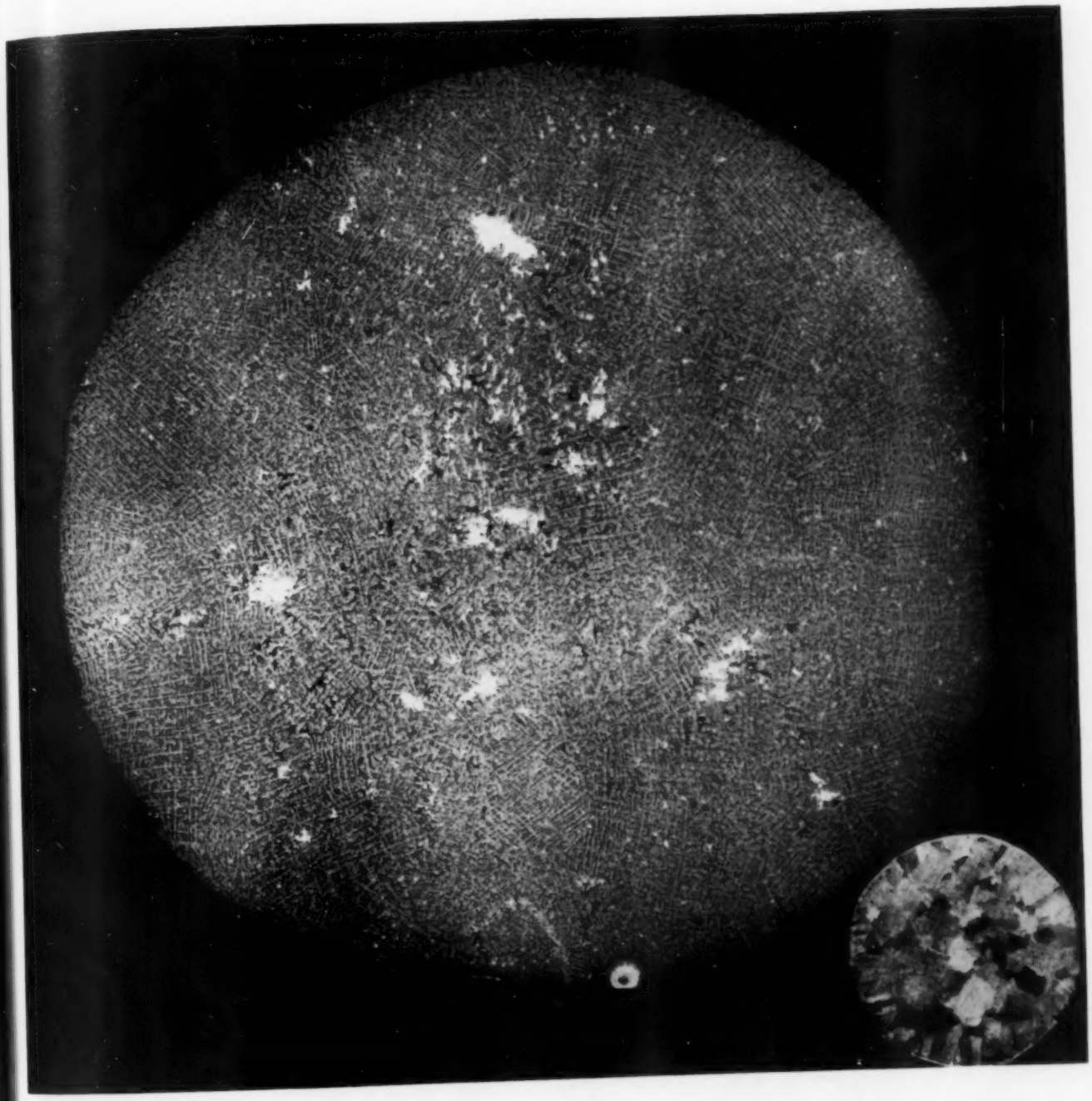


Fig. 3

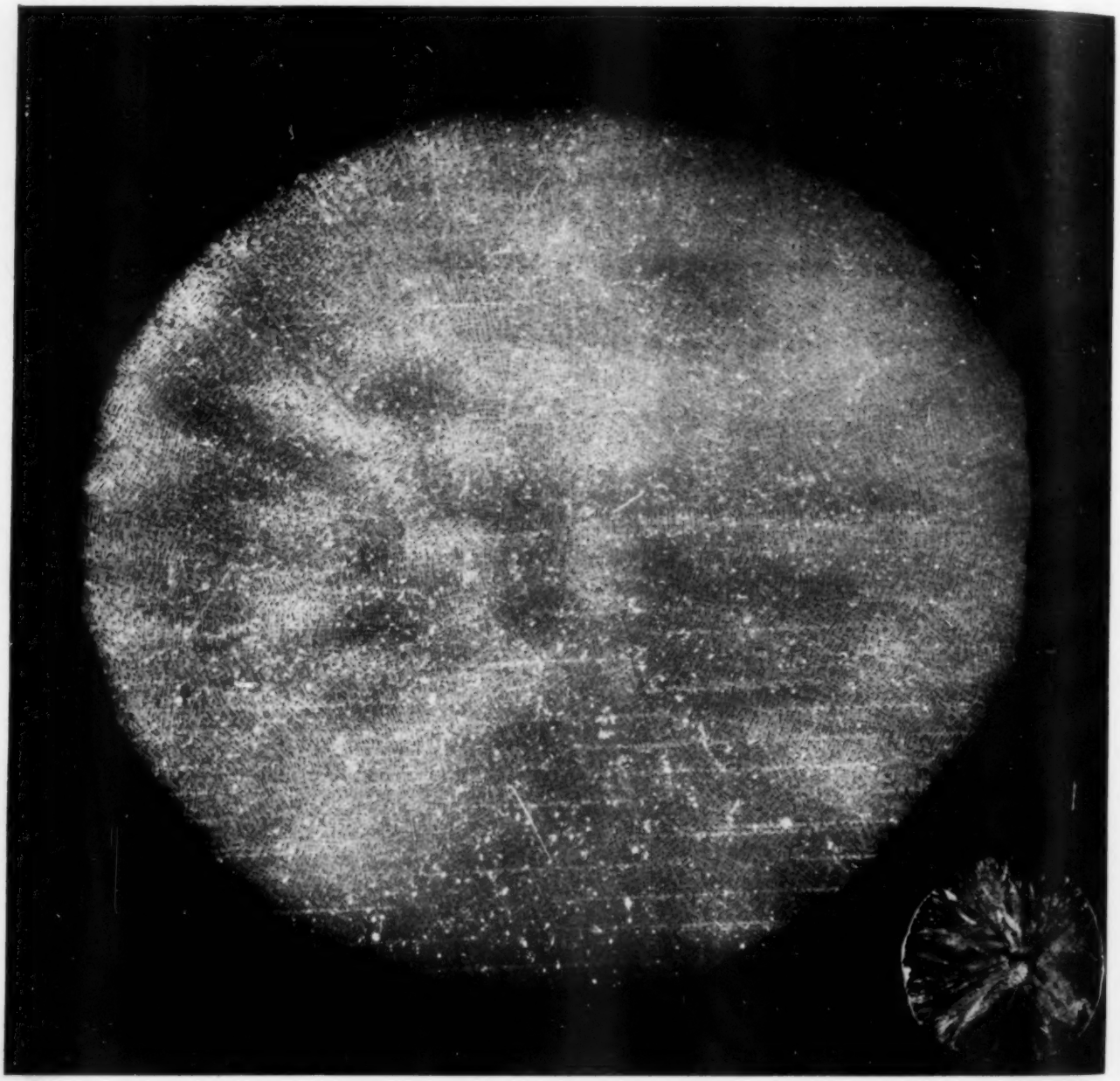


Fig. 4

Figs. 4, 5, and 6 (See Table 1 for pertinent data) as compared with Figs. 1, 2, and 3, show a much smaller volume of microporosity (light areas) because of the lower gas content of the melt. The effects of pouring temperature on lead distribution (dark specks) are about the same as those observed for Figs. 1, 2, and 3. The coarse, generally distributed porosity noted on the fracture of the bar represented by Fig. 6 is also observed as light areas on the microradiograph. This coarse porosity contributes to the low elongation value. Experience has indicated that coarse porosity is especially associated with this cast-to-shape design poured at a low temperature. This defect probably is shrinkage, sometimes quite localized, and it is a result of inadequate feeding which accompanies the low pouring temperature.

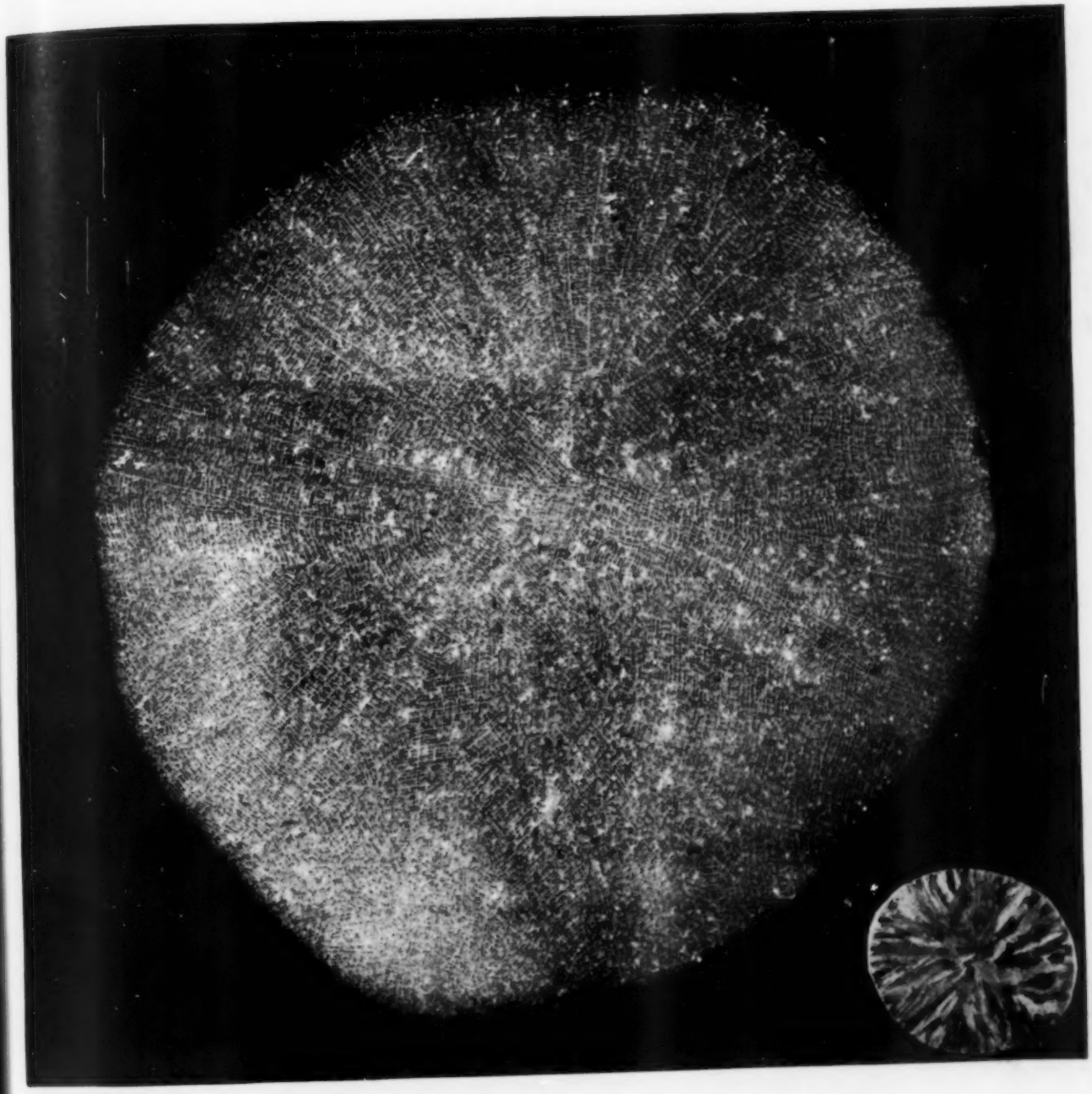


Fig. 5

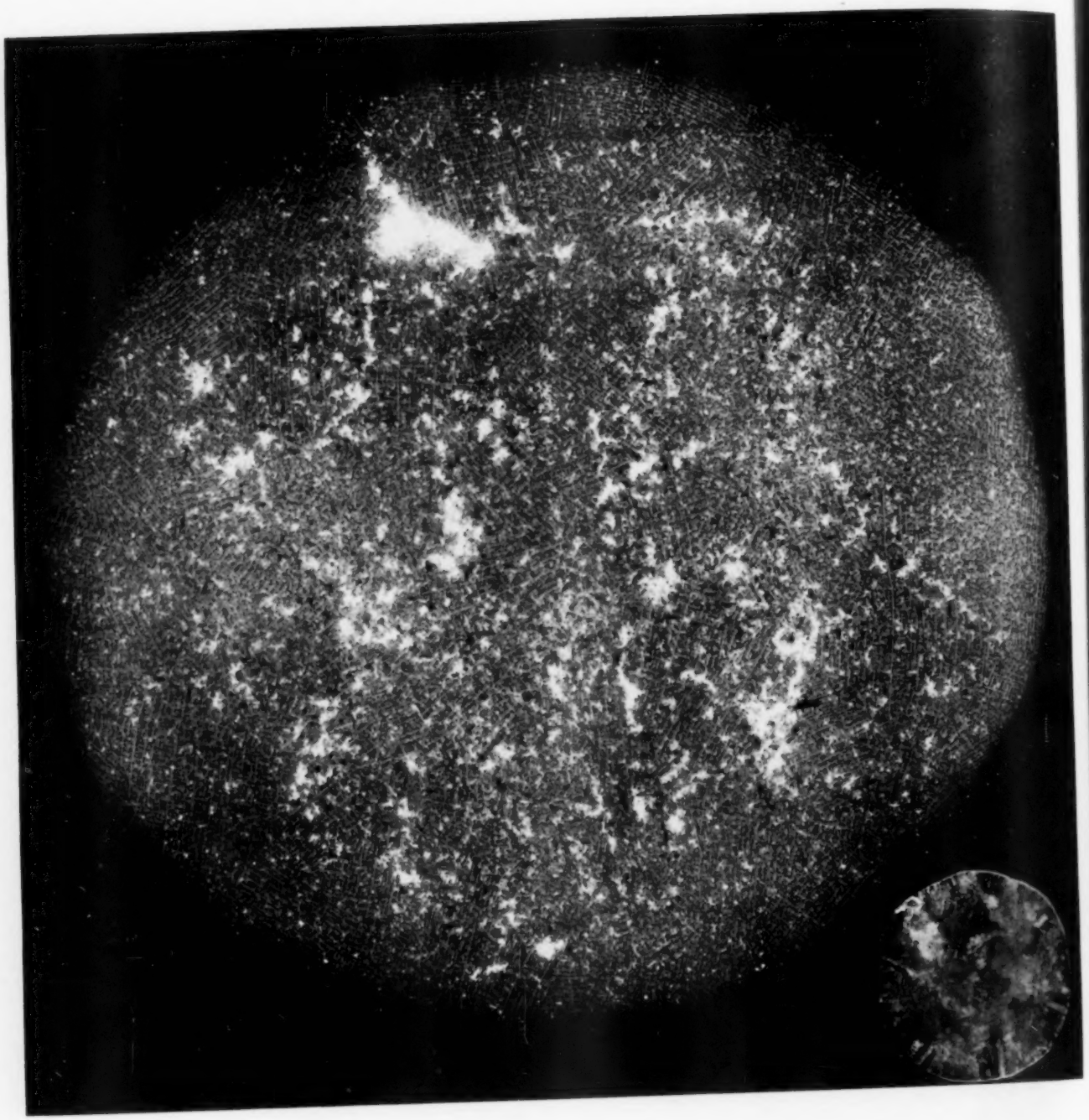


Fig. 6

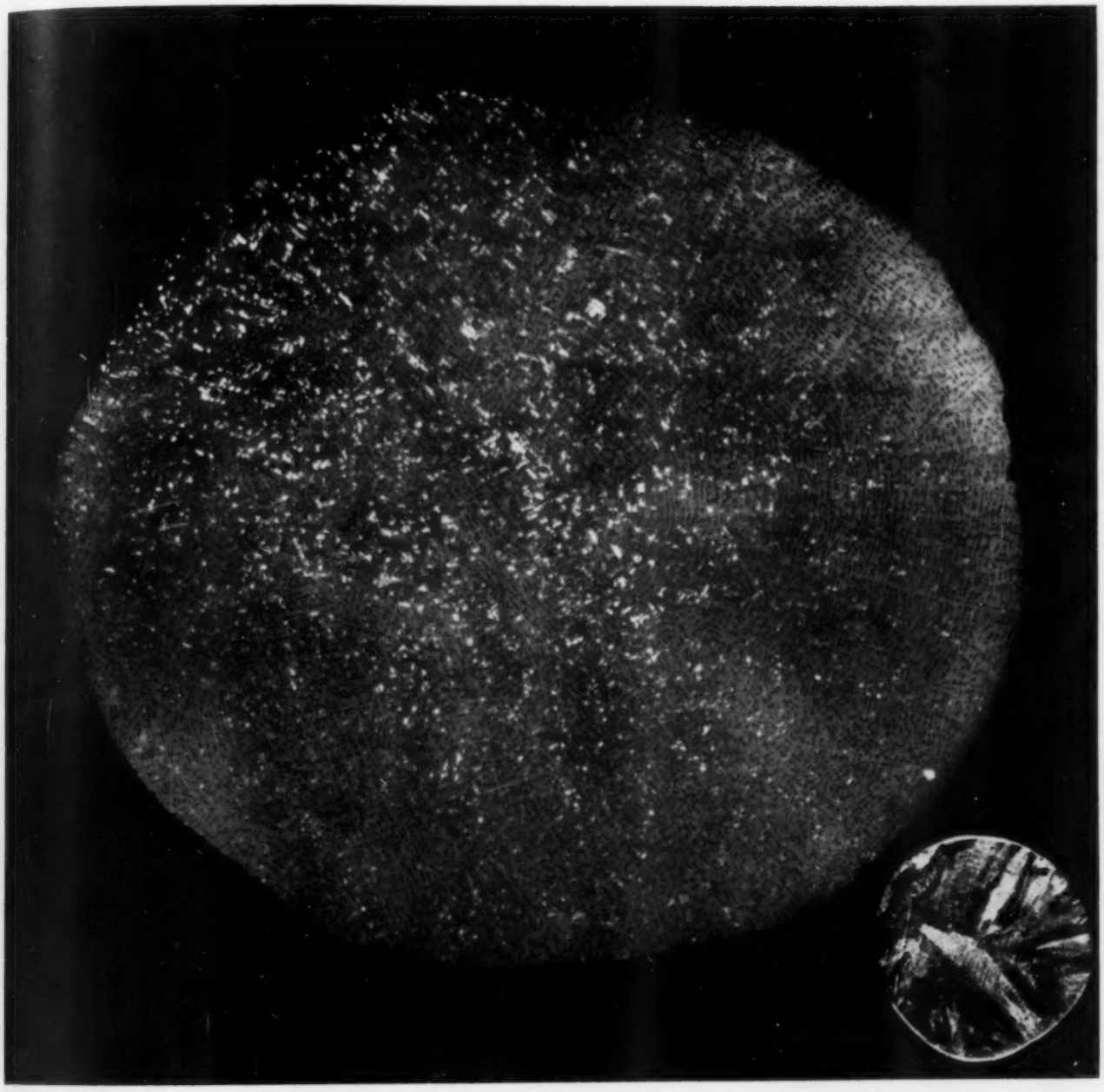


Fig. 7

In Figs. 7, 8, and 9 (See Table 1 for pertinent data) because of the slower rate of solidification in the test section of this test-bar design, the amount of microporosity (light areas) is greater than that shown by Figs. 4 and 5, but the lead particle size is somewhat more favorable because it is slightly coarser at the two highest pouring temperatures. The net result of these two effects, one favorable, the other unfavorable, is slightly lower tensile properties at the two highest pouring temperatures. The angular lead (dark specks) again occurs at the lowest pouring temperature as shown by Fig. 9. The elongation value of the bar represented by Fig. 9 is, however, higher than that of the bar illustrated by Fig. 6, largely because of the absence of localized shrinkage as indicated by the fracture rating in Table 1.

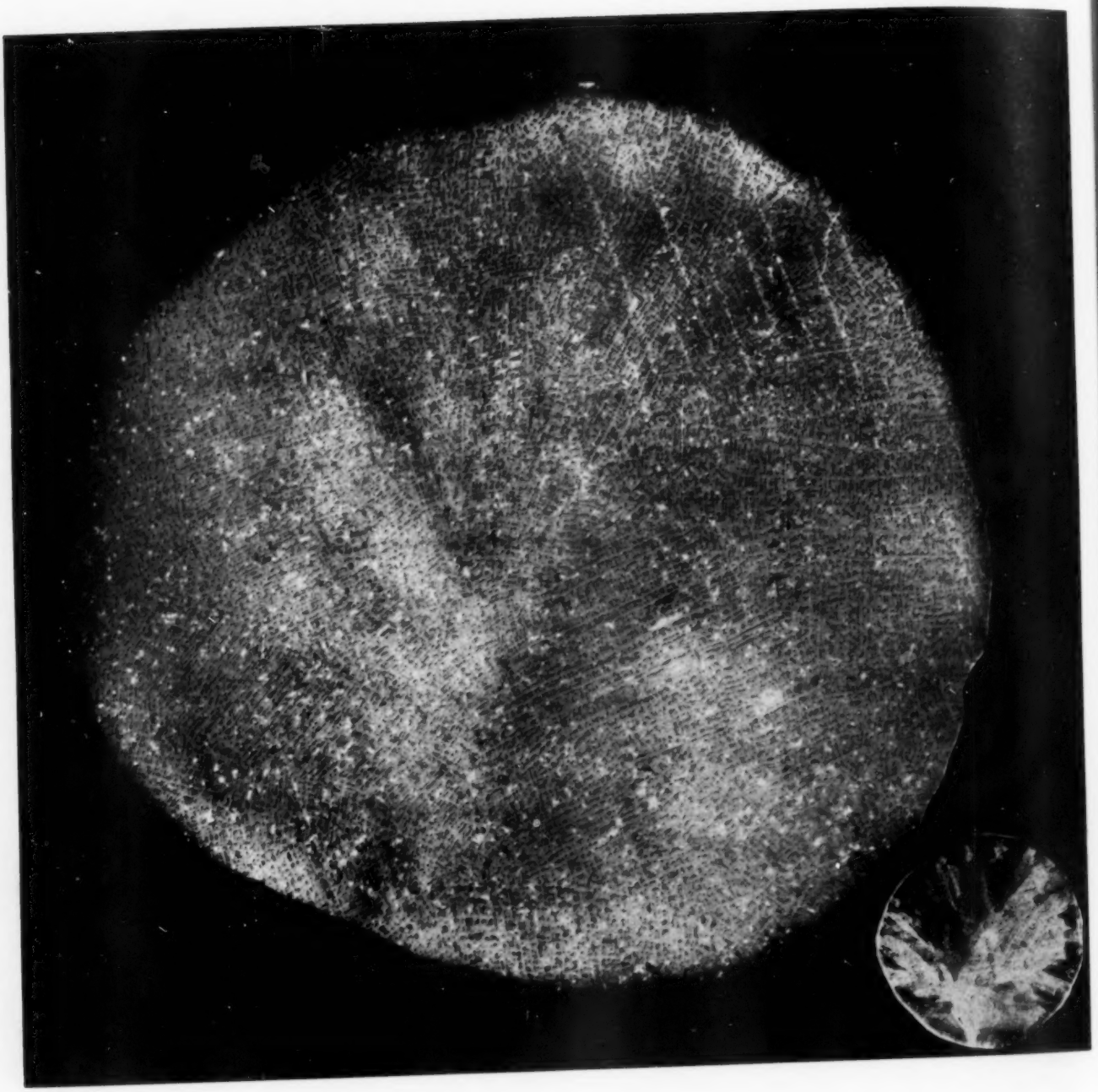


Fig. 8

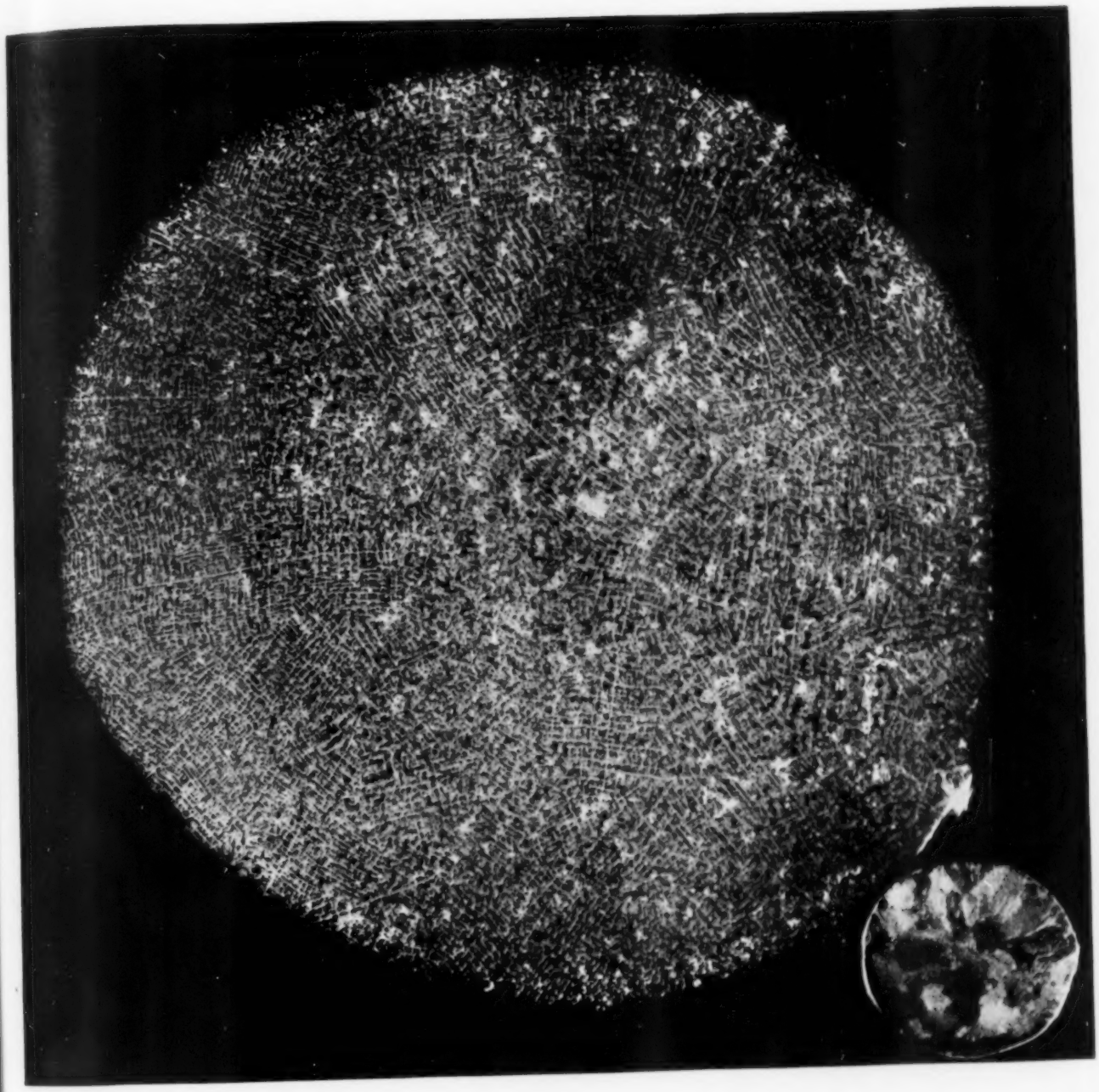


Fig. 9

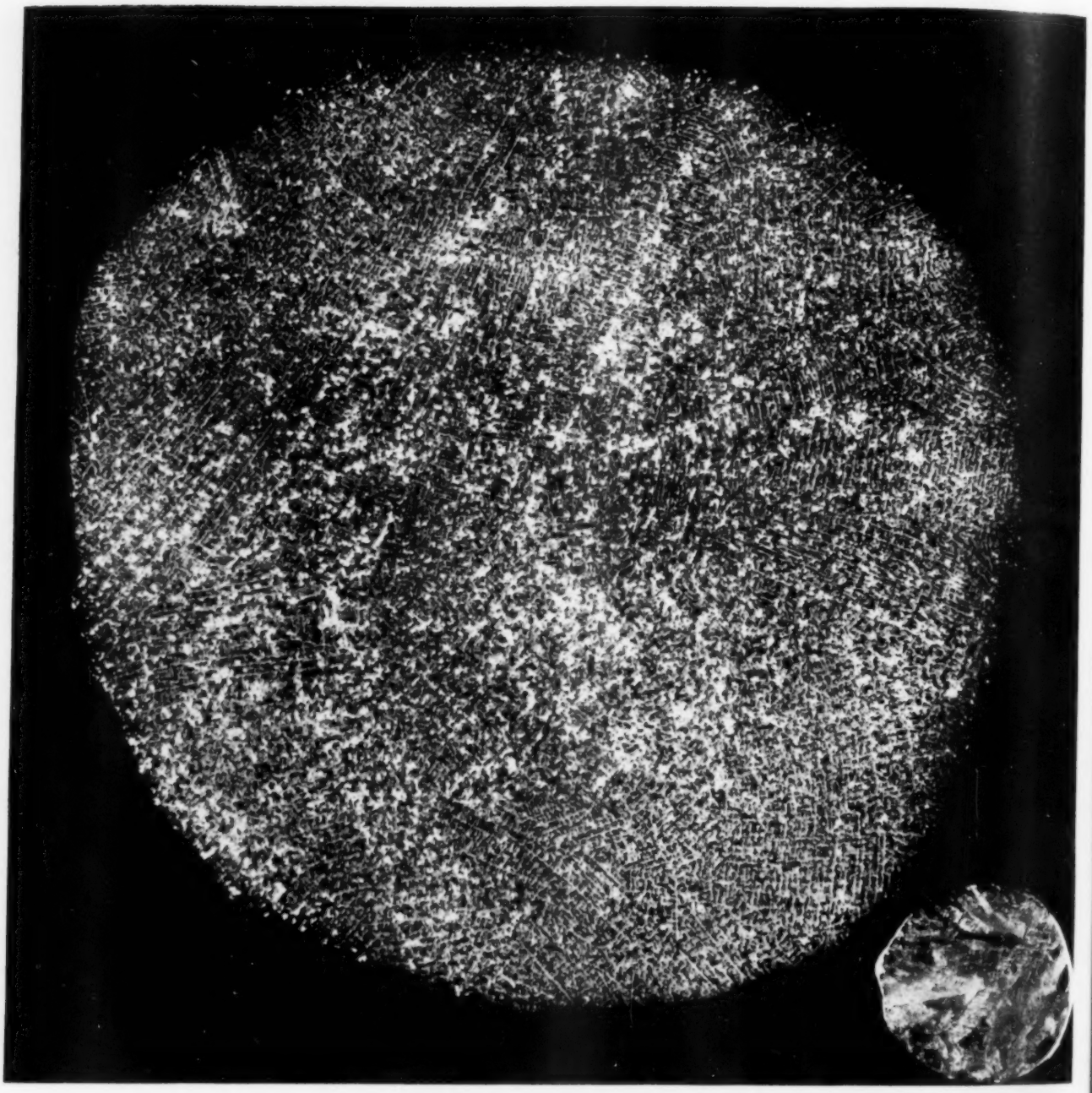


Fig. 10

In Figs. 10, 11, and 12 (See Table 1 for pertinent data) the test section of this bar design solidifies even more slowly than the test section represented by the preceding series of three microradiographs. As a result, the lead (dark specks) is coarser at the high and medium pouring temperature and is, therefore, more favorable. Because of the slower solidification rate, the amount of microporosity (light areas), however, is accentuated throughout the entire pouring-temperature range. Representing the lowest pouring temperature, Fig. 12 shows more angular lead particles and more microporosity than is shown by Fig. 9. The net result is a lower elongation value.

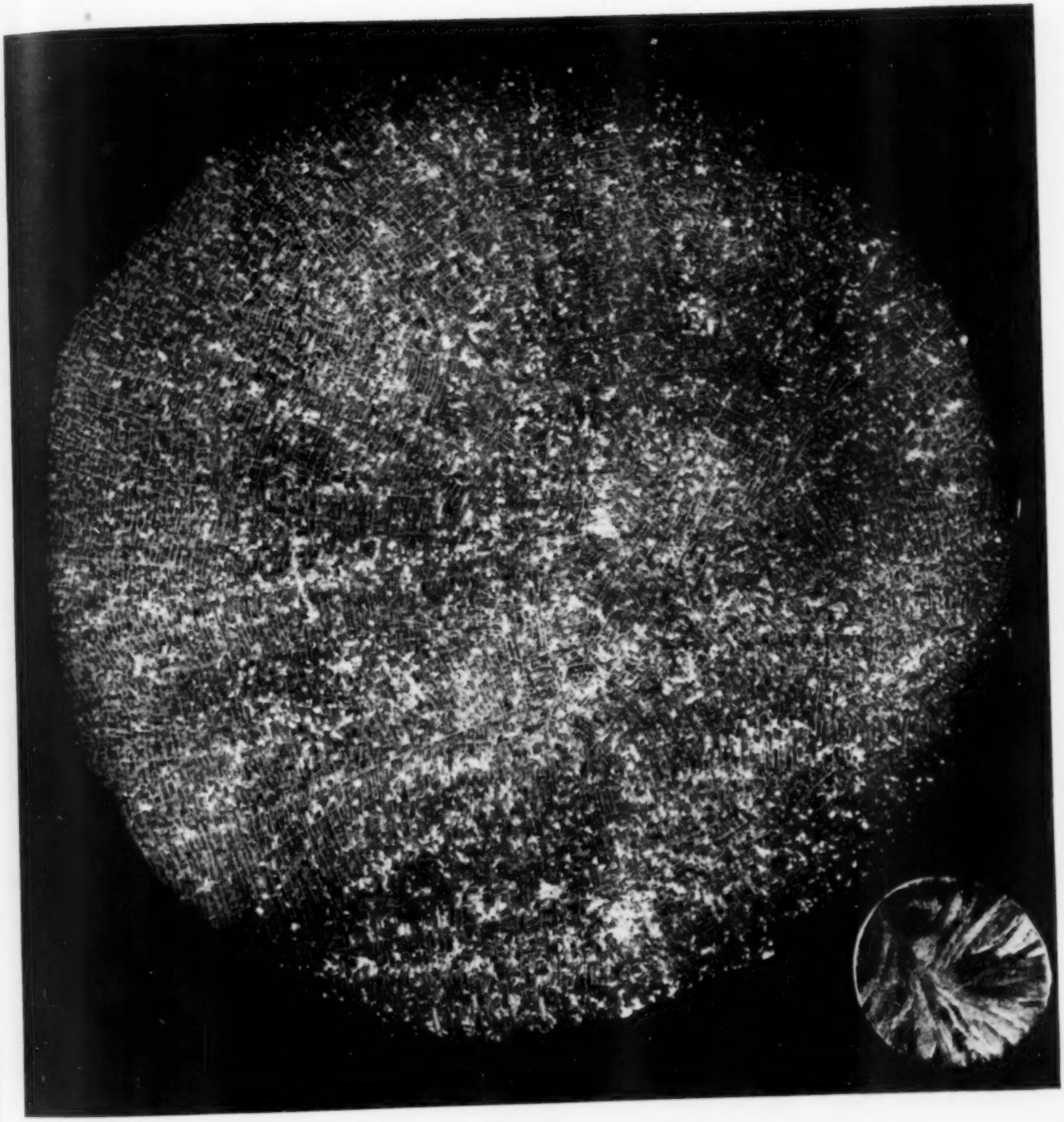


Fig. 11

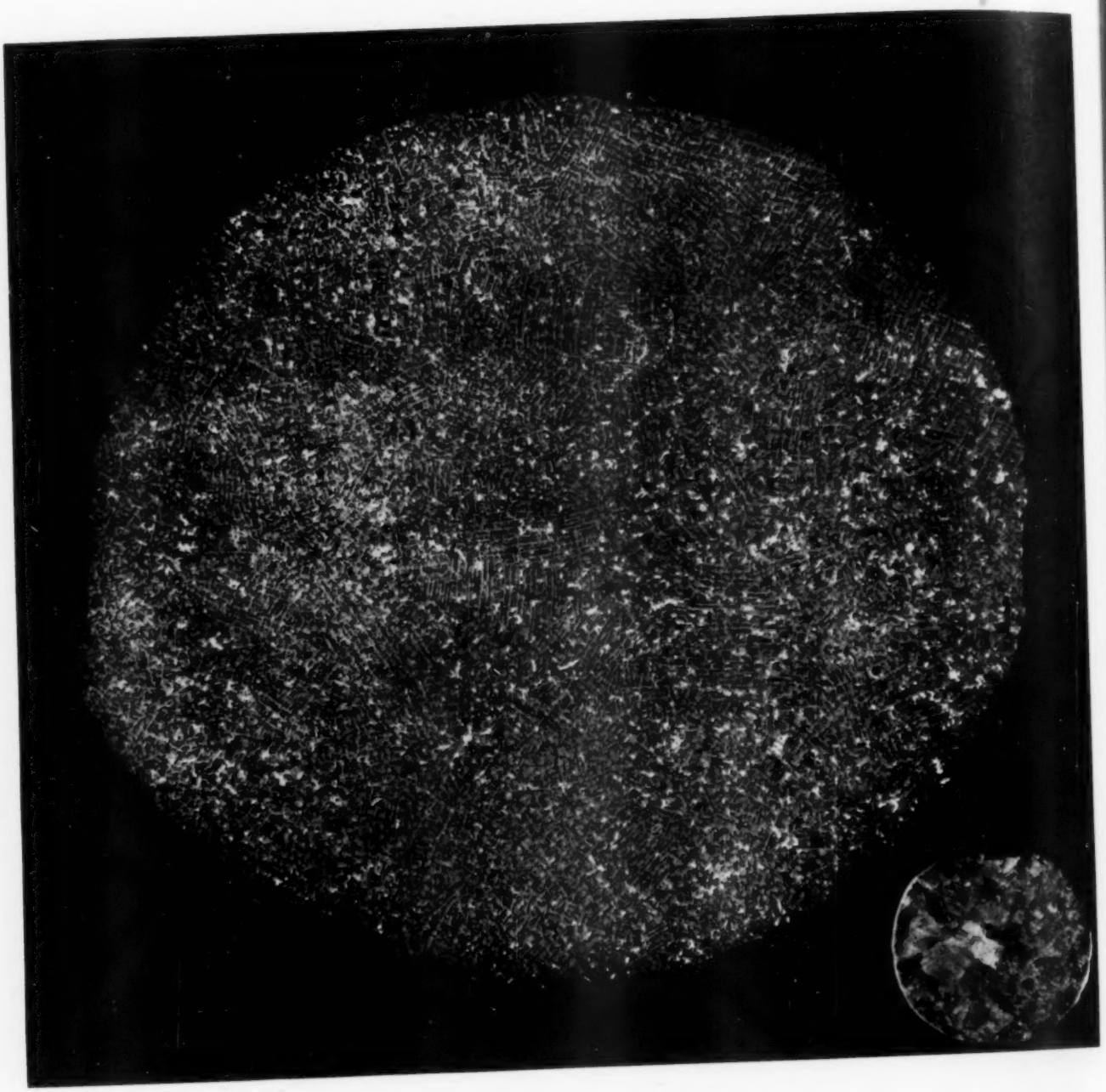


Fig. 12

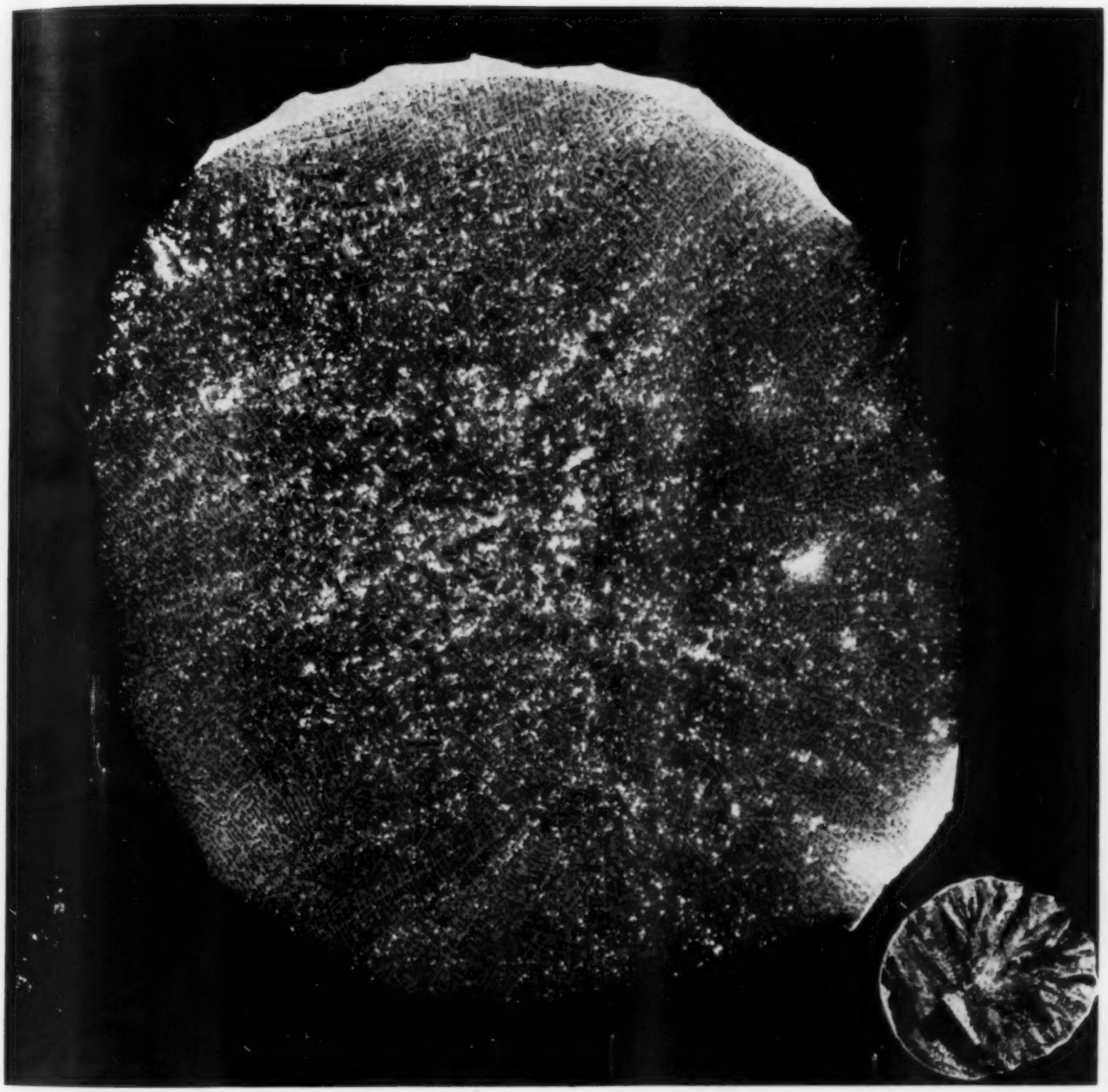


Fig. 19

A microradiograph of 85-5-5-5 alloy at X15 showing a Horizontal 5/8-in. web-Webbert bar, A-767A, poured from 2157° F. producing 37,200 psi. and 44.3 per cent elongation with a fine gray fracture. The relatively high degree of soundness is a direct result of the high melt quality. Dark specks are lead particles, light areas are microporosity. Compare with Fig. 20.

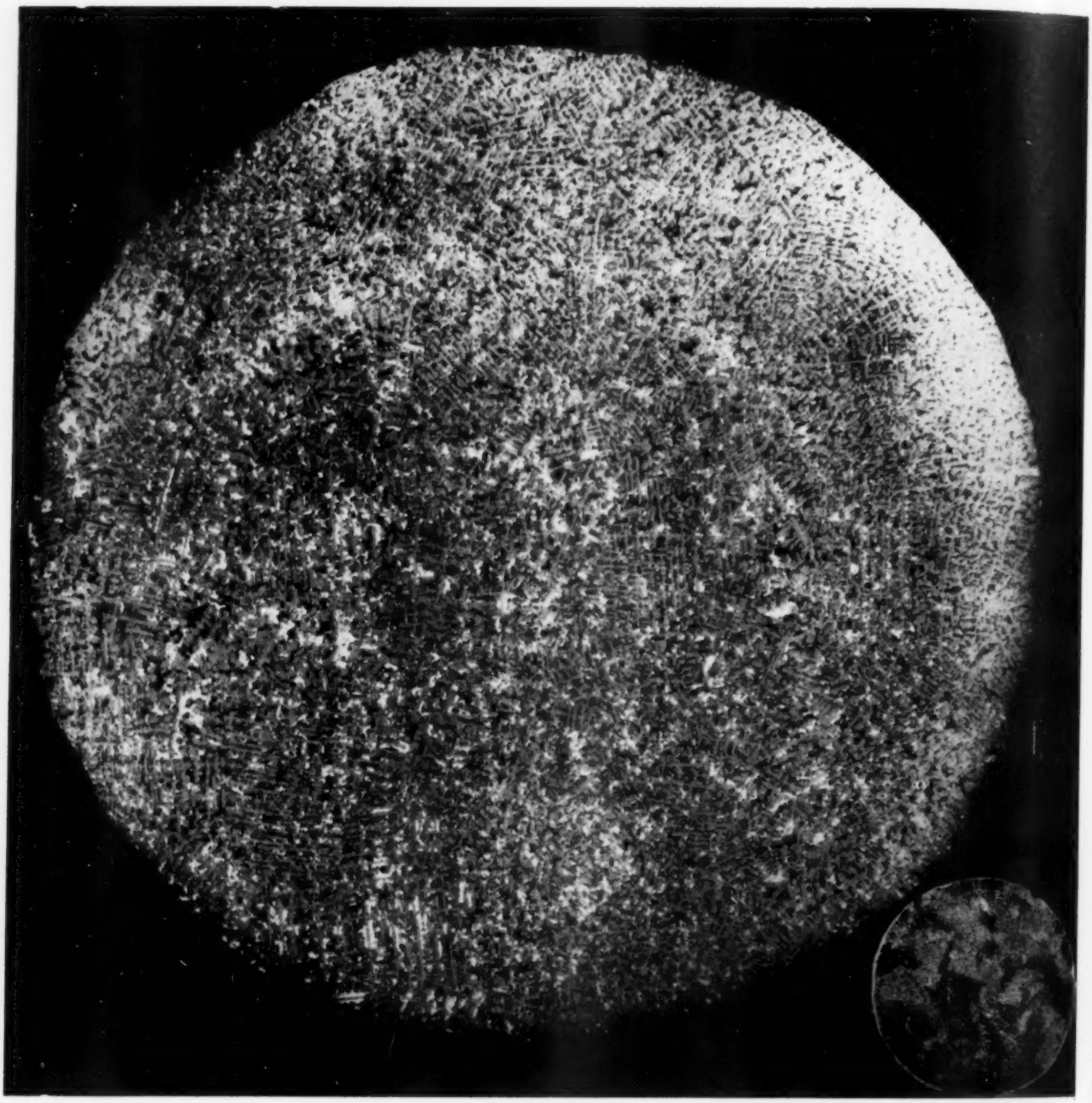


Fig. 20

A microradiograph of 85-5-5-5 alloy at X15. Same as Fig. 19, but the bar, A-767B, is from a Navy keel block poured at 2170° F., producing 27,100 psi. and 14.3 per cent elongation with a coarse, open fracture. At a pouring temperature of 2065° F., the tensile properties were only 32,700 psi. and 21.8 per cent elongation. This microradiograph clearly shows the coarse, angular lead (dark specks) and coarse microporosity (light areas) causing the low properties, despite the high quality of the melt in the pot. Relative large changes in gas content would be required before a noticeable change in tensile properties would occur. Compare with Fig. 19.

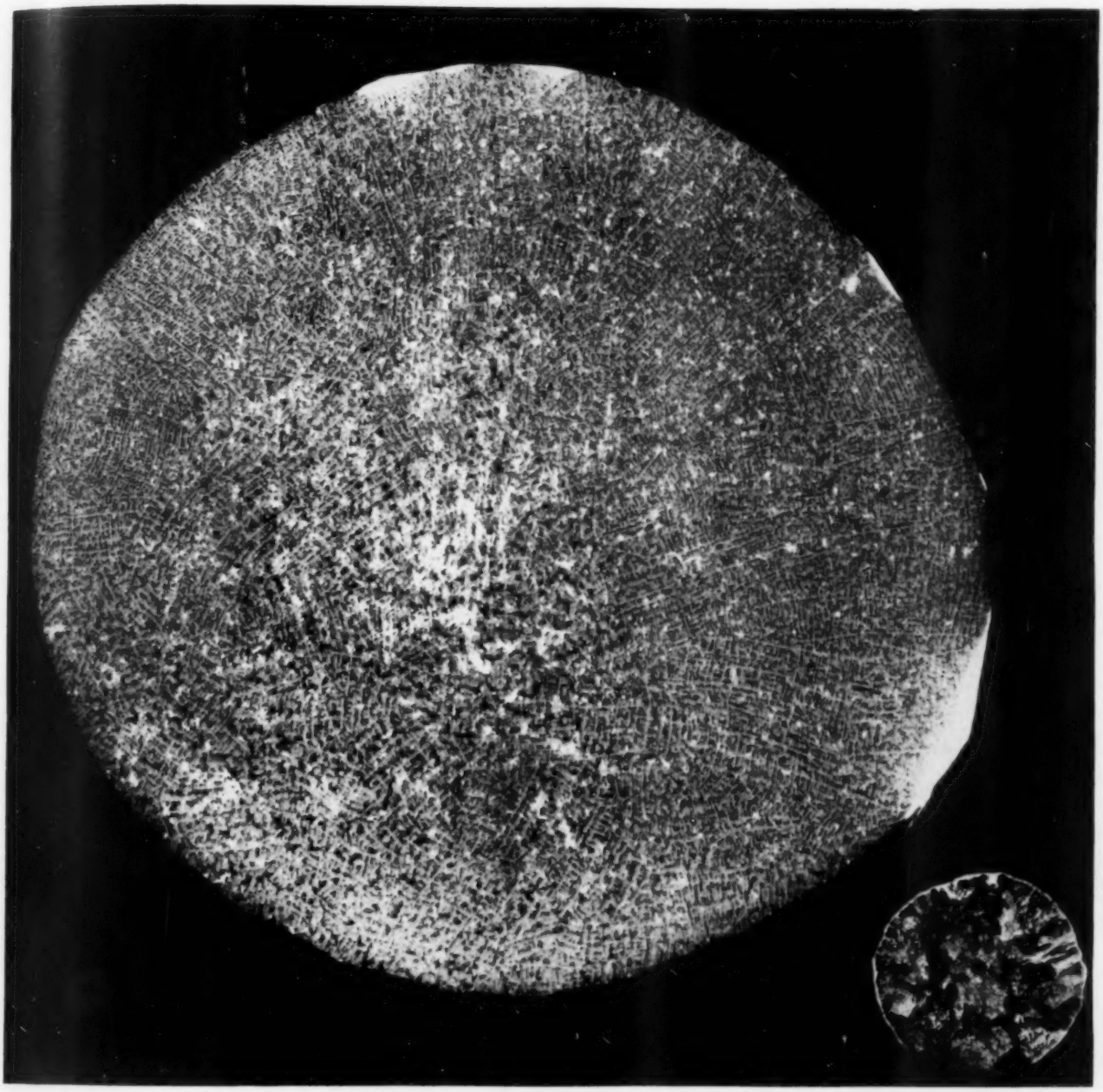


Fig. 21

A microradiograph of 85-5-5-5 alloy at X15, showing a Horizontal 5/8-in. web-Webbert bar, A-871K poured from 2100° F., producing 38,000 psi. and 38 per cent elongation with a fine gray fracture. A similar result was also obtained at a pouring temperature of 2170° F. This structure is conducive to good properties because the amount of microporosity (light areas) is moderate and the lead distribution (dark specks) is mostly favorable with some angular particles produced as a result of the moderately low pouring temperature. Had all the lead particles been rounded, the tensile properties probably would have been quite high because the bar is relatively sound. With high quality melts, such as the one represented here, this design produces high properties. The light streaks near the center are caused by a slight abrasion of the X-ray film. Compare with Fig. 22.

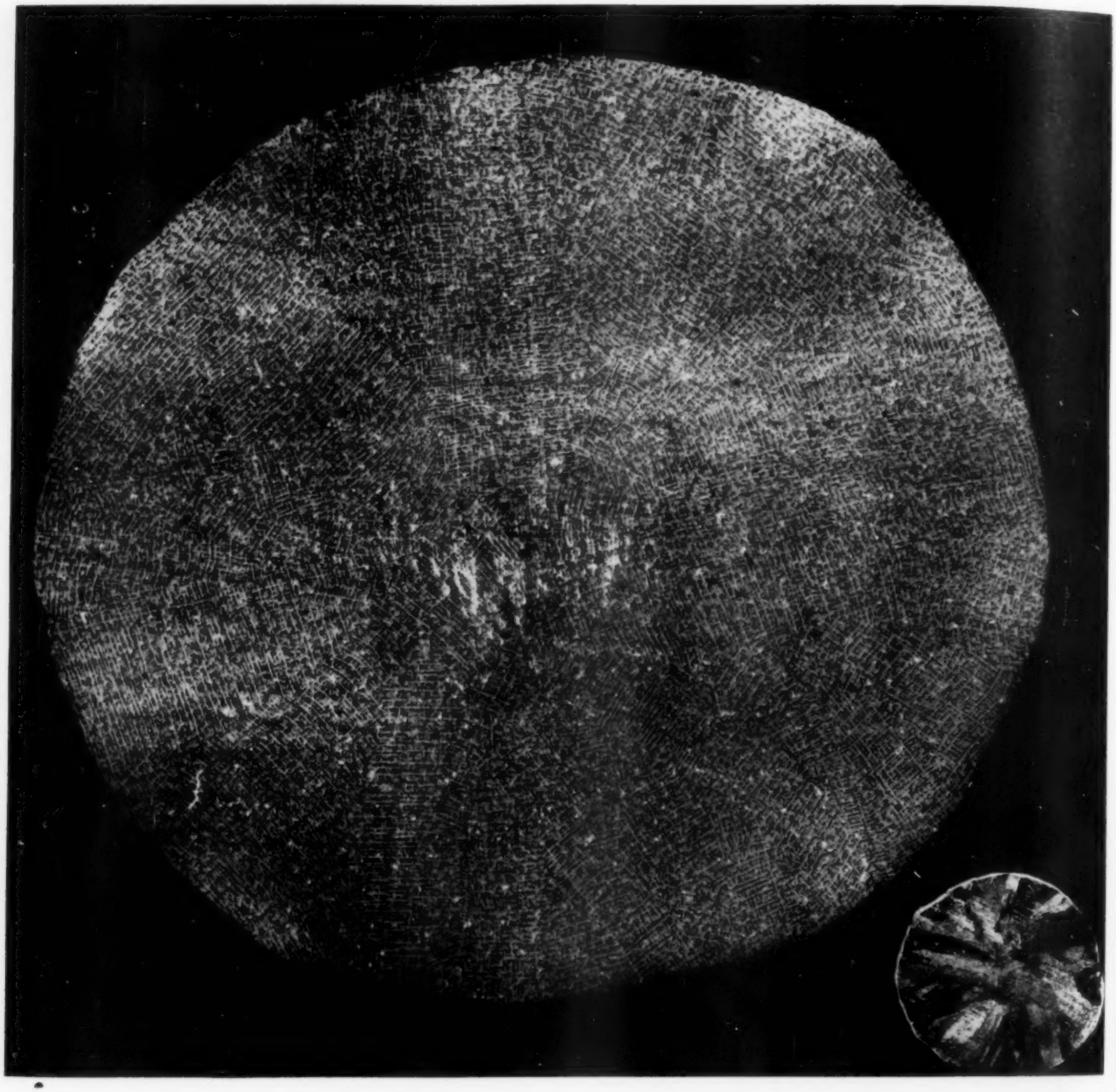


Fig. 22

A microradiograph of 85-5-5-5 alloy at X15. Similar to Fig. 21, except that the bar, A-871 O, is the British D.T.D., poured at 2070° F., producing 34,400 psi. and 27.5 per cent elongation, and a fine gray fracture. Compare with Fig. 21. In this instance at least, the lead particles (dark specks) are almost continuous with a slight tendency to form a lead-filled centerline shrinkage. The undesirable lead distribution and the centerline shrinkage cause low properties. Probably for these reasons, the D.T.D. bar produces generally low properties, even with good quality melts such as the one represented here, masking the effects of microporosity and the gas content of the melt. The light streaks near the center are caused by a slight abrasion of the X-ray film.

The effect of melt quality on the occurrence of microporosity illustrated by the comparison of Figs. 1, 2, and 3 with Figs. 4, 5, ad 6 is typical, although it should be noted that the amount of microporosity depends also upon the test-bar design and pouring temperature. But, for a given test-bar design and pouring temperature, the amount of microporosity is considerably increased with an increasing gas content of the melt. This is also evident on the fracture rating because porosity is indicated by an open, coppery or discolored area on the fracture.[1]

A comparison of many other microradiographs, not illustrated here, indicates that the gas content of the melt has no noticeable effect upon the size, shape, and distribution of the lead particles. This is somewhat confirmed also, by a comparison of Figs. 1, 2, and 3 with Figs. 4, 5, and 6, respectively.

Effects of Pouring Temperature

The effects of pouring temperature on the structure of 85-5-5-5 alloy are somewhat more complex than the effects of melt quality because both the porosity and the lead distribution are affected. Figures 1, 2, and 3 show the effect of pouring temperature on the structure of the L.T.B. test bars poured from a melt of poor quality, whereas Figs. 4, 5, and 6 show a similar effect of pouring temperature when the test bars are poured from a melt of high quality. Figures 7, 8, and 9 show the effect of pouring temperature on the structure of the Horizontal 5/8-in. web-Webbert test bar poured from the same melt of high quality, whereas Figs. 10, 11, and 12 show a similar effect of pouring temperature on the structure of the Modified 5/8-in. web-Webbert test bar also poured from the same high-quality melt.

In general, for a given melt quality and test-bar design, the amount of microporosity decreases with decreasing pouring temperature. As will be described in more detail later, the decrease in the amount of microporosity with decreasing pouring temperature is greater in the L.T.B. and least in the Modified 5/8-in. web-Webbert. The position of the Horizontal 5/8-in. web-Webbert is intermediate between the other two. In other words, while the amount of microporosity decreases with decreasing pouring temperature in all designs, the amount of the decrease is somewhat dependent upon the test-bar design, because the decrease apparently is less the slower the solidification rate of the test section of the bar.

There is some tendency for the porosity which does remain in the bars poured at the lowest temperature to be coarser than that obtained at an intermediate pouring temperature. This is well illustrated by comparison of Figs. 2 and 3 and Figs. 5 and 6. Experience has shown that this tendency for the porosity to be coarser at the lowest pouring temperature, is more pronounced when the bars are of cast-to-shape design, such as the L.T.B. or possibly the Crown type. These bars are not adequately fed, and localized shrinkage frequently occurs. If shrinkage occurs with columnar grain, it is of the centerline type, but when the grains are equiaxed, the shrinkage occurs as coarse more generally distributed cavities, as illustrated by Fig. 6. Microporosity is accentuated by gas evolution and decreases markedly with decreasing pouring temperature. On the other hand, shrinkage tends to increase with decreasing pouring temperature. This is caused in part, at least, by the decreasing microporosity which normally compensates for part of the contraction during solidification.

The most noteworthy effect of pouring temperature on the size, shape, and distribution of the lead particles is the occurrence of angular lead, rather than round or oval-shaped particles, at the lowest pouring temperature. This is true for all designs and is well illustrated by Figs. 3, 6, 9, and 12. These angular lead particles accompany the formation of equiaxed grains at the lowest pouring temperature. The macrographs at X3, accompanying each of the microradiographs, clearly indicate that the angular lead particles are associated with the finest equiaxed grains.

There is some indication that, as the pouring temperature is decreased from a high temperature near 2225° F. to an intermediate one near 2150° F., the lead particle size decreases slightly. This refinement of the lead particle when the pouring temperature is decreased from 2225° F. to 2125° F. is only slight and requires careful study. The size of the lead particle is best judged by the amount of space between them, since the quantity of lead is approximately the same in all of the specimens. This is admittedly difficult because, in a section 0.005-in. thick, there are 2 to 5 lead particles at various levels and their images are superimposed on the print. If the particles at the various levels are directly in line with the X-rays, they appear as one particle; if not, they may appear as one elongated particle or a series of separate particles. Examination of the prints with a hand lens will show that many of the elongated particles are actually a series of rounded particles, closely associated. The microradiographic technique is the most useful when the thickness of the section is of the same order of magnitude as the structural detail of maximum interest. This would mean sections about 0.002 in. thick instead of 0.005 in., but such sections offer additional difficulties. Had they been used, however, the lead distribution would have appeared much more geometrical and orderly, and the exact size and spacings of the particles would be much more apparent.

Effect of Test-bar Design

The effect of test-bar design on the amount, size, shape, and distribution of the microporosity and the size, shape, and distribution of the lead is apparently a function of (1) rate of cooling and (2) manner of feeding. In general, the test section of the cast-to-shape bar, such as the L.T.B.-2, solidifies the most rapidly, that of the Horizontal 5/8 in. web-Webbert second, whereas the Modified 5/8-in. web-Webbert is the most massive and the test section solidifies the most slowly of the three designs. Entirely in conformity with known metallurgical phenomena, for a given pouring temperature and melt quality, the amount of microporosity increases in these three bars in the order listed. A keel block would, of course, solidify still more slowly and contain still more porosity, as illustrated by a comparison of Figs. 19 and 20. Furthermore, for a given high or intermediate pouring tem-

perature, as illustrated by a comparison of Figs. 4, 5, 7, 8, 10, and 11, the lead particle size tends to increase quite noticeably in the same order. In the case of a keel block, however, the lead is not only very coarse, but quite angular at a normal pouring temperature, as shown by a comparison of Figs. 19 and 20. Apparently, the optimum lead distribution occurs at cooling rates about comparable to that which occurs in the Modified 5/8-in. web-Webbert.

Although it is not clearly illustrated on the accompanying microradiographs, the examination of several score microradiographs has indicated that the average tendency to form angular lead at the lowest pouring temperature appears to be about the same in at least six different designs investigated in some detail, but the test sections of all these six designs solidified as rapidly or more rapidly than that of the Modified 5/8-in. web-Webbert. Various heats differ somewhat in their tendency to form fine equiaxed grains at low pouring temperatures and in other respects as well, so that the tendency to form angular lead particles varies somewhat, also.

Localized Shrinkage

Localized shrinkage is produced by the lack of proper feeding. As a result, there is inadequate compensation of the contraction which occurs during solidification. As previously noted, the tendency to form localized shrinkage is greater the lower the pouring temperature. It occurs quite frequently in the cast-to-shape L.T.B. test bar poured at the lowest temperature. That is, the two factors, low pouring temperature and rapid solidification of the test section, which tend to reduce the volume of microporosity, accentuate localized shrinkage probably because the microporosity normally tends to at least partially compensate the solidification shrinkage.

Localized shrinkage of the centerline type also occurs in Crown bars, not illustrated here, but rarely in the Horizontal 5/8-in. web-Webbert or the Modified 5/8-in. web-Webbert bars. Since the centerline shrinkage is highly localized, it may or may not appear on the micrograph, depending upon whether or not the section happened to be taken from a place in the bar containing the shrinkage. Localized shrinkage is most readily observed on the fracture where it appears more or less near the center of the 85-5-5-5 alloy test bar as a light reddish or coppery colored area corresponding to the cavity. As indicated in Table 1, the occurrence of shrinkage on the fracture is shown by the *S* attached to the fracture rating. Therefore, the microradiograph should not be depended upon to reveal shrinkage; whereas, the fracture is a reliable criterion for this defect.

Correlation of Structure and Tensile Properties

Keeping in mind the above effects of pouring temperature, melt quality, and test-bar design on the structure of the bar, a correlation with the tensile properties can be readily made. It is entirely within expectations that the tensile strength and elongation, especially the latter, would be adversely affected by the occurence of microporosity. The larger the amount and the coarser it is, the more adverse the effect. Consequently, any factor such as decreased gas content, decreased pouring temperature, or the selection of a test-bar design having a higher rate of solidification will tend to increase both the tensile strength and the elongation values, all other factors the same, because the amount of microporosity is decreased.

The effect of lead particle size on the tensile properties is not quite so evident. In 85-5-5-5 alloy of constant lead content, the volume of lead remains the same though the size, shape, and distribution does not. It should be noted that the lead particles are substantially without strength and, therefore, do not contribute to the ductility or strength of the bar. Consequently, if the lead particles are coarse angular, they have a greater adverse effect than if they are rounded. Apparently, however, this angular lead has a greater adverse effect upon the elongation value than upon the tensile strength, because the latter invariably increases as the pouring temperature is decreased to low values. The angular lead particles produce a notch effect and in conformity with such a phenomenon, the ductility is the most adversely affected; whereas the tensile strength is certainly much less damaged. It appears quite likely also that the finer the rounded lead particles, the more adverse the effect they have upon the tensile properties, particularly the elongation. Similar effects have been noted in malleable cast iron where the finer the carbon nodules, the lower the ductility, but with no noticeable change in tensile strength, yield strength, or hardness of the matrix.

The actual effect of melt quality, test-bar design, and pouring temperature on the mechanical properties obtained will, therefore, depend upon the net effect of these three factors on (1) the microporosity, (2) the lead particle size and distribution, and (3) the tendency to form localized shrinkage. The mechanical properties of 85-5-5-5 alloy test bars have been satisfactorily accounted for by these three factors, and they will be referred to as the "porosity factor", "lead factor", and "shrinkage factor". The interrelationship of these factors has been quantitatively estimated and is graphically illustrated by Fig. 14 and 17.

On the basis of the available knowledge obtained to date, the lead factor is dependent mainly upon the pouring temperature and test-bar design and not upon the gas content of the melt. However, with increasing gas content, the microporosity increases and the fracture always occurs through the voids. As the total percentage of area on the fracture comprising these voids increases, the less important the lead factor becomes. Thus, if the area of porosity on the fracture increased from 5 per cent to 10 per cent, the lead factor should decrease considerably because more of the fracture occurs through porosity and less through the metallic portion of the bar. Since the lead factor is dependent to some extent upon the grain size, as indicated previously, and possibly upon other factors not now known, the lead factor will vary somewhat from heat to heat, but the general principles described above are valid.

Taking the lead particle distribution obtained in

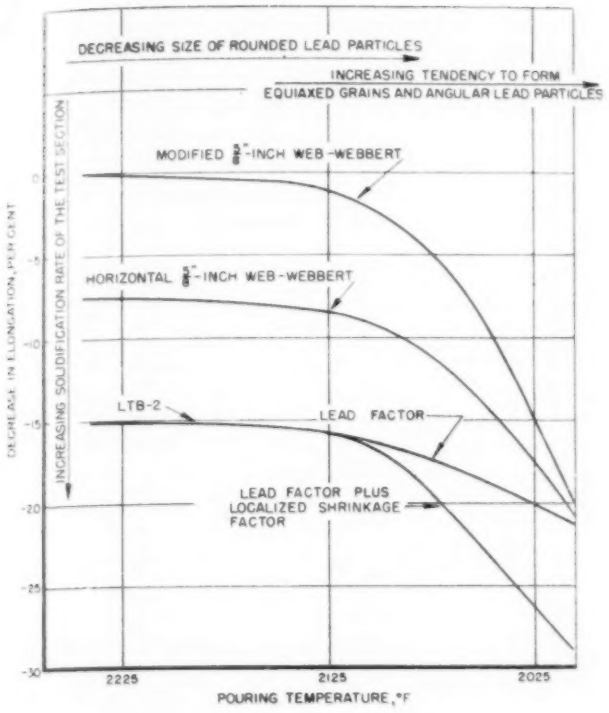


Fig. 14—Estimated average decrease in elongation produced by the lead particles in three different test-bar designs poured in 85-5-5-5 alloy over the temperature range indicated (the lead distribution is the most favorable in the Modified 5/8-in. web-Webbert poured from high temperature and is therefore taken as the zero point for purposes of comparison).

the Modified 5/8-in. web-Webbert test bar poured at 2225° F. as a standard, the lead particles obtained in this bar at a lower pouring temperature or in other bars at this or a lower pouring temperature are less favorable, as previously indicated. An estimate of these average effects of pouring temperature on the lead distribution in the three different test-bar designs, in terms of decrease in per cent elongation, is illustrated by Fig. 14. Thus, at a pouring temperature of 2225° F., it was estimated that the Horizontal 5/8-in. web-Webbert has a sufficiently less desirable lead particle size, shape, and distribution to produce a 7.5 per cent lower elongation than that obtained in the Modified 5/8-in. web-Webbert poured from the same temperature. On the other hand, the L.T.B. apparently has a still less favorable lead distribution, and it has been estimated that this finer lead particle size obtained at a pouring temperature of 2225° F. is sufficient to produce 15 per cent lower elongation values. As indicated by this chart, for a given design, the lead factor becomes less favorable with decreasing pouring temperature. This less favorable lead distribution is rather minor down to a pouring temperature of 2125° F., below which it becomes rapidly less favorable, with formation of equiaxed grains and increasing amounts of coarse, angular lead particles. Thus, according to Fig. 14, it has been estimated that a reduction in pouring temperature from 2225 to 2025° F. reduces the elongation value, because of the less favorable lead distribution, so that all designs produce about 20 per cent lower elongation than would have been obtained if the lead distribution had been as favorable as that obtained in the Modified 5/8-in. web-Webbert poured at 2225° F. The lead factor expressed as a decrease in the per cent elongation is the same for all designs, about −20 per cent, because, when the bars are poured near 2025° F., all three types of bars have similar angular lead particles. In addition, as previously noted, the estimated lead factor differs considerably in the three test-bar designs poured at the higher temperature because of the different solidification rates of the test section of the three designs.

Shrinkage Factor

The shrinkage factor, important in the L.T.B. bar, is also shown in Fig. 14.

The average effects of pouring temperature on the tensile strength and elongation values of these three designs have been reported,[1] but these relationships are again illustrated in Fig. 15 for high-quality melts and in Fig. 16 for low-quality melts. Since the average effect of pouring temperature on the test-bar properties is based on a considerable amount of data and the effects of lead particle size and localized shrinkage has been estimated, the average effect of the porosity

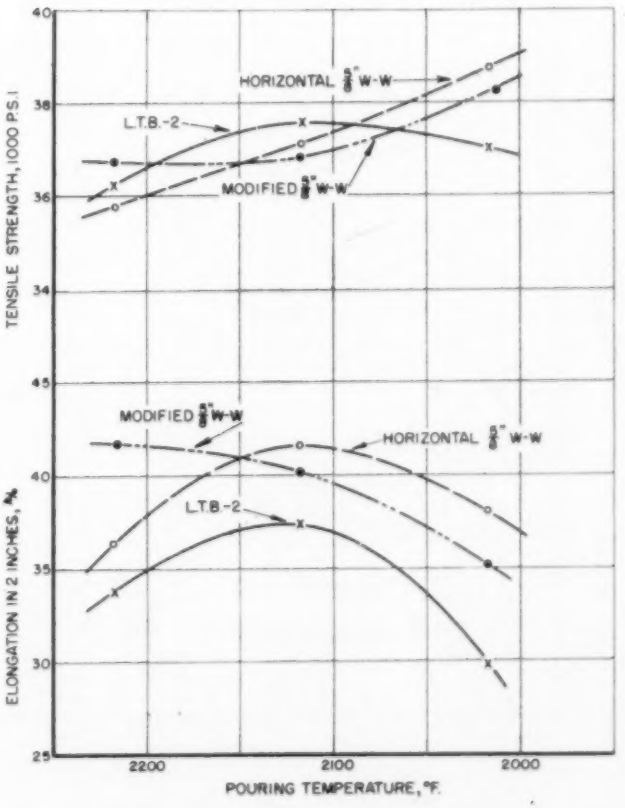


Fig. 15—The effect of test-bar design on the tensile properties. Each point represents the average of approximately 54 test bars from 27 melts prepared under a glass slag are excluded.

factor can be calculated by difference. This has been done, and the resulting values of the porosity factors obtained for the three different designs, the L.T.B., the Horizontal 5/8-in. web-Webbert, and the Modified 5/8-in. web-Webbert, are shown by Fig 17. The zero porosity factor is taken at complete soundness, i.e., the absence of all microporosity. If the Modified 5/8-in. web-Webbert were completely sound, it would have an estimated elongation value of 42+17.5 or 59.5 per cent since the average actual elongation is 42 per cent, the lead and shrinkage factors are zero and the porosity factor is—17.5 per cent. As another example, the estimated lead factor for the Horizontal 5/8-in. web-Webbert test bar poured from 2125° F. is about —8.5 per cent elongation. The shrinkage factor is, of course, zero, and the porosity factor is approximately —9 per cent elongation for high melt quality and —31 per cent for low melt quality. Consequently, the elongation of the bar is 59.5 per cent (the estimated elongation of the Modified 5/8-in. web-Webbert bar poured at 2225° F., but free of microporosity and having a lead rating of zero) minus 8.5 per cent less 9.0 per cent or 42 per cent elongation for high-quality melts, and 59.5 per cent minus 8.5 per cent less 31.0 per cent or 20 per cent elongation for low-quality melts. These values, of course, correspond to the actual average per cent elongation obtained on the Horizontal 5/8-in. web-Webbert poured from 2125° F. as illustrated by Fig. 15 and 16.

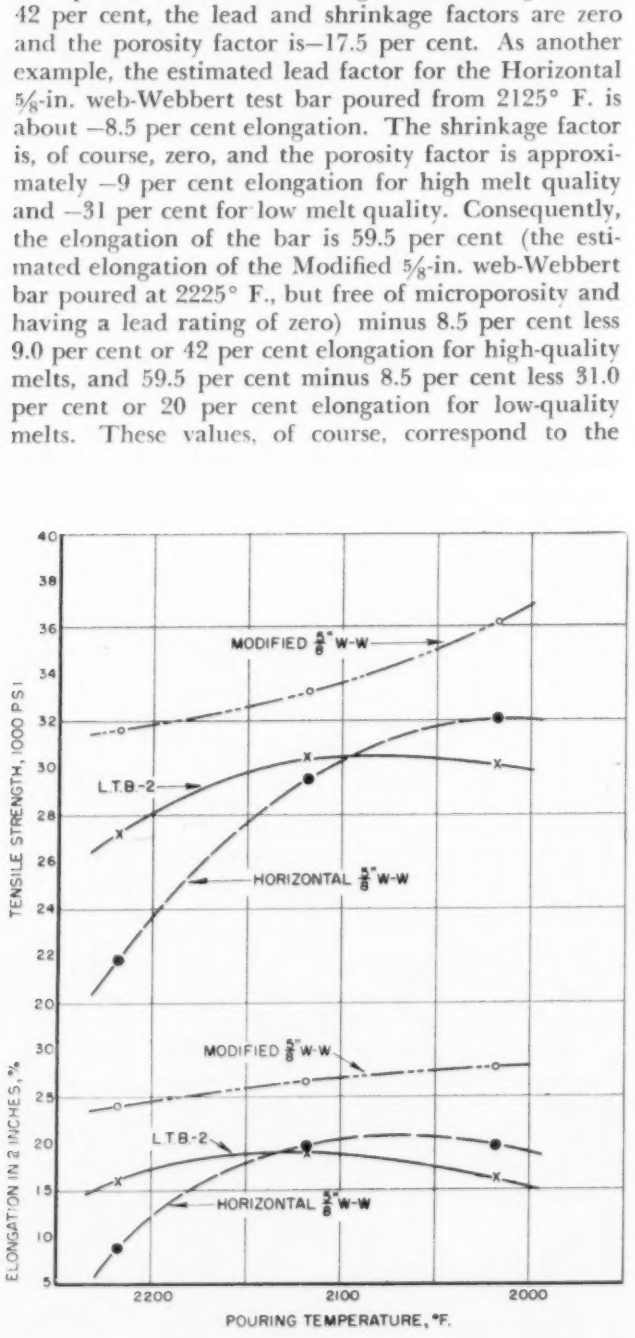


Fig. 16—The effect of test-bar design on the tensile properties of bars cast from melts of poor quality made under a glass slag in a clay-graphite lined high-frequency induction furnace. Each point is the average of six test bars from three melts.

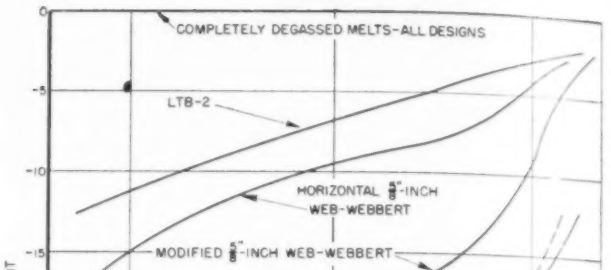


Fig. 17—Estimated average decrease in elongation produced by microporosity alone in three different test bars poured from high quality 85-5-5-5 alloy melts (full lines) and poor-quality melts (broken lines) over the temperature range indicated.

As a further example, the L.T.B-2 poured from 2025° has an estimated lead plus shrinkage factor of —26 per cent, a porosity factor of —3.0 per cent for high-quality melts and—17.5 per cent for low-quality melts. Consequently, the actual elongation values are 59.5 per cent minus 26.0 per cent less 3.0 per cent or 30.5 per cent for high-quality melts and 59.5 per cent minus 26.0 per cent less 17.5 per cent or 16 per cent for low-quality melts. These values correspond to the average values illustrated on Fig. 15 and 16 taken from the data on this bar poured at 2025° F.

It should be emphasized that these figures representing the quantitative effects of pouring temperatures are based on estimates. However, they do represent, in a fairly satisfactory way, the complex effects of test-bar design and pouring temperature on the microporosity, lead distribution, and localized shrinkage. These curves, of course, pertain only to elongation values. Similar estimates could be made for tensile values. Such curves, however, would be quite similar to those for the porosity, excepting that the adverse lead distribution has a less harmful effect upon the tensile strength than upon elongation. Because of this, the tensile strength tends to increase with de-

creasing pouring temperature for practically all test-bar designs and melt qualities, excepting those designs which are markedly subject to localized shrinkage at the low pouring temperature. It probably should also be emphasized that the curves represent average effects. Individual heats deviate somewhat from these average values. In general, however, the structure of the bars poured from heats which deviate from the average correlate very well with the tensile properties obtained. The cause or causes of the deviations from the average structure, however, are not so well understood.

It is evident that the average effect of pouring temperature on the elongation value will tend to produce a maximum at the intermediate pouring temperature. It is apparent from the preceding discussion and from Fig. 17 that a decrease in pouring temperature from about 2225 to 2125° F. produces a marked decrease in the amount of microporosity in the L.T.B. a less decrease in the Horizontal 5/8-in. web-Webbert, and still less in the Modified 5/8-in. web-Webbert, but normally has only a slightly adverse effect on the lead particle distribution, as shown by Fig. 14. As a result, this decrease in pouring temperature generally produces an increase in elongation, an effect which is obviously the least for the Modified 5/8-in. web-Webbert and the most for the L.T.B. However, when the temperature is dropped from 2125 to 2025° F., a further decrease in the amount of microporosity occurs in all designs, but because of the equiaxed grains, the lead particles become quite angular and have a very adverse effect on the elongation values, more than offsetting the improvement obtained by the decreased amount of microporosity. As a result, the average elongation value obtained at the lower temperature is less than that obtained at an intermediate pouring temperature.

It is well to note at this point that the test section of the bar which solidifies the most slowly has the most microporosity. Because of this large quantity of microporosity, changes in pouring temperature have little effect on the amount of this unsoundness, as illustrated by Fig. 10, 11, 12, and 17. It is quite likely that substantial changes in the gas content of the melt would also produce relatively small changes in the amount of microporosity. If so, such designs would not be sensitive to small differences in melt quality. Probably for this reason, such designs as the Modified 5/8-in. web-Webbert, and to a very much greater degree, the Navy keel block are not responsive to small differences in melt quality. On the other hand, the L.T.B. solidifies so rapidly that it tends to minimize the effects of gas content unless the pouring temperature is very high. This is because of the rapid directional solidification which tends to minimize the formation of microporosity. At low pouring temperatures, this design also introduces shrinkage, an adverse effect which is most likely to occur with degassed melts.

These effects are graphically illustrated in Fig. 18 which shows that, in accordance with established metalurgical phenemena, at very low gas contents, a given small increase in gas content produces increasingly larger amounts of unsoundness, the heavier the section thickness and the lower the solidification rate. Heavy sections probably would be very suitable for measuring relative melt quality if the melts had very low gas content. However, in more nearly commercial ranges of gas content, an intermediate cooling rate is probably slightly superior to either a more rapid (L.T.B.) or a slightly less rapid (Modified 5/8-in. web-Webbert) solidification rate.

It should be observed that of the three designs mainly considered in this paper, the Modified 5/8-in. web-Webbert produces high tensile properties in 85-5-5-5 alloy because it is generally free of dross, the lead particles are favorable, and the amount of microporosity is large but not so coarse and angular as in the keel block. This latter design, in contrast to the Modified 5/8-in. web-Webbert, produces low properties because both the lead and porosity tend to be coarse angular, as shown by a comparison of Figs. 20 and 21. It should be emphasized, therefore, that the choice of the test-bar design is not based on whether it produces high or low properties but on its sensitivity to melt quality. However, if a given test-bar design produces properties which are generally low, it is not likely to be responsive to melt quality because it requires such large changes in gas content of the melt to affect the test-bar properties appreciably. The British D.T.D. bar falls in this category, as shown by a comparison of Figs. 21 and 22, and it is probable that its usefulness is limited for this reason. If so, it will no doubt eventually be abandoned despite the fact that it is still highly regarded in

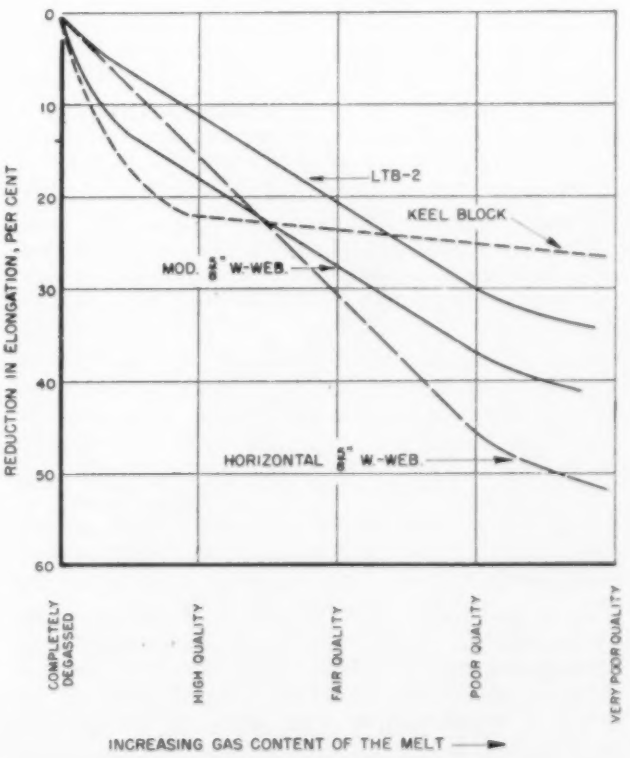


Fig. 18—Estimated effect of gas content of the melt on reduction in elongation of four different test-bar designs poured near 2225° F.

England. Unfortunately, the trend there appears to be toward the keel-block type because it produces similar properties, despite the source or quality of the melt.[2] Thus, the insensitivity of this bar to melt quality appears inadvertently the main reason behind the recommendation that it be used.

A good test bar should be moderately sound and produce high properties with high-quality melts and become increasingly unsound with decreasing melt quality. Low properties should be obtained with low-quality melts only.

Summary

The three main structural features which mainly determine the tensile properties of 85-5-5-5 alloy are:

1. The quantity and distribution of microporosity
2. The size, shape, and distribution of the lead particles
3. The occurrence of localized shrinkage

The greater the amount of microporosity and the coarser it is, the more adverse the effect on both tensile strength and elongation. The amount of microporosity decreases with (1) a decrease in the gas content of the melt, (2) a decrease in pouring temperature, and (3) an increase in the solidification rate of the test section of the bar. Consequently, these factors tend to increase the tensile strength and elongation values.

The Horizontal 5/8-in. web-Webbert apparently is slightly more sensitive to melt quality since more rapid solidification rates tend to minimize slightly the unsoundness produced by gas in the melt. Less rapid solidification rates, as in the Modified 5/8-in. web-Webbert, are slightly less sensitive and heavy sections, such as the keel block, apparently are markedly less sensitive to melt quality because large changes in gas content are required to effect the test-bar properties appreciably. This is probably a characteristic of all test-bar designs which produce relatively low properties even with high-quality melts.

Coarse, rounded lead particles are the most favorable, and as the rounded lead-particle size decreases, the lead probably becomes slightly less favorable in distribution. The Modified 5/8-in. web-Webbert has the optimum lead distribution since more rapid solidification rates produce less favorable fine lead particles, whereas less rapid solidification rates, as in the keel block, produce much less favorable, coarse, angular lead particles. At low pouring temperature, all test bar designs tend to produce equiaxed grains accompanied by relatively coarse, angular lead particles which, in conformity with established metallurgical phenomena, have a very adverse effect upon the elongation values but a less adverse effect upon the tensile strength. Furthermore, localized shrinkage is accentuated by low pouring temperature, especially in the cast-to-shape test-bar designs, such as the L.T.B.-2. Consequently a reduction in the pouring temperature in the test bar reduces the amount of microporosity but has an adverse effect upon the lead distribution or accentuates localized shrinkage or both. Therefore, the highest elongation values are obtained normally at the intermediate pouring temperature because, as the pouring temperature is decreased from approximately 2225 to 2125° F., a substantial reduction in microporosity generally occurs, but with a production of only slightly less adverse lead distribution. However, when the pouring temperature is dropped from 2125 to 2025° F., the beneficial effects obtained by the decreased amount of microporosity are generally more than offset by the coarse, angular lead particles which are produced. In the cast-to-shape design, such as in the L.T.B.-2, this adverse effect is further accentuated by the occurrence of localized shrinkage. In general, a good correlation is found between the microstructure revealed by the microradiographs and the test-bar properties obtained.

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DISCUSSION

Chairman: G. P. HALLIWELL, H. Kramer & Co., Chicago

Co-Chairman: B. M. LORING, Naval Research Laboratory, Washington, D.C.

A. J. SMITH[1] *(written discussion):* Messrs. Kura and Eastwood have been engaged in a program of research which is of interest to nearly all non-ferrous foundrymen and the results of which have been eagerly awaited. They are to be highly commended for bringing to bear on the problem some of the most advanced tools of metallurgy; also for their endeavor to answer the whys that keep cropping up at every stage.

Unfortunately, they have been working on one of the most temperamental of foundry alloys. The 85-5-5-5 alloy poured from the same pot of metal into a series of identical test bar molds (within the limits of foundry control) and with a slight temperature drop from the first to the last mold will show variations of 8 per cent elongation and 3000 psi. tensile strength, these variations irrespective of the order of pouring the bars or the test bar pattern. To many foundrymen such variations have been merely a cause for annoyance since the specification tensile properties required are so low that under the worst variations the minimum requirements usually are exceeded. This is illustrated in the bar shown in Fig. 2 where even with low melt quality the tensile properties were well above the minimum.

These same temperamental variations are evident in the present paper. Figure 15 compiled from some 486 bars from 27 melts shows superior elongation values at any pouring temperature of the two Webbert type bars, at 2120° F., both Webbert bars showing better than 40 per cent elongation, the L.T.B.-2 bar some 37 per cent elongation. In Table 1 the results on the L.T.B.-2 bar for high quality melts are definitely superior to those on the two Webbert bars at the two higher pouring temperatures, although all three sets of bars were poured from the same melt. Only the horizontal 5/8-in. web-Webbert bar poured at 2109° F. showed an elongation above 40 per cent while the L.T.B.-2 bars poured at 2240° F. and 2140° F. showed 43.2 per cent and 44.6 per cent, respectively. These variations cast some doubt on the conclusions the authors have drawn.

It should be pointed out, with reference to pouring temperature, that pouring temperatures may be compared on bars of the same design, but cannot be compared, per se, in bars of different design. As modified by the effects of cooling velocity, the significant temperature is the temperature of the metal in the gage length of the bar at the instant that flow ceases. This would depend not only on the initial pouring temperature but also on the time required to run the mold and some functional relationship of surface area and volume of casting including its gating system. An effort has been made to study these compara-

[1] Lunkenheimer Corp., Cincinnati, Ohio

tive temperatures by direct measurement in the laboratories of the Lunkenheimer Company. This has not been too successful suggesting that for such studies recording pyrometers of higher speed than now available would be required.

The objection the authors raise to center line shrinkage in the cast to size bar such as the L.T.B.-2 or the crown bar may not be valid. Bronze foundry product usually is cast to size and center line shrinkage is a normal occurrence with cold pouring. The expedients employed in the steel foundry for insuring directional solidification, such as chills and padding are rarely employed in the bronze foundry. Thus it does not appear logical to establish a freezing pattern in a test bar which would otherwise rarely be found.

The authors' attack of the lead problem is on the right track. The variations above noted are not nearly as significant in ASTM-B-61 alloy with 1.5 per cent lead, and they are worse in the higher leaded alloys. The use of microradiography for study of lead distribution is highly commendable.

The answer to test bar design does not appear to be forthcoming from the present paper and probably will not be until more is learned of the manner of solidification of castings. Properly, a test bar should show the ultimate properties available in a high quality melt.

A question naturally arises, what properties can be sought as the ultimate? The writer has seen tensile strength results on 85-5-5-5 from a jobbing foundry of 41,000 psi. with consistant frequency. Some individual bars have run as high as 44,000 psi. Elongations in all of these cases has been on the order of 25 to 27 per cent. These results give some evidence of cold pouring.

In our own practice with the L.T.B.-1 bar our results are not so high with most bars in the neighborhood of 39,000 psi. tensile strength. On the other hand we have had a number of bars with elongations in excess of 45 per cent and a few as high as 50 per cent. Tensile strengths in these cases usually are approximately 40,000 psi.

The Webbert type bars rarely show such good results for high quality melts. However, if they are more critical of melt quality, then they should be studied further for that reason, for a proper test bar should be more sensitive to variation than a well designed and gated casting.

DR. L. W. EASTWOOD: It is true that test bars are not too satisfactory and there is some variation in results. We were attempting to correlate structure with tensile properties and we have investigated a number of bars which apparently were cast under similar conditions, but which showed differences in tensile properties. In general we can see the reasons for the differences in properties because of the differences in structure; but we do not know or rarely understand what it was that caused the differences in the structure. We can get the correlation, then, between the structure and the properties, but we do not always understand what causes the differences in structure. We, therefore, do not agree with Dr. Smith when he suggests that the inevitable variations in test-bar properties "cast some doubt on the conclusions the authors have drawn."

DR. B. M. LORING: The authors have completed a much needed and highly commendable investigation on the microstructure of a leaded bronze. Quantitative data and the factors contributing to the strength of bronzes are very difficult to secure because of the large number of variables. The authors have made a notable contribution to the metallography of the 85-5-5-5 alloy.

There are still many questions open for discussion regarding the effect of free lead particles in bronzes. Such particles are believed to be important not only in their effect on the tensile properties but also in the tendency to increase the pressure tightness. The free lead particles obviously cannot be absolutely pure in a multi-component system, but must have very small percentages of tin, zinc, and even copper in solution. The lead solution remains liquid to about 327° F. while the remainder of the alloy becomes solid. Being molten, the lead is forced to take the shape imposed upon it by the adjacent dendrites of bronze. Angular dendrites are then the cause for angular lead particles.

Is it sufficient to study the configuration of the lead or is it necessary to probe deeper into the mechanism formation of the dendrites to describe the complex factors contributing to the tensile strength of bronzes?

MEMBER: It is my belief that the zinc ratio had something to do with the lead distribution, or the effect of the lead. In the production of valves we found it advisable to add zinc to the 85-5-5-5 alloy.

DR. EASTWOOD: We cannot say much about the effect of composition on lead distribution inasmuch as we confined our study entirely to the 85-5-5-5 alloy. While we at one time thought there may be some effect of melt quality on the lead distribution, we are satisfied now that the quantity of gas in the melt apparently has no effect upon the size or shape of the lead particles.

H. J. ROAST[2]: I have had no experiences in microradiography, but feel from observing the photographs that they must be of a very high order.

The weight of the keel block is 50 lb. approximately. With such a keel block made of the 85-5-5-5 alloy we have frequently obtained 40,000 psi. tensile strength and 40 per cent elongation. The secret is in the pouring temperature, and it surprises me that a pouring temperature as high as 2230° F. is advised. We use 1900° F.

At our plant we have a standard pattern for a bushing of suitable size so that we can pressure test it under 6000 psi. pressure. However, the test is made after first eliminating one-third of the O.D. and one-third of the I.D. This leaves a bushing with one-third the original cross-section with the outside and inside removed. Under these circumstances we are justified in correlating pressure tests or pressure resistance with soundness of metal. We do not depend on skin-tightness. We test the 85-5-5-5 alloy up to about 1000 lb. pressure. Of course, the cross-section varies with the others, but we test other alloys up to 6000 and 8000 lb.

Why is it that lower temperatures would not yield better results? If the shape of the bar is a point of issue, and the object, research, then I am not in disagreement. If, however, the object of practical foundrymen is to know how he is to judge whether his metal will make a pressure-proof casting in a valve, for example, then he would be well advised to use the keel block for the reasons given.

DR. EASTWOOD: We would probably obtain good properties in the keel block if we poured them at very low temperatures. I am not sure that the keel block poured at 1950° F. would reflect melt quality.

One investigator in England concluded that the keel block was the best test bar because it showed about the same properties regardless of how or where they were prepared; or in other words irrespective of the melt quality. That is not the object. The test bar should reflect melt quality. Certainly the keel block does not at pouring temperatures higher than 2050° F.; whether it would at temperatures lower than 2050° F. is another matter. The data on other designs poured at low temperatures indicate that melt quality is not reflected under these conditions.

W. A. BAKER[3] *(written discussion):* The authors' detailed examination of these cast test bars by the micro-radiographic technique has brought out some interesting information on the effect of lead distribution in this alloy. As the authors remark, it is not an easy matter to distinguish between lead particles and cavities by ordinary metallographic examination and their work provides yet another illustration of the usefulness of the micro-radiographic method.

In the work done by the British Non-Ferrous Metals Research Association on the casting of bronzes most of our effort in the early stages at least was devoted to the simpler types of bronze casting alloys in order to keep variables other than shrinkage and gas effects to a minimum and the present paper in illustrating the complicating effect of lead distribution seems to justify that action. For this reason we have not a very extensive experience of the relative merits of different types of test bar for leaded alloys but a fair amount of work has been done with this type of alloy in recent years and so far our experience is that for research purposes at least the D.T.D. type test bar is the most reliable guide to the gas content of the metal irrespective of the type of bronze alloy used. It should be noted however that for research purposes we attach more significance to density measurements than to tensile test

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[3] Senior Metallurgist, British Non-Ferrous Metals Research Association, London, England

data in attempting to assess the gas content of the metal poured or in assessing the amount of gas absorbed by a bronze during solidification in the sand mold. The choice of the D.T.D. type bar for this purpose is dictated by two main considerations, (a) the bar is not shaped and cools more slowly than shaped bars of the L.T.B.-2 type and (b) despite this fact it is relatively well fed and when cast from degassed material, and when no gas absorption occurs in the mold, the bar contains a relatively small proportion of shrinkage voids. Consequently, small amounts of gas cause a rapid decrease in the density of the test bar and larger amounts cause further decreases and the bar in our experience is very satisfactory from this point of view. The experimental work published in a paper in the *Journal* of the Institute of Metals, vol. 70, p. 373 (1944), shows how the gas content of the melt as judged by the density of this type of test bar, affects the properties of poorly fed castings by altering the distribution of porosity in them and in view of these results we have no misgivings about the virtues of the D.T.D. type of test bar for work of this character. It is worth noting that in the paper referred to, a poorly fed test casting was used which proved to have the best properties when the leaded alloys were thoroughly degassed, whereas for other types of bronze casting alloys gasses present in the metal before pouring, or which were absorbed by the casting during solidification, improved the distribution of porosity and thereby improved the poorly fed casting. More recently it has been shown that the same type of casting with an even more restricted ingate, leaked under pressure when made in degassed leaded alloys and in this case small amounts of gas had a beneficial effect, so that the behavior of leaded alloys differs from the other types only in degree. It seems probable, however, that the gas contents likely to be useful in these leaded alloys in practice will be smaller than those likely to be useful in the lead-free alloys and it is particularly important that a test bar used for these alloys should be sensitive to the effects of small amounts of gas. Our results to date indicate that the D.T.D. type bar is satisfactory in this respect.

It is appreciated that production foundries would prefer to use tensile data as an index of the condition of bronze melts and while our own experience with the D.T.D. type bar has shown it to be quite satisfactory in this respect the foundry industry in Great Britain has been reluctant to use this type and heretofore has normally used shaped bars gated and fed from the ends, i.e., similar to the L.T.B. bar. It is recognized, however, as the authors remark in their paper, that this type of bar freezes relatively quickly so that the presence of small amounts of gas may not be detected. The authors suggest that the tensile properties of the bar are sensitive to amounts of gas of practical significance in the foundry but to judge from our observations amounts of gas smaller than those detected by the L.T.B. type are also very significant. It is for this reason that the present trend in this country is to use a keel bar type consisting of a 1 in. diameter bar, not shaped, fed over its entire length through a ½ in. wide ingate. This type of bar in this Association's experience is not quite so well fed as the D.T.D. type so that degassed alloys give test bars with relatively high volumes of shrinkage voids. The typical data shown in Table A illustrates this point—compare the percentage voids for the D.T.D. and wedge shaped bars cast from degassed alloys. The same table shows that when the test castings contain gas, absorbed by the metal during solidification in the mold, the density of the test-bar is more markedly affected in the D.T.D. than in the wedge type especially with high casting temperatures. Nevertheless the tensile properties of the two types of bar were affected to about the same extent by the presence of gas. Unfortunately data for bars with intermediate gas contents are not available but it is to be expected that smaller gas contents will cause some reduction in the properties of the D.T.D. type but would have little or no effect on the wedge bar. Nevertheless it seems to us that the wedge type is likely to be a more sensitive index to the gas content than shaped bars of the L.T.B. type and it remains to be seen whether experience in the use of the wedge type bar justifies this view.

The evidence in the paper indicates that relatively rapid or very slow rates of solidification produce angular particles of lead which have an unfavorable effect on the properties of the leaded gunmetals. It is to be expected that the D.T.D. type bar would freeze at a rate intermediate between that for the L.T.B.-2 type and that for the wedge type bars described and it is therefore somewhat surprising to find an unfavorable lead distribution as illustrated in Fig. 22. The particular bar referred to was poured near the lower end of the pouring range used by the authors and it would be of considerable interest to know whether the unfavorable lead distribution shown in Fig. 2 was also present in bars cast at higher temperatures. Our experience confirms that of the authors to the extent that the D.T.D. type bar generally gives somewhat lower values of tensile strength for these alloys, and indeed for most of the bronzes, than shaped bars of the L.T.B. type but we have heretofore attributed this to two facts, firstly that the shaped bars contain less total voids and secondly that the soundest metal in the D.T.D. test bar section, i.e., the outer part, is removed when machining the test piece length, whereas most of the outer section of a shaped bar is retained in the test section. While not disputing the fact that lead distribution has an appreciable effect upon the tensile properties of these alloys it does seem likely that the effect is small in comparison with the effect of porosity, and particularly, with the effect of the distribution of porosity. A further factor to be considered is the form of the porosity present and it should be noted that the micro-radiographic technique will not reveal the intercrystalline and interdendritic fissure type porosity so commonly present in cast bronzes and which has been shown (*Journal* of the Institute of Metals, vol. 67, p. 231 (1941)) to have extremely detrimental effects on mechanical properties even when the total volume of the porosity is small. This type of porosity is usually fairly widely dispersed and is largely responsible for the fact that as-cast sections of bronze castings, will leak under pressure, the fissures providing channels of communication from one surface to the other. Nevertheless this porosity will generally be most prevalent in the center part of the section and it is probably this factor which is mainly responsible for the relatively low tensile properties obtained when machining the test piece involves the removal of the relatively strong outer layers.

In reading the authors' paper one gets the impression that the horizontal ⅝ in. web-Webbert bar is the most sensitive to gas of those tested but the authors expressed no opinion in their summary as to which test bar type is preferred for foundry control purposes, bearing in mind the various factors discussed in the paper. It would be of interest to know their final verdict on this point.

W. W. EDENS[4] *(written discussion)*: The authors and their sponsors, the Non-Ferrous Ingot Metal Institute, are to be congratulated for undertaking to evaluate metallurgical and mechanical properties as related to test bar practice and for making the results of their studies to date available to the Brass & Bronze Division members through the medium of this fine paper.

[4] Formerly Ampco Metal, Inc., now Badger Brass and Aluminum Foundry, Milwaukee

TABLE A—COMPARISON OF PROPERTIES OF B.S.I. WEDGE BARS AND D.T.D. BARS*

Alloy		D.T.D. Bar	B.S.I. Wedge Bar
88/8/4 Lead-free Gunmetal, 0.02% P. Thoroughly degassed.	Voids, %	1.4 to 0.7	2.5 to 0.9
	T.S. ton sq. in.	20.9 to 22.6	18.9 to 20.6
	Elongation, %	54 to 45	43 to 37
83/7/5/5 Leaded Gunmetal, 0.02% P. Thoroughly degassed.	Voids, %	0.5 to 1.0	1.3 to 1.4
	T.S. ton sq. in.	17.5 to 16.2	16.0 to 17.6
	Elongation, %	36 to 18	36 to 25
88/8/4 Lead-free Gunmetal, 0.08% P. Gas absorbed from sand mold.	Voids, %	2.0 to 2.2	2.9 to 2.5
	T.S. ton sq. in.	17.4 to 18.4	15.9 to 17.9
	Elongation, %	29 to 32	28 to 35
P. Gas absorbed 10% Sn, 0.38% from sand mold.	Voids, %	1.1 to 0.4	2.1 to 1.1
	T.S. ton sq. in.	18.9 to 23.8	17.1 to 22.4
	Elongation, %	26 to 41	24 to 30

* A range of properties is shown, the figures being average results for high casting temperatures (on the left) and low casting temperatures (on the right) for each type of test bar.

The microradiograph work is excellent. However, if a larger magnification is practical it would seem to be advantageous in the evaluation of structure variation as compared to mechanical properties. Even with a magnifying glass I found the interpretation somewhat difficult, however, the problems of technique are recognized.

The emphasis on the form of the lead particles bother me somewhat. While this is a recognizable distinction in the microradiographs it must also be recognized that it is a result and not a cause. The lead is the last material to solidify in the test bar and therefore takes the shape not already filled with solidified metal. In other words, the lead tends to show in relief the dendritic pattern of the matrix.

Apparently the type, size and specific orientation of these dendrites which the authors have chosen to recognize as the shape of the lead (angular lead) has a marked effect on the mechanical properties. Without a doubt, conditions of cooling or chill which would include pouring temperature play a very important part in the formation and growth of the dendritic pattern. However, it seems probable that other factors including composition, particularly with respect to minor impurities, gas content, mold conditions and others may be important.

If it is within the scope of the present investigations, a study of the conditions affecting this dendritic type of structure might prove very valuable, not only as related to test bar practices but to general casting practice as well.

DR. EASTWOOD *(authors' closure):* The authors wish to express their appreciation of the valued contributions submitted by those discussing this paper.

While we agree with most of Mr. Smith's comments, we do not in any way agree with the paragraph in which he suggests that shrinkage in a test bar is acceptable because shrinkage may occur in a commercial casting. First of all, such a point of view is contrary to the now almost universally accepted one that test bars are intended to represent melt quality and not the properties of any specific casting. In other words, test bars represent the melt quality which is only one of many factors determining the quality of the casting. If shrinkage occurs in the test bars, this defect will produce low properties obviously unrelated to the melt quality. Any test bar, therefore, which is subject to shrinkage defects should not be employed in the foundry because low values may be caused by shrinkage and not by poor melt quality.

In reference to Dr. B. M. Loring's last paragraph, it may be definitely stated that the size and shape of the lead particles is entirely dependent upon the way in which the dendrite containing them grows. We know that a coarse, columnar dendrite tends to produce rounded lead particles; whereas, an equiaxed one tends to produce angular lead particles. The reasons for this have not been established, but it certainly would be an interesting subject for investigation.

Mr. Roast apparently misread the paper because we do not recommend that the keel block be poured at 2230° F. Our recommendation is that the keel block be avoided when pouring 85-5-5-5 alloy.

We are in general agreement with Mr. Baker's views, and greatly appreciate having his comments. We agree that density measurements are of real value in investigations of this kind, and we are making these measurements now somewhat belatedly. It should be noted that the B.S.I. wedge bar used in Britain is very substantially different from the much heavier keel block used in a part of our investigation. Mr. Baker implied that the tendency in Britain is to use this B.S.I. bar because "amounts of gas smaller than those detected by the L.T.B. type are also very significant." The data in his Table A, however, are fairly typical for heavy bars of this type in that at the higher pouring temperature the density of 88-8-4 alloy poured from a thoroughly degassed melt is about the same as when gas is absorbed from the sand mold. This illustrates our contention very well that bars of this type have a fairly high degree of unsoundness when poured from melts of a high quality, and it makes little or no difference in the amount of this unsoundness whether gas is absorbed from the mold or not, or whether the melt is of rather poor quality or high quality; substantially the same results are obtained in any case. We note that there is some difference in nomenclature and Mr. Baker refers to the unsoundness in bars of this type as "shrinkage porosity." We refer to this defect as "microporosity" because the term shrinkage porosity indicates that the defect is caused by shrinkage, an implication which is partly erroneous.

In neither our microradiographic nor our very extensive metallographic examinations at higher magnifications have we observed the so-called fissure type of unsoundness, illustrated by Fig. 13, *Journal* of the Institute of Metals, vol. 67, opposite page 238 (1941). Such a structure is probably either a result of the metallographic technique employed, or an indication of trapped dross films in the metal. Regardless of their origin, we do not believe that such defects have been of any importance in any of the sand castings prepared in our work.

Mr. Baker stoutly defends the British D.T.D. bar, but we cannot share his enthusiasm for it, even though we do not think too highly of any test-bar design for 85-5-5-5 alloy cast in ordinary sand molds. We obtain about the same results with the British D.T.D. bar at 2135° F. as we do at lower pouring temperatures; namely, low properties are obtained with little or no microporosity, producing a sound fracture, excepting for centerline shrinkage. We are not prepared to say that the lead distribution in the British D.T.D. bar would be unfavorable at all normal pouring temperatures. We have observed, however, that all British D.T.D. bars show centerline shrinkage and this, undoubtedly, at least in part, accounts for the low properties obtained. We are not inclined to favor any test-bar design which produces shrinkage because this defect is not related directly or precisely to the gas content of the melt, but has a very adverse effect upon the tensile properties.

Of the test bars investigated, we would be inclined to select the Horizontal $\frac{5}{8}$-in. web-Webbert as being the most sensitive to the gas content of the melt. It is quite likely that this is a suitable design, but we are doubtful that the ordinary sands are very satisfactory for making the casting because the gas absorption from the mold interferes with reliable qualitative measurement of the gas content of the metal in the pouring ladle.

We agree with all of Mr. Eden's comments, excepting a part of the sentence "apparently the type, size, and specific orientation of these dendrites which the authors have chosen to recognize as the shape of the lead (angular) has a marked effect on the mechanical properties." We are not inclined to think that the angularity of the lead is the same as the size and specific orientation of the dendrites. As indicated in our reply to Dr. Loring's comment, the manner of growth of the dendrites determines the shape of the lead particles; i.e., the lead particles invariably become angular in all castings poured at a temperature low enough to form equiaxed grains. Normal variation in impurities, such as gas content, has had no perceptible effect upon this tendency. It is true, however, that some heats of 85-5-5-5 alloy may produce fine equiaxed grains at a pouring temperature of 2125° F.; whereas, another heat may require a pouring temperature of 2025° F. to produce a similar fine equiaxed grain. Beyond noting this relationship, we are not prepared to account for the shape of the lead particles formed.

MICROSTRUCTURE OF SILVERY PIG IRON

By Richard Schneidewind[1]
and Carl Harmon[2]

ABSTRACT

Comparatively little has been published on the microstructure of silvery pig iron and efforts to study it required the development of new etchants. The structure is a matrix of alpha silico-ferrite with large hyper-eutectic graphite flakes and small eutectic flakes. The grain shape varies, the crystals being equi-axed or elongated. X-ray diffraction indicates that the silico-ferrite in the silvery irons studied is the same. Steadite was found in all samples.

SINCE THE INCREASED USE of silvery pig irons during the war, some interest has been displayed in its metallographic structure. In common with other pig iron, this material should contain pro-eutectic as well as eutectic graphite as shown in Fig. 1. The matrix of ordinary pig iron consists of a mixture of pearlite and ferrite. The matrix of silvery pig iron has not been studied in great detail. The etching agents recommended in *Metals Handbook* were tried in an effort to resolve the structure, but it was found necessary to develop new etchants.

The silvery pig irons studied had the following compositions:

Element	*Per Cent*
Silicon	9.54 –11.08
Total Carbon	1.92 – 2.34
Graphitic Carbon	1.85 – 2.22
Combined Carbon	0.09 – 0.12
Manganese	1.31 – 2.36
Sulphur	0.16 – 0.22
Phosphorus	0.054– 0.118

Etching Tests

Considerable difficulty was encountered in etching the irons. The etching agent recommended in the ASM *Metals Handbook* was developed by M. G. Corson.[1] This solution exhibits a violently corrosive action on silvery pig iron and brings out the so-called "barley shell" structure which Corson shows in his publication. This is not a true structure and shows no grain boundaries but according to Corson is caused by internal strain and is affected by casting conditions and heat treatments. This reagent is hydrofloric acid, nitric acid and glycerine.

Other reagents were tried, including aqua regia, chromic acid, nital, ferric chloride, and many others. They were applied in the normal manner and also electrolytically. In all cases the action, if any, was violent and either simply corroded the sample or caused a dirty stain.

The late R. G. Guthrie[2] stated that his specimens were etched with 50 per cent aqueous nitric acid but no details for use were given. This solution, used at room temperature, had no etching effect but only produced a dirty stain. When used at 180-200 F its action was too violent. It was found that if the solution contained 20 per cent nitric acid and 80 per cent water, somewhat better results were obtained. Saturating the above with sodium nitrate gave apparently good results when used at 180-200 F.

The structure obtained by this etch is shown in Fig. 2. The modified Guthrie etch was unsatisfactory when the samples were examined at 1000 diameters. The centers of the crystals had been dissolved so that apparent double grain boundaries were revealed as shown in Fig. 3.

After considerable experimentation, it was found that if a strong oxidizing agent is added to the nitric acid, sodium nitrate, and water reagent, the grain boundaries are properly etched. Chromic acid was satisfactory but sometimes decomposed the nitric acid when heated and ruined the specimen. The best solution was the following:

20 cc of concentrated nitric acid
80 cc of saturated sodium nitrate in water
1-2 grams of potassium persulphate

Etching was done at 180-200 F for 10-30 sec.

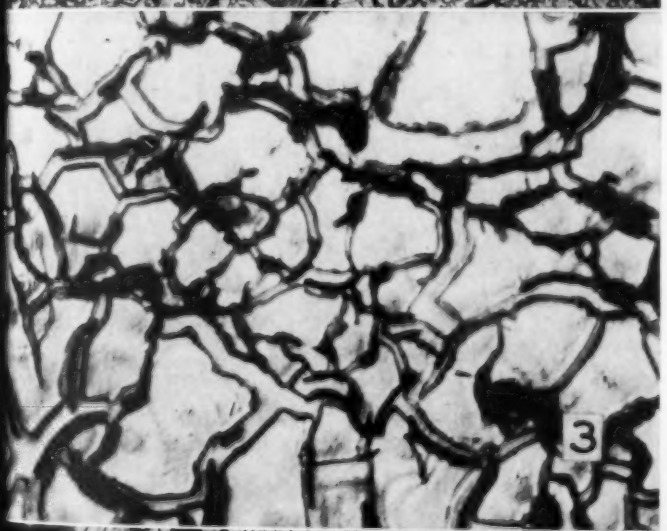
Figures 4 and 5 show the structures at 500 diameters. The matrix consists of but one phase, alpha silico-ferrite. The grains are apparently either equi-axed or elongated. In the latter case, it may well be that the actual grain may be large and equi-axed but, due to internal forces, etches into elongated, parallel sub-grains or twins.

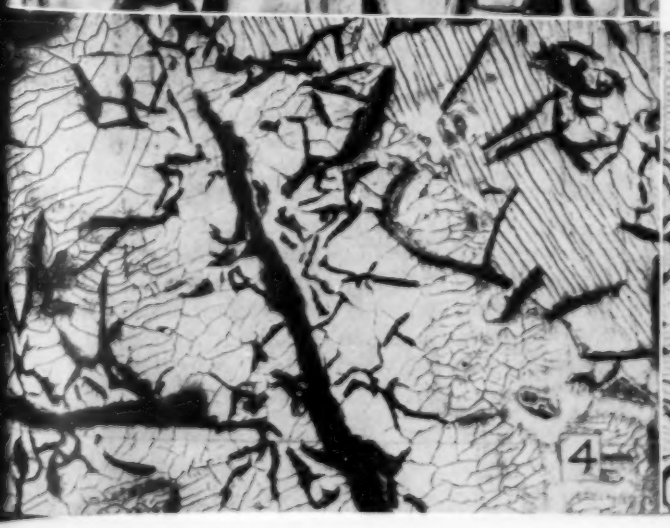
Some of the structures, Fig. 5 for example, show areas where the etch is interrupted. This is probably due to high phosphorus areas, in the center of which a small crystal of iron phosphide appears.

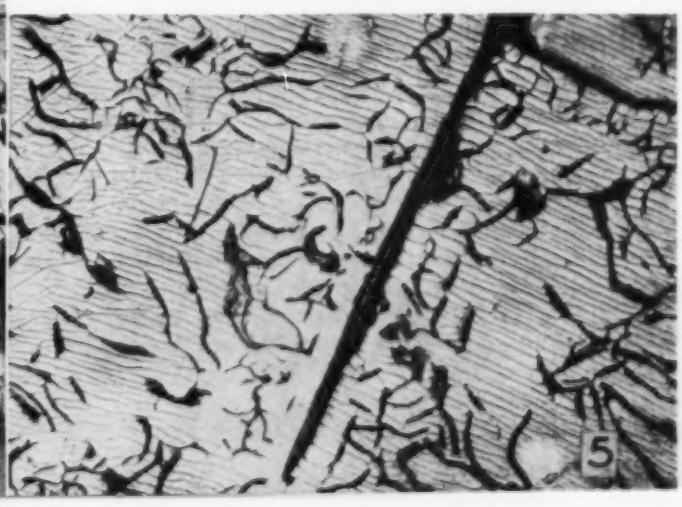
[1]Professor of Metallurgical Engineering, University of Michigan, Ann Arbor, Michigan.
[2]Chief Metallurgist, Hanna Furnace Corporation, Buffalo, N. Y.











X-ray Diffraction

Polished specimens of four irons were placed in an X-ray diffraction machine. X-rays of moderately short wave-length were directed at the sample and the intensity of the reflected radiation was measured. Readings were taken as the specimen was rotated with respect to the X-ray beam. In each case the angle of maximum reflection was measured and the results are given in the following table:

Sample No.	*Angle of Max. Reflection*
1	57.87°
2	57.85°
3	57.80°
4	57.85°

These readings are identical, within the limits of error of the apparatus and show that the ferrite is the same in all samples of silvery pig iron studied.

Steadite. It was decided that a study of the amount and distribution of steadite or iron phosphide constituent might prove fruitful. Low silicon pig irons and steels can be treated to reveal phosphides by several methods: (1) heat tinting; (2) Stead's etch; and (3) an etch of long duration with nital.

Nital will not attack silvery iron and Stead's etch does not react satisfactorily because it contains hydrochloric acid. While an etch such as Stead's or Le Chatelier's is used successfully on ordinary steels, good results require careful technique and the appearance will vary from sample to sample. Examples of heat tinting are shown in an article by Carl Joseph.[3] The steadite appears white in a dark brackground. It is impossible to get a uniformly dark background and as may be seen from the illustrations, it is difficult to distinguish between the light phosphides and light spots in the background.

A new etch was developed in this investigation which deposits an adherent bluish-black film of metallic arsenic on the ferrite and leaves the phosphides white. This etch is prepared as follows:

Digest 0.5 gram of arsenous acid (As_2O_3) in 5 cc of concentrated nitric acid until copious fumes of nitric

Fig. 1—Silvery pig iron, X 100, unetched. Fig. 2—Silvery pig iron, X 100, etched in 20% HNO_3 80% saturated with $NaNO_3$ in water. Fig. 3—Silvery pig iron shown in Fig. 2 X 1000. Fig. 4—Silvery pig iron etched as recommended by authors. X 500. Fig. 5—Same as Fig. 4 showing probable high phosphorus areas.

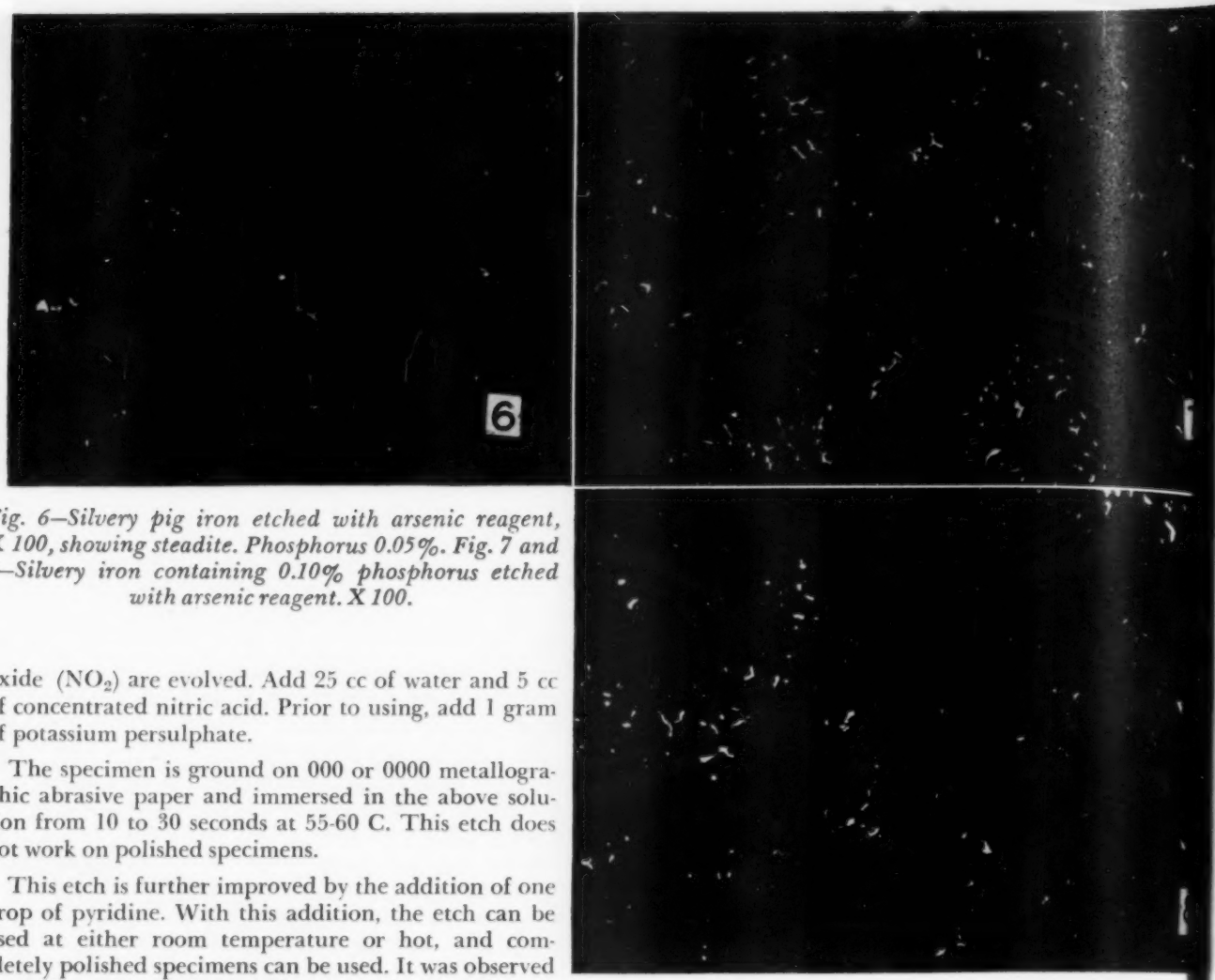


Fig. 6—Silvery pig iron etched with arsenic reagent, X 100, showing steadite. Phosphorus 0.05%. Fig. 7 and 8—Silvery iron containing 0.10% phosphorus etched with arsenic reagent. X 100.

oxide (NO_2) are evolved. Add 25 cc of water and 5 cc of concentrated nitric acid. Prior to using, add 1 gram of potassium persulphate.

The specimen is ground on 000 or 0000 metallographic abrasive paper and immersed in the above solution from 10 to 30 seconds at 55-60 C. This etch does not work on polished specimens.

This etch is further improved by the addition of one drop of pyridine. With this addition, the etch can be used at either room temperature or hot, and completely polished specimens can be used. It was observed that when the specimen is immersed in one of these arsenic etches, sometimes there is a heavy evolution of gas; at other times, there is little action. When gas has been evolved, the sample is pitted and corroded. It is properly etched only when there is a minimum evolution of gas. It would seem that there are two possible chemical reactions:

$$Fe + 2H^{+} \rightarrow Fe^{++} + H_2$$

or

$$3Fe + 2As^{+++} \rightarrow 3Fe^{++} + 2As$$

The first reaction removes iron, the evolved hydrogen escapes, and the remaining surface is attacked further. In the second reaction, the surface of the iron is coated with arsenic which protects it from further attack by the acid. The function of the potassium persulphate seems to be to react and oxidize the hydrogen as soon as it is formed. The pyridine likewise acts in a manner similar to that of a depolarizer. Both of these compounds evidently restrain the first reaction and favor the second, which is desirable.

Figures 6 through 11 show the steadite at 100 diameters. Sample A (Fig. 6) contains about 0.05 per cent phosphorus and therefore exhibits very few phosphides. The irons shown in Figs. 7 and 8 both contain approximately 0.10 per cent phosphorus and the amount of steadite is considerably greater. The distribution of steadite differs, however. One might conclude from the pictures that about 0.03 per cent phosphorus is in solid solution. The size of the particles varies to some extent and the location of the phosphides outlines the boundaries of the original eutectic grains.

This sample was reground on 000 metallographic paper and given a shorter etch. Figure 9 shows not only the phosphides as white crystals but also the areas of phosphorus-rich ferrite outlining the eutectic grain boundaries. The phosphides occur in the center of these phosphorus-rich areas.

To further check the accuracy of this etching medium, a sample of 2 per cent silicon pig iron was prepared. This contained 0.193 per cent phosphorus and Fig. 10 shows larger amounts of steadite than in the silvery irons.

An arsenic-etched surface of a specimen of stove plate whose phosphorus content should be in the neighborhood of 0.7 per cent is shown in Fig. 11.

Conclusions

As a result of this study the following conclusions are drawn:

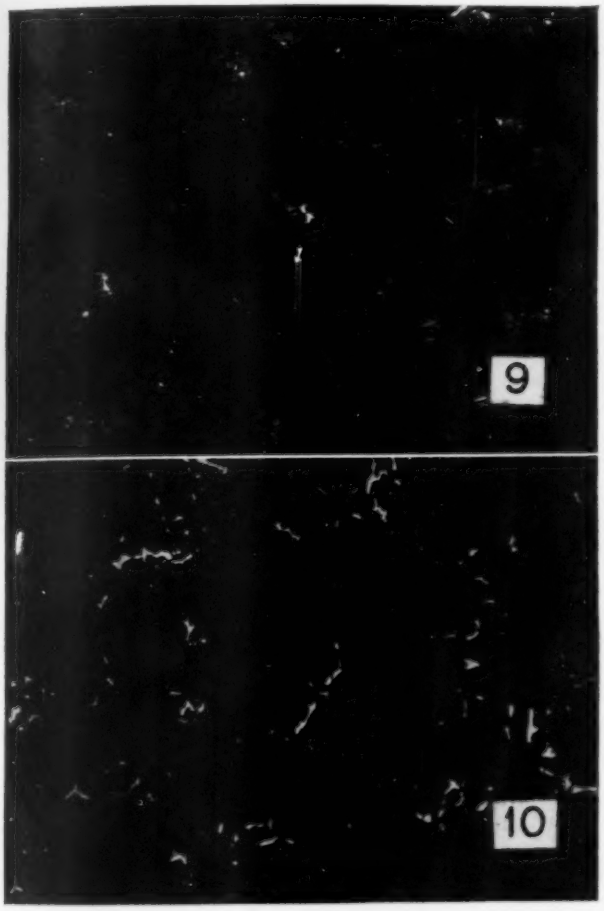
1. The graphite of silvery pig iron exists in long coarse flakes which represent the hypereutectic carbon and in small flakes which represent the eutectic carbon.

At 100 diameters the coarse graphite appears from 1½ to 3 in. long; the difference between samples does not seem appreciable. The fine graphite is generally a simple flake, ⅛ to ⅜-in. in length, with random orientation. In some locations the flake graphite is oriented along dendrites. The fine graphite in some portions is normal but in other portions it is exceedingly fine and tends to form a eutectiform pattern.

2. The matrix consists solely of alpha silico-ferrite. The grain shape varies from area to area. The crystals are either equi-axed or elongated. Inclusions are not numerous.

The size of the crystals varies somewhat but this could well be caused by differences in cooling rate in the mold.

Fig. 9—Silvery iron lightly etched with arsenic reagent to bring out phosphorus-rich areas as well as steadite. X 100, phosphorus 0.10%. Fig. 10—Steadite in iron containing 0.193% phosphorus and 2% silicon. Arsenic etch, X 100.



In some cases it is difficult to determine whether the long parallel grains are actually separate and distinct crystals or whether, due to internal strains, a large casting-grain is subdivided into smaller units or twins.

The chemical nature of the crystals is identical in all silvery irons studied as measured by X-ray diffraction methods.

3. Steadite or iron phosphide was found in all samples. The quantity of steadite can be predicted from the phosphorus content although the distribution and size of the individual steadite crystals may be affected by the rate of cooling.

In the course of this study two new etching reagents had to be developed: one, for etching the silico-ferrite, and the other, for easy and reproducible detection of steadite.

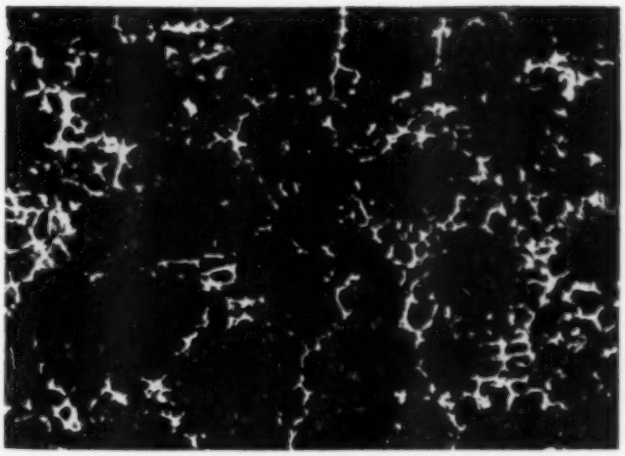


Fig. 11—Steadite in stove plate brought out with arsenic etch. X 100, phosphorus about 0.7%.

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DISCUSSION

Chairman: V. A. CROSBY, Climax Molybdenum Co., Detroit
Co-Chairman: H. N. MYERS, Perfect Circle Co., Hagerstown, Ind.

J. E. REHDER[1] *(written discussion):* This paper is of value in that it extends the scanty fund of information on the microstructure of high silicon pig iron. It is generally recognized that there is a heredity effect which acts through to the iron as cast in the gray iron or malleable iron foundry, and a more complete knowledge of the subject of the author's paper is useful.

A good deal of work has been done on the microstructure of iron-silicon alloys in the range 10 to 16 per cent silicon by J. E. Hurst in England. In a paper before the Iron and Steel Institute in 1944 he showed that the "barley-shell" markings found when using strong hydrofluoric acid etchants were a surface deposit phenomenon. In the February (1947) issue of the *Journal* of the Iron and Steel Institute (England), Hurst and Riley discuss the microstructure of 15 per cent silicon-iron as revealed by an etchant consisting of one part HF, one part HNO_3, and six parts water. It would be of interest if the structures revealed by the author's etchant would also be shown by that of Hurst and Riley.

J. T. EASH[2] *(written discussion):* The paper presented by the authors is of interest to the metallographist in showing the structure of high-silicon pig iron and in the development of a new etch to reveal steadite. I should like to ask if the authors have applied their new etchant to white irons and if it will distinguish between cementite and iron phosphide?

R. G. MCELWEE[3]: The author indicated that the phosphide in solution is 0.03 per cent under the lower concentrations. Do you think that is the function of the amount of phosphorus present? The author also indicated that with 0.70 per cent phosphorus, he found a difference in the color. That would lead me to suspect that with the higher phosphide the changing of the color might mean that more phosphorus is in solution at the higher concentrations of phosphorus.

Dr. Rote and Wood presented a paper and made the claim that the amount of phosphorus in solution was 0.12 per cent.

[1] Bureau of Mines, Ottawa, Canada
[2] International Nickel Co., Bayonne, N. J.
[3] Vanadium Corp. of America, Detroit

Does this refute their theory that we can dissolve 0.12 per cent phosphorus in iron?

DR. SCHNEIDEWIND: I thought that an estimate of 0.03 per cent in solution was fair because the 0.05 per cent of total phosphorus showed some steadite but the 0.10 per cent showed more than twice as much.

The difference in color referred to was not detected in the steadite but in the matrix where one can see a difference in color in the background or matrix metal immediately surrounding the steadite.

I do not believe that an estimate of 0.03 per cent refutes Drs. Rote and Wood's estimate of 0.12 per cent because we are talking of different things. He is talking of normal gray irons with maybe 1, 1½ to 2½ per cent max. silicon. The specimens studied all contain around 10 per cent silicon. They have ferrite matrixes and the gray irons have pearlite matrixes.

W. H. WHITE[4]: I have a question with regard to this sodium nitrate etching. Was the etching agent saturated with sodium nitrate at room temperature or at the temperature at which the etch was used, 180° to 200° F.?

DR. SCHNEIDEWIND: Our nitric acid mixture contained sodium nitrate in excess of saturation at high temperature near the boiling point. That will etch steel even though it is not an alcoholic solution like nital. We were able to etch steels and get clean pearlite and ferrite even though the etch is a high-strength water solution of nitric acid.

[4] Jackson Iron and Steel Co., Jackson, Ohio

SEVENTH ANNUAL REPORT ON INVESTIGATION OF PROPERTIES OF STEEL SANDS AT ELEVATED TEMPERATURES

D. C. Williams* and P. E. Kyle**

Introduction

THIS REPORT COVERS the work on the Sand Research Project sponsored by the Sand Division of the American Foundrymen's Association which has been completed during the year 1946 at Cornell University. It is divided into four parts as follows: Part 1 describing the Research Furnace, Part 2 reporting on a study of reproducibility of hot compressive strength test data, Part 3 on the determination of exposure time required to heat all parts of the test specimen to the same temperature, and Part 4 reporting on hot compressive strength vs. test temperature for several sand mixtures used in steel foundries.

The workers at Cornell wish to acknowledge the many helpful suggestions by the A.F.A. Sand Division Committee on Physical Properties of Steel Foundry Sands at Elevated Temperatures, and the encouragement and advice given by Dr. H. Ries, Chairman of the Sand Division.

PART 1

Research Furnace

The following descriptive material and discussion is a record of the design and performance of the research furnace for testing sand mixes at elevated temperatures.

ORIGINAL DESIGN REQUIREMENTS

When the need for this furnace was first realized, plans were made for the construction of one which would meet the following requirements:

1. The test specimen should be visible when being tested.
2. The furnace should have high heat capacity and input to minimize the drop in furnace temperature on the introduction of the test specimen.
3. The zone around the test specimen in the furnace should be of uniform temperature.
4. The furnace should be large enough to make transverse tests at elevated temperatures.
5. The hydraulic loading system should be designed to give varying rates of application of the load.

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DESCRIPTION OF FURNACE AND AUXILIARY EQUIPMENT

The research furnace is shown in the following:

Figure 1—Vertical section through the furnace showing specimen location and other details as indicated.

Figure 2—Horizontal section through the furnace showing location of globar heating elements.

Figure 3—General view of the furnace and some the auxiliary equipment.

Figure 4—View showing wall construction at the charging doors.

Figure 5—View of electrostatic condensers and controlling switchboard.

DESIGN OF FURNACE WALLS

A brief summary of the calculations used in the design of the furnace walls and in the determination of the power requirements and power input control characteristics is given below.

The two major sources of heat loss from the furnace are (1) radiation losses from the surface of the furnace shell which depend upon the thermal conductivity of the walls and, (2) loss in the terminals of the "AT" type Globar heating elements.

The design of the furnace walls makes use of Ohm's law for a thermal circuit which is expressed by the following equation:

$$H = \frac{T-t}{KA}$$

Where H = heat flow in $\frac{\text{BTU}}{(\text{sq. ft.})\ (\text{hr.})\ \frac{(\text{deg. F})}{\text{in.}}}$

T = temperature of hot face in deg. F.

t = temperature of cool face in deg. F.

R = "thermal resistance" of composite wall.

R is expressed by the following equation:

$$R = \frac{L}{KA}$$

Where L = length of thermal circuit in in.

K = conductivity in $\frac{\text{BTU}}{(\text{sq. ft.})\ (\text{hr.})\ \frac{(\text{deg. F.})}{\text{in.}}}$

A = area of thermal circuit in sq. ft.

R is the sum of the "thermal resistances" of the individual layers of the wall and was found to be 0.1579.

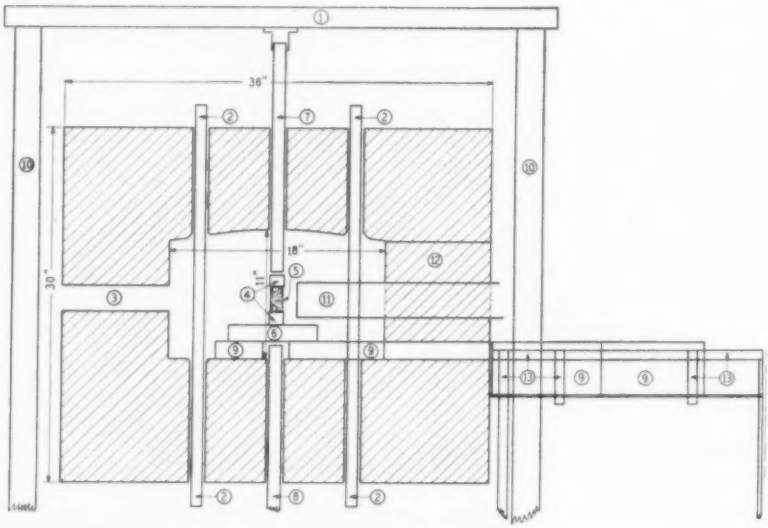


Fig. 1—Vertical section through research furnace. Specimen location is shown as well as refractory specimen support that operates as a skid. Parts are identified as follows: (1) rigid top cross-piece; (2) nine Globar heating elements; (3) observation port; (4) ceramic disks; (5) test specimen; (6) compression and transverse specimen support; (7) fixed upper loading post; (8) moving lower loading post; (9) ceramic skidway charging platform; (10) top cross-piece support; (11) radiation pyrometer target tube; (12) charging door (full height of furnace); (13) charging platform support.

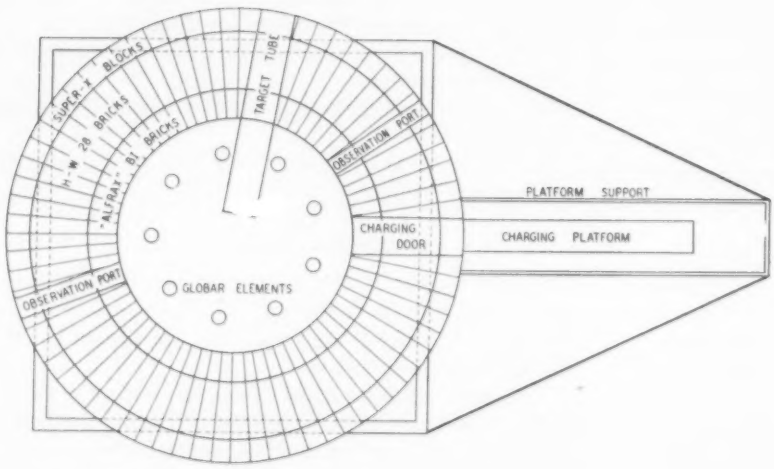


Fig. 2—Horizontal section through research furnace. Location of observation ports, charging door and target tube is shown.

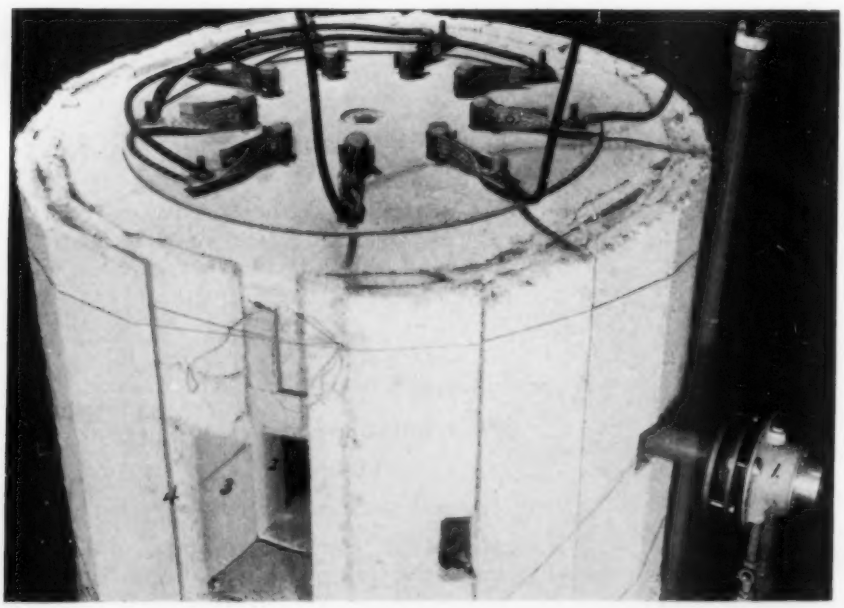


Fig. 4—Wall construction through charging door. Outside view showing furnace without sheet metal casting; (1) total radiation pyrometer with target tube for temperature control; (2) inner lining; (3) intermediate insulation; (4) outer insulation; (5) observation port; (6) electrical connections to heating elements.

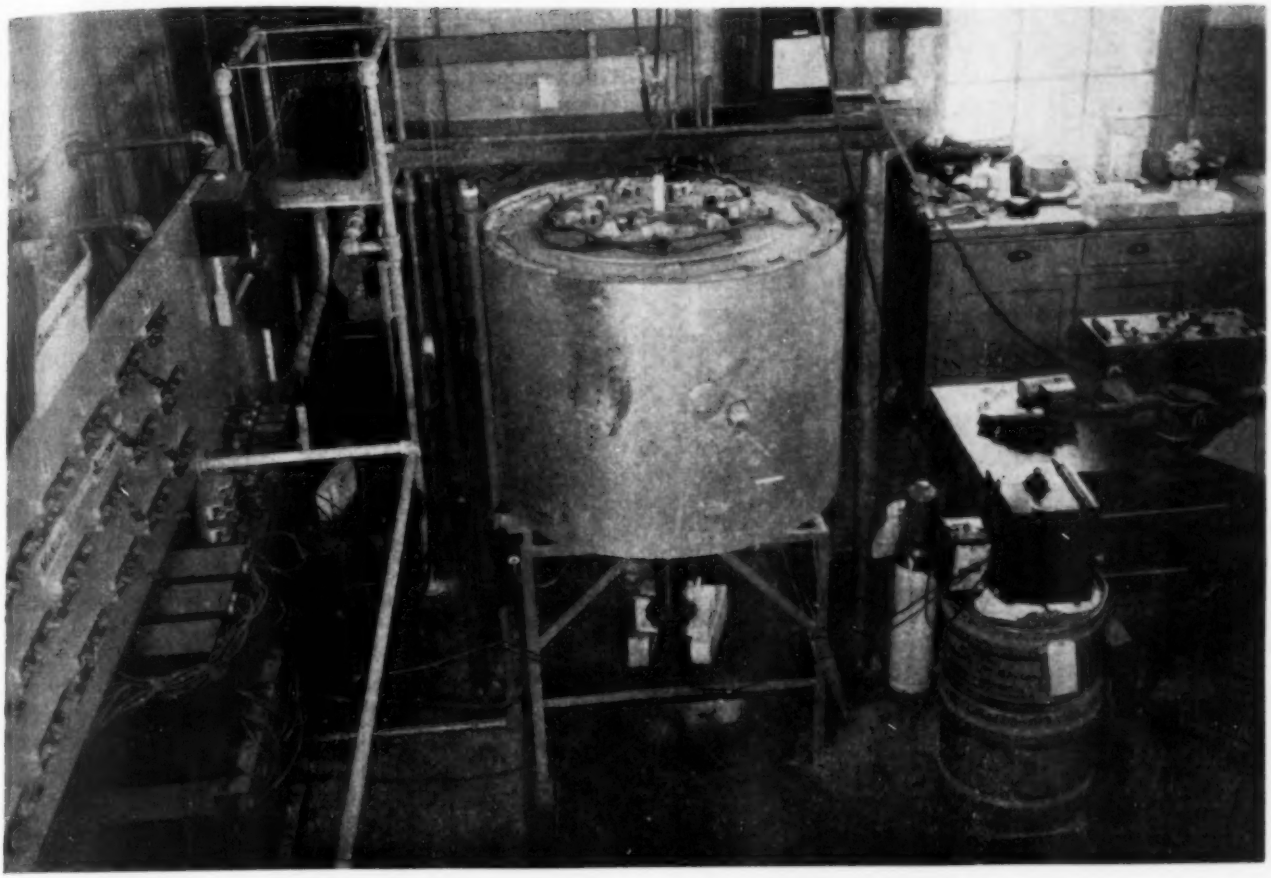
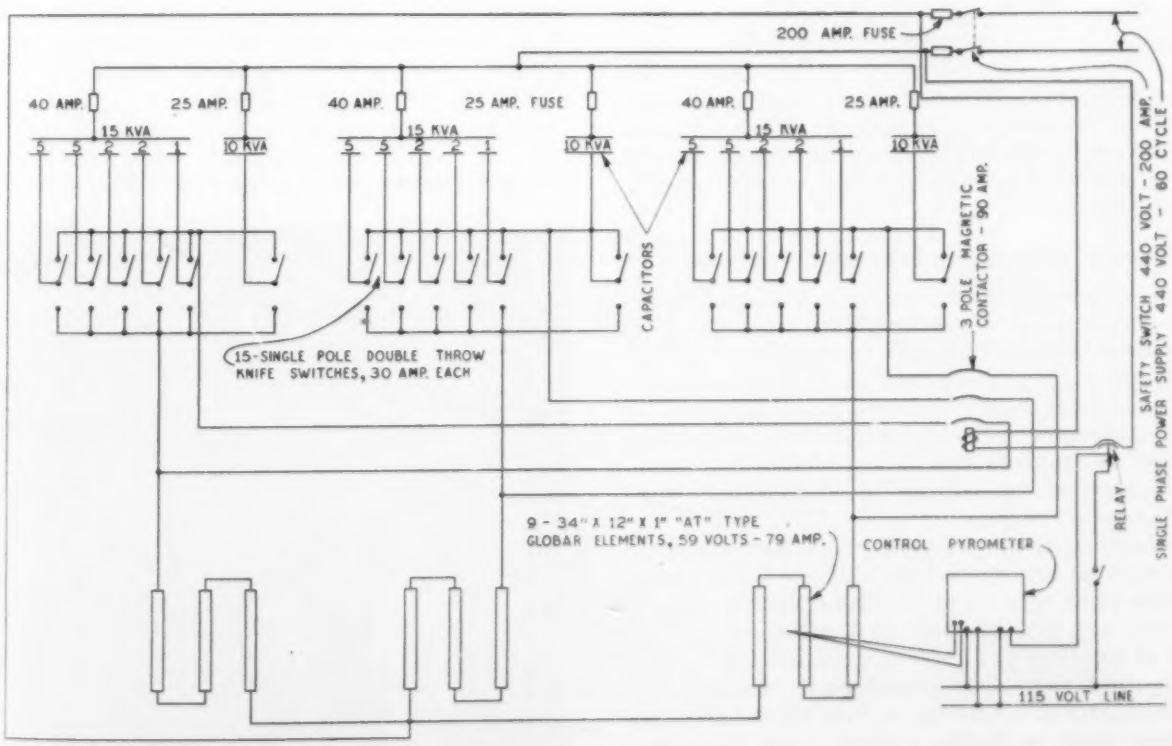


Fig. 3—General view of furnace, loading cylinder and controls.

Fig. 6—Wiring diagram for research furnace.



Substituting this value for R and using 3000° for T and 300° for t, H was found to be 17,200 B.T.U.

Knowing H and the R for each layer the temperature gradient through each layer of the wall is calculated. These calculated values are then compared with those gradients assumed originally and are as follows:

Assumed gradients		*Calculated gradients*
3000°F	Hot face	3000°F
200°F	Gradient through "Alfrax" BI	450°F
2800°F		2550°F
1000°F	Gradient through H-W 28	871°F
1800°F		1679°F
1500°F	Gradient through Superex	1385°F
300°F	Cool face	295°F

POWER REQUIREMENTS

The amount of power required to balance the heat loss by radiation was calculated to be 5.0 kw. The loss from 18 terminals at 150 watts per terminal is 2.7 kw. Other losses were based on experience and the total input required was set at 12 kw. The power input determined for two furnace temperature was as follows:

Furnace Temperature	*Duration of Test*	*Average Low Input*
2000°F	45 hr.	6.2 kw.
2500°F	40 hr.	8.3 kw.

POWER INPUT CONTROL

Instead of controlling the power input to the furnace with a step transformer, the procedure of using electrostatic condensers was adopted. Figure 6 is a wiring diagram for the research furnace. The capacity of the electrostatic condensers was determined from the following equation:

$$C = \frac{10^6}{2\pi f \sqrt{\frac{RE^2 - R^2}{W}}}$$

and was found to be 845 microfarads when $R = 0.646$ ohms, $E = 440$ volts, $F = 60$ cycles and $W = 12{,}000$ watts. Seventy-five kva. of capacitors was installed making available 937.5 microfarads to be divided between 3 parallel circuits.

DISCUSSION OF RESEARCH FURNACE PERFORMANCE

The *time to bring the furnace up to testing temperature* is a function of the temperature required. The safe heating time which has been used is 24 hr. to attain 1600°F and 36 hr. to reach 2500°F. With greater heat input these times could be reduced if necessary.

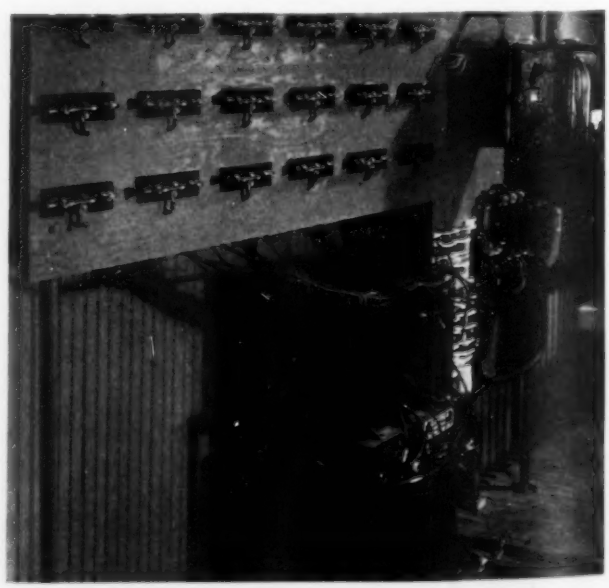
The *maximum operating temperature* which has been used to date is 2850°F. It is doubtful if temperatures higher than 2950°F can be safely held for any extended period of time.

The *location of the temperature control devices* in the furnace has been given some study in order to pick a location which would cause the controller to indicate a temperature corresponding to the temperature attained at the center of the test specimen after it has been in the furnace sufficiently long to establish constant-temperature conditions at that point. Observations were made by placing a green 1⅛-in. x 2-in. test specimen of a 4 percent western bentonite mixture containing 5 percent moisture in the furnace. This specimen had a thermocouple junction at its center and the temperature controlling couple in the furnace was located at several points in the furnace chamber and at one location in one of the furnace ports. It appears that all locations satisfy the conditions wherein the temperature indicated by the controller corresponds to the constant temperature attained at the center of the specimen.

The measurement of the *temperature drop in the furnace* due to the introduction of the test specimen is somewhat complicated by the fact that such measurements are dependent on the mass of the controlling device and the distance between the device and the test specimen. It is therefore necessary in reporting such data to point out that the values reported are only indicated temperature drops shown by the particular controlling device when located a certain distance from the specimen. Typical observations made with the above test specimen at furnace temperatures of 1600°F, 2000°F and 2500°F showed that with the thermocouple junction (32 gage wire) located midway between the specimen and one of the heating elements, the indicated drop in temperature was about 75°F and the recovery time was between 8 and 10 min. At a location in one of the ports of the furnace the indicated temperature drop was about 250°F with a recovery time of 4 to 6 min.

In order to observe the performance of the furnace in heating test specimens, studies were made on the time required to heat the 1⅛-in. x 2-in. and 2-in. x 2-in. specimens. The arrangements of thermocouples and the procedure in making temperature readings are explained in Part 3 of this report. Test runs were made at 1600°F and 2000°F on both green and dried specimens of the 4 percent western bentonite, 4 percent bentonite and 10 percent silica flour, and 4 percent western bentonite and 1 percent corn flour mixes. The results of these tests and the corresponding heating times for the 1⅛-in. x 2-in. green specimens in the dilatometer are shown in Table I.

Fig. 5—Electrostatic condensers and switchboard.



The *loading system* permits the rate of load application to be varied by the use of a by-pass valve in the supply line of the loading piston. The maximum load which can be applied is approximately 1250 lb which gives 400 psi on a 2-in. x 2-in. specimen and 1260 psi on a 1⅛-in. x 2-in. specimen.

PART 2

Reproducibility of Hot Compressive Test Results in Dilatometer

In testing a material such as molding sand at elevated temperatures, there are many variables which can be carefully controlled and some others which, due to their nature, cannot be regulated with the same degree of accuracy. In obtaining the results which are reported below, the specimens were prepared in a routine manner similar to that used by foundry sand testing laboratories, and the tests were conducted in the 1944 Model dilatometer (Fig. 7), a type used by foundries.

SUMMARY OF TEST RESULTS

Statistical data on the frequency distribution of hot compressive strength values for four series of tests are given in Fig. 8. In this method of representation, the arithmetic mean $\overline{X}$ is determined as the average of all tests made on each mixture. The number of tests which showed hot compressive strength values within a range of 10 psi (the chosen cell boundary) are plotted at the mean value within that range. The percentage of all tests which showed values of hot compressive strength within ± 5 percent and ± 10 percent of the arithmetic mean values are calculated and shown above each of the plots.

The most obvious conclusions from Fig. 8 are that at the higher strength levels, there is a wider distribution of test results and the distribution about the arithmetic mean value is not symmetrical. For all mixtures at various strength levels, between 80 and 88 percent of all values are within ± 10 percent of the arithmetic mean, while only 47 to 56 percent of all values are within ± 5 percent of this mean. In expressing results in this manner, it should be noted, however, that a variation of ± 10 percent at a mean value of 320 psi indicates that the actual value of hot compressive strength may fall between 288 psi and 352 psi. The same variation of ± 10 percent at a mean strength of 126 psi, however, refers to a possible spread of test results from only 113 psi to 139 psi.

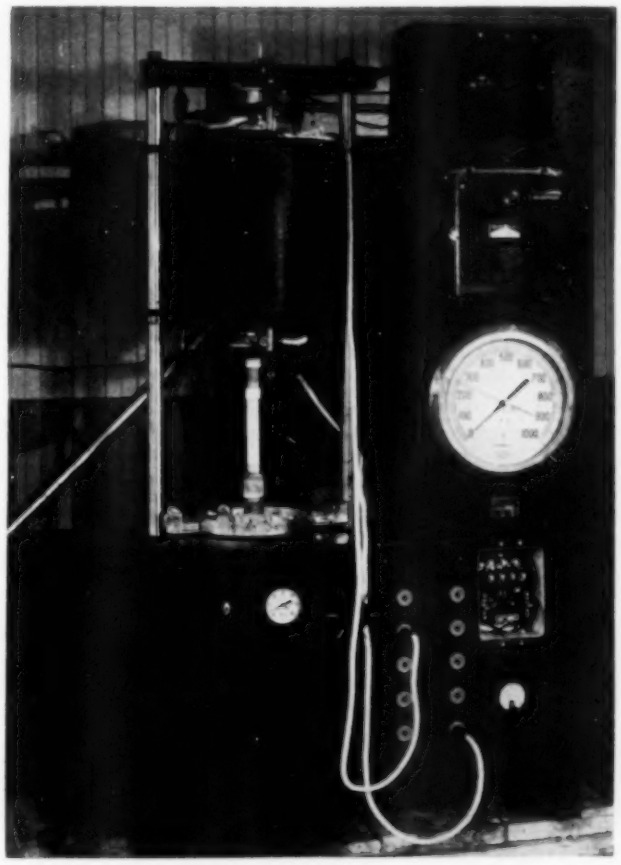


Fig. 7—Photo of 1944 dilatometer

There is some indication that other investigators find that by present test methods about 80 percent of all test values can be expected to fall within ± 10 percent of the arithmetic mean. A careful analysis should be

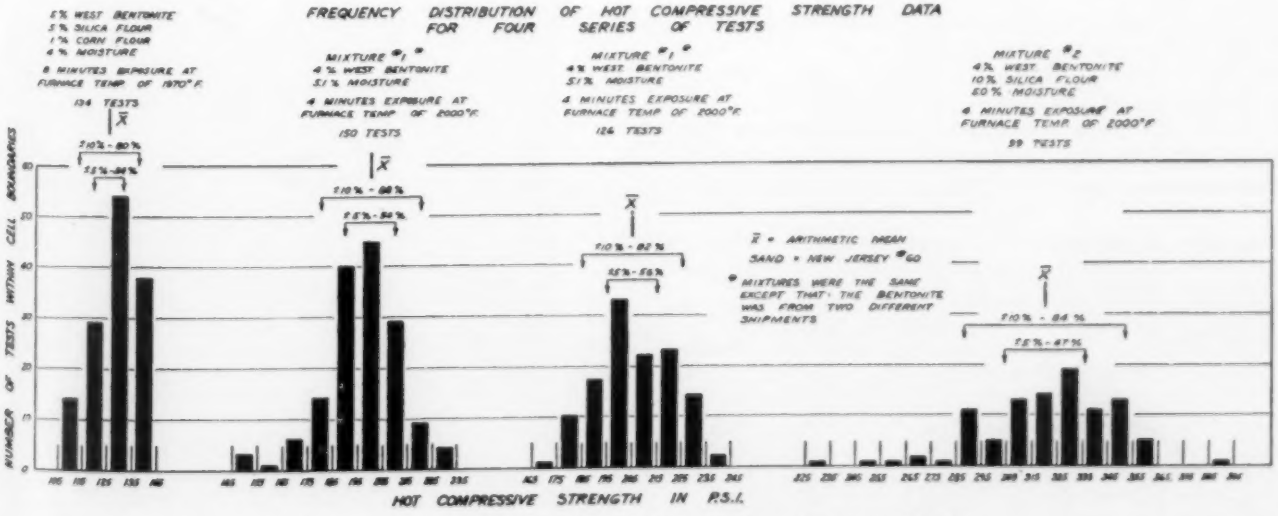


Fig. 8—Frequency distribution of hot compressive strength data.

made of this conclusion. In a general way, it might be said that perhaps a variation of ± 10 percent is satisfactory considering the nature of the material being tested. When it is necessary to study the effects of variables on the hot compressive strength at medium or high strength levels, extreme care should be used in the interpretation of results.

NUMBER OF TESTS REQUIRED

While the above analysis based on large numbers of tests is useful to evaluate the reproductibility of results, it is realized that such procedures would be impractical in sand control work in a foundry laboratory. The data used to plot Fig. 8 were examined in a slightly different manner to determine, if possible, the minimum number of tests required to give an average value which would compare favorably with the average value determined by many tests. This analysis of data given in Table I was made on only the low and high strength mixtures of Fig. 8.

After an elapsed time of two hours following the mixing of the bath, the data show that the average of a very small number of test values is nearly the same as the average for all of the tests made on that day. If testing is done immediately after mixing, as many as 8 to 10 casts may be required to get an average varying from the mean $\overline{X}$ by less than 10 percent.

Although it may not be necessary to conduct many tests to get a fair average, it should be noted that a few tests may not indicate the spread of values which might occur if more tests are made.

FACTORS AFFECTING REPRODUCIBILITY

Cracking of Specimen—In testing the mixtures used on the project, it is quite evident that the test specimen is cracked due to the heat shock when placed in the dilatometer. The hot compressive strength is therefore determined by testing a specimen which is severely cracked. Specimens heated in the dilatometer were seen to have developed surface cracks at temperatures of about 1200°F and higher. In order to study the nature of the cracking, it was necessary to cool the specimen to room temperature for examination. The cooling process increased the size of the surface cracks, but apparently did not cause new ones to form. At present the effect of cooling on interior cracking has not been studied so it will be assumed that the examination of the specimen after cooling to room temperature reveals the effects of shock heating primarily.

Figure 9 shows a series of sand specimens which have been treated in the dilatometer in the manner employed in the hot compression test, removed from the furnace, cooled to room temperature, and then examined by removing a portion of the loose outer sand layer to disclose the interior conditions. One set of specimens (Fig. 9a) consist of an hour-glass shaped core surrounded by what might be called a rind. At the midsection of the core transverse cracks are present. These transverse cracks are seldom parallel to the ends of the test specimen and may be found in widely different locations along the axis of the specimen. The vertical surface cracks occur at random, but the horizontal surface cracks occur at point where rind is thinnest.

Cereal bonded mixtures tend to eliminate surface cracking, but even though a specimen from this type of mixture may not show surface cracks on cooling to room temperature after heat shock, the rind and core formation is still present. This has been determined by means of X-ray examination as in Fig. 10 and confirmed by subsequent dissection.

The hour-glass shaped core in all probability results from the pressure bulbs formed in the ramming procedure used in preparing the specimen. Examination of specimens of certain other mixtures subjected to the

TABLE 1—ANALYSIS OF REPRODUCIBILITY DATA

	5% West Bentonite 5% Silica Flour 1% Corn Flour 4% Moisture				4% West Bentonite 10% Silica Flour 5% Moisture			
Daily Groups of Tests	1	2			1	2		
Number of Tests	91	43	Total 134		43	56	Total 99	
Arithmetic Mean Value $\overline{X}$ of Hot Compressive Strength psi	129	120	Avg. 126		327	314	320	
% of Tests within ± 10% of $\overline{X}$	—	—	80%		—	—	84%	
% of Tests within ± 5% of $\overline{X}$	—	—	54%		—	—	47%	
Tests started immediately after mixing			Deviation from $\overline{X}$				Deviation from $\overline{X}$	
			1	2			1	2
Avg. of first 3 tests daily	137	136	±6.2%	±13.3%	278	248	—15%	—21%
Avg. of first 5 tests daily	135	137	±4.7%	±14.2%	292	258	—11%	—18%
Avg. of first 8 tests daily	134	131	±3.9%	± 9.2%	303	276	—75%	—12%
Avg. of first 10 tests daily	132	130	±2.3%	± 8.4%	311	282	—4.9%	—10%
Avg. of first 20 tests daily	130	125	±0.8%	± 4.2%	323	299	—1.2%	—4.8%
Tests made starting two hours after mixing			Deviation from $\overline{X}$				Deviation from $\overline{X}$	
			1	2			1	2
Avg. of first 3 tests daily	128	126	—0.8%	+5%	328	327	+0.3%	+4.1%
Avg. of first 5 tests daily	127	120	—1.5%	0	327	324	0%	+3.2%
Avg. of first 8 tests daily	125	119	—3.1%	—0.8%	330	328	+0.9%	+4.5%
Avg. of first 10 tests daily	126	119	—2.3%	—0.8%	331	323	+1%	+2.9%
Avg. of first 20 tests daily	129	119	0	—0.8%	331	322	+1%	+2.5%

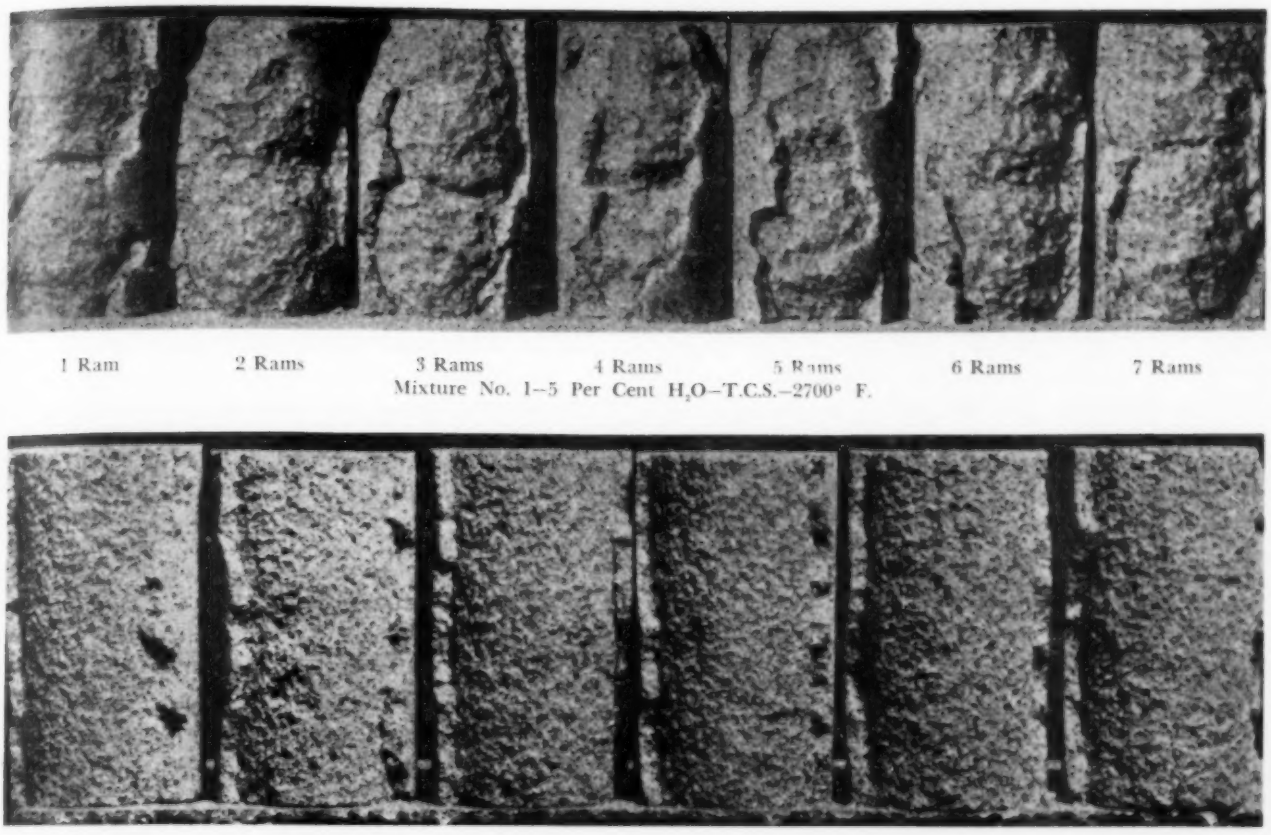


Fig. 9—Sand specimens subjected to heat shock.

same heat shock conditions show no hour-glass formation (Fig. 9b). The explanation of these two conditions has not yet been obtained.

Initial trials using test specimens prepared by "slinging" indicate this molding method produces cylindrical rather than hour-glass shaped cores.

If all of the material forming the rind of the specimen expands rapidly due to shock heating, it is quite likely that the expansion of that portion of the rind at the mid-section of the hour glass core would be sufficient to cause transverse fracture in the core at that location.

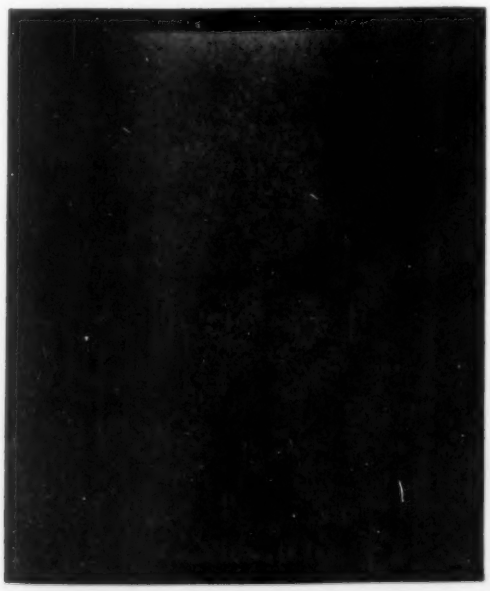


Fig. 10—Radiograph of sand specimen after heat shock.

Test Temperature—In the course of sand testing at elevated temperatures, it will be necessary that complete data be obtained for various mixtures over a wide range of temperatures. Within these temperature ranges, there are regions where the strength-temperature gradients are steep. Neglect of operating details in conducting tests at such temperatures can lead to variations. Part 4 of this report contains data on several mixtures tested over a wide range of temperatures and illustrates the point under discussion. It should be noted, however, that the maximum strengths occur at different temperatures for various mixtures. Table 2 lists four of the sands in decreasing order of peak strength. If all sands were tested at only one temperature for purposes of comparison, it is readily seen that the test would not be conclusive. The abrupt changes in strength-temperature relations also points to the need for testing at close temperature intervals.

TABLE 2—COMPARISON OF HOT COMPRESSIVE STRENGTH DATA

Mixture	Max. H.C.S. psi	Temp. at Max. H.C.S.	H.C.S. at 1850°F psi	H.C.S. at 2000°F psi
4% Western Bentonite Mix No. 1	865	1850°F	865	125
10% Fire Clay Mix No 7	470	2000°F	150	470
4% Illite	375	1850°F	375	100
4% Fire Clay	170	2000°F	50	170

Exposure Time—In considering reproductibility of test results, it seems logical that equilibrium conditions in a test specimen would be the easiest conditions to reproduce consistently. Part 3 of this report deals with exposure times for shock heating of test specimens. From the plots included there, it can be seen that for short exposure times, the specimen temperature can be very non-uniform. If short exposure times are chosen when testing, it will be extremely difficult to reproduce the same non-uniform temperature condition in the specimen, particularly where the gradient is steep such as at the 2-min. and 4-min. exposure times.

DISCUSSION OF REPRODUCIBILITY

The testing of specimens which are badly cracked is perhaps the cause of most of the troubles in reproducing test results within close tolerances. It is possible that some better method of preparing the specimen can be devised. By vibrating the sand in the specimen tube for 15 sec. before ramming, some improvement is noted. (Vibration is at 3700 cycles per minute).

To overcome the effect of rapid surface expansion relative to center expansion, it would undoubtedly be necessary to heat the specimen more slowly by placing a cold specimen in a cold furnace and bringing both to temperature uniformly or else heating the center of the specimen as rapidly as the surface by a radically different method; possibly by dielectric heating. The former method would require hours and the constituents in the sand mixture would undoubtedly undergo changes which would not commonly occur in actual foundry practice. The latter method has not been explored.

To minimize the effects of other test variables, it seems advisable to use exposure times sufficient to get uniform constant temperatures throughout the specimen and to test enough specimens to get a good average, particularly in regions of steep strength gradients.

PART 3

Exposure Time to Heat Test Specimens to Constant Temperature

Studies were made to determine the time required to bring the center of the standard test specimens up to the temperature of the outside of the specimen, both being at the specified test temperature within the limits of the control and temperature measuring equipment being used.

MEASUREMENT OF TEMPERATURES

To measure the specimen temperatures, it is necessary to mount the thermocouples as shown in Fig. 11. Due to the small size of the test specimens, it was necessary to effect a compromise between complete protection and no protection for the hot junctions imbedded in the test specimens. Therefore, approximately 6 in. of each wire (platinum vs. platinum-10 percent rhodium-32 gage) was tightly wound on a clay cylinder ($\frac{1}{16}$ in. diameter x $\frac{1}{4}$ in. long) with the junction between the windings and separated from them. When this assembly was placed at the center of the test specimen, the axis of the clay cylinder was parallel to the axis of the test specimen and the junction was on the specimen axis midway between the specimen-ends. When the assembly was placed at the surface, the axis of the clay cylinder was parallel to the specimen ends and could "see" the furnace chamber. Temperature readings were taken with only one thermocouple in the specimen, thus two runs and two specimens were required for each set of heating curves.

Test specimens were in either the green or dried condition when introduced into the furnace. The emf developed by the thermocouple was determined by a potentiometer that was equipped with a cold junction, compensating feature. All millivolt readings were converted to the nearest 5 degrees interval in °F.

DETERMINATION OF EXPOSURE TIME

In this report studies are reported showing the time required to bring a test specimen up to a constant uniform temperature in the dilatometer and the research furnace. The times recorded were obtained by plotting the temperature vs. time relations for the centers and surfaces of the specimens and taking from this plot what was considered the correct value. The specimens behaved somewhat differently in some tests than in others and it became necessary to use personal judgment in establishing the proper time values.

In order to explain how this was accomplished, Fig. 12 is included to show four of the typical plots encountered. Figure 12A shows the common condition where the relations are straightforward and there is little doubt that from the plot, a time of 12 min. is the time when the temperature is essentially constant throughout the specimen. Fig. 12B is typical of the case where

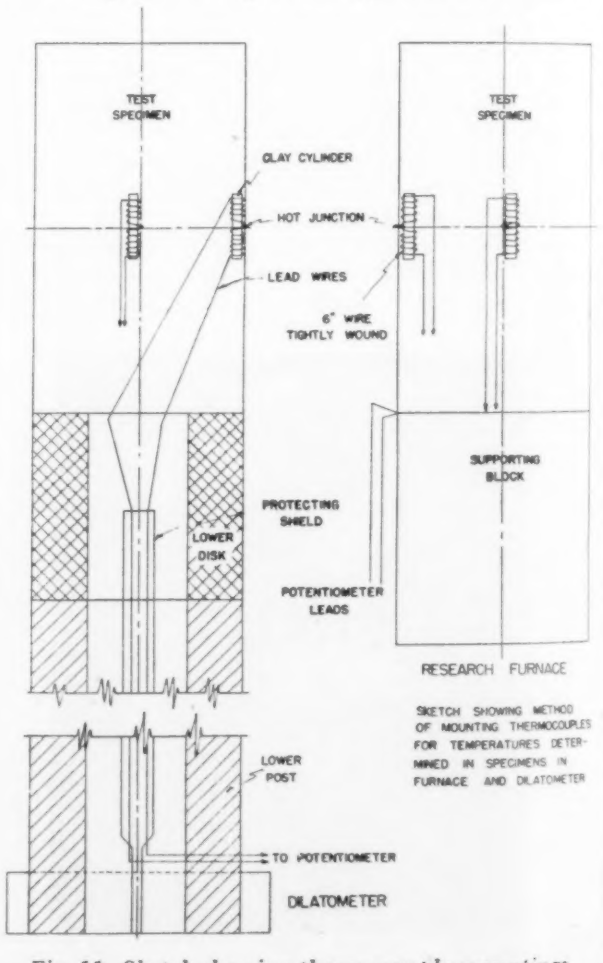


Fig. 11—Sketch showing thermocouple mountings.

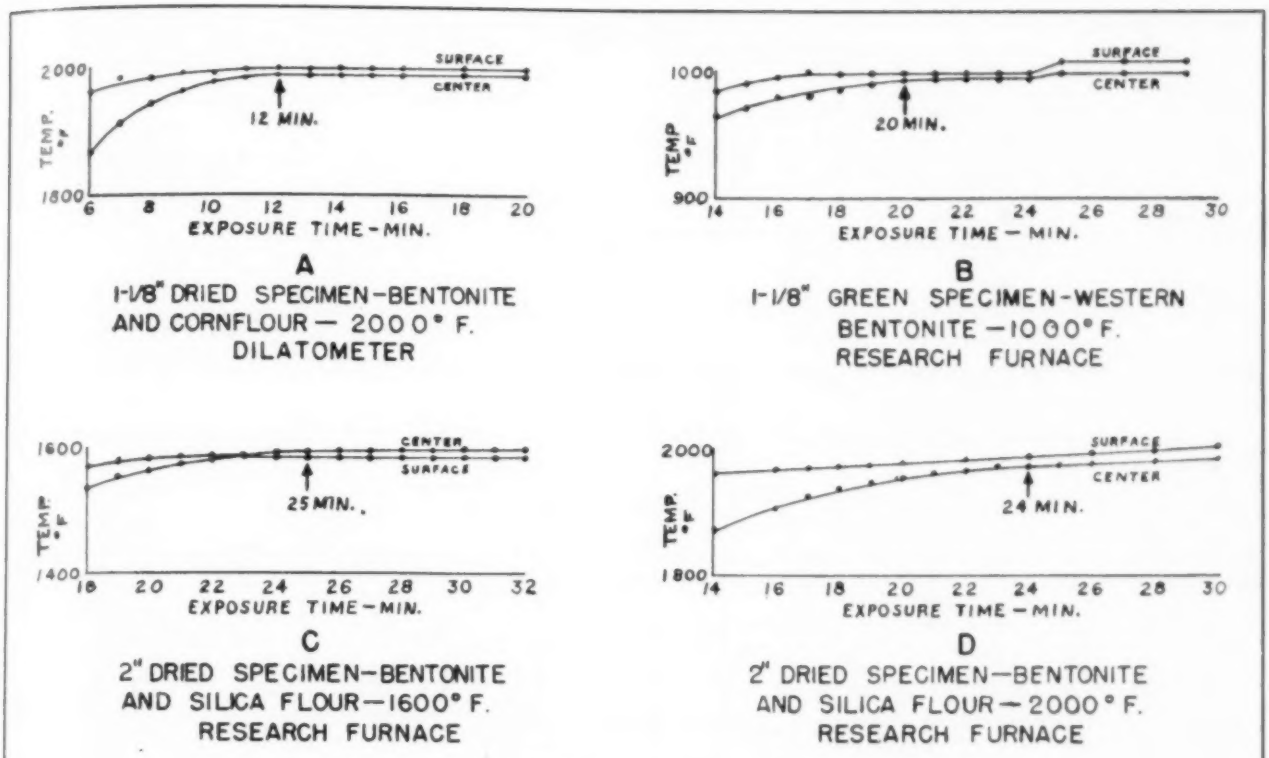


Fig. 12—Typical temperature-time relations in shock heating.

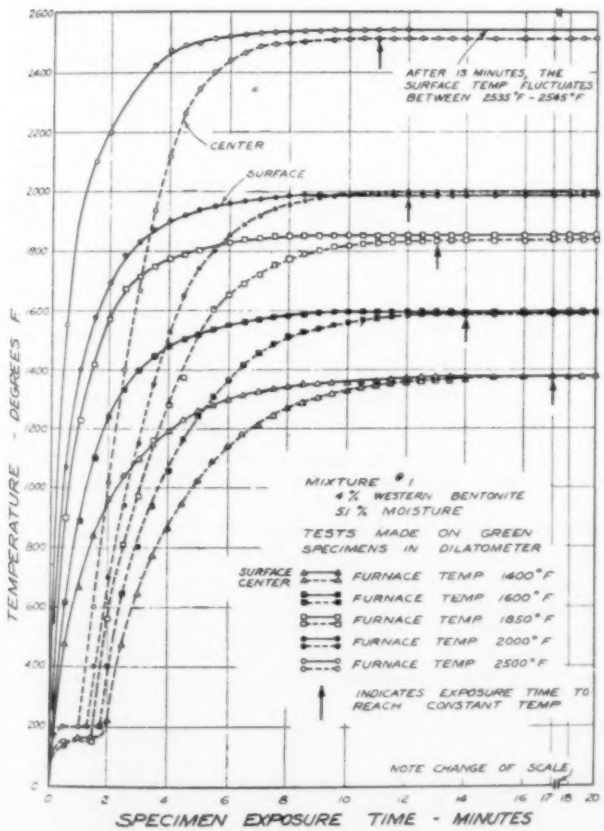


Fig. 13—Temperature at center and surface of 1⅛-in. diameter test specimens during shock heating in dilatometer.

the readings continue constant for some time and then later, due to variations in the heating and the temperature recording, the values change usually to a slightly higher level. Under these conditions it was felt that 20 min. was the proper value to record as the point where constant conditions were reached. In other cases it was impossible to get the furnace adjusted exactly for the run to get the curve for the surface condition and the following run to get the data for center conditions. In such a case as in Figure 12C it is noted that the maximum temperatures attained were slightly different and the curves crossed each other. In this case the value of 25 min. was thought to be the proper one to record. In Fig. 12D is shown a typical case where the temperatures approach the temperature of the furnace and then drift very slowly upward. This type of plot was the most difficult to interpret and the values of 24 min. shown is the best value which could be chosen under the circumstances.

Summary of Test Results

Using the above described methods of measuring temperature and determining exposure time, the results shown in Fig. 13, 14, 15 and 16 were obtained.

The exposure times taken from these plots are summarized in Table 3 and Fig. 17.

The detailed plots are given in Fig. 13-16 because it is felt that as we find out more about the conditions within a sand mold during pouring and solidification of the casting, these actual data based on time and temperature will be extremely useful. They also indicate the wide variation in temperature, surface to center, of specimens heated for short times for hot compression testing.

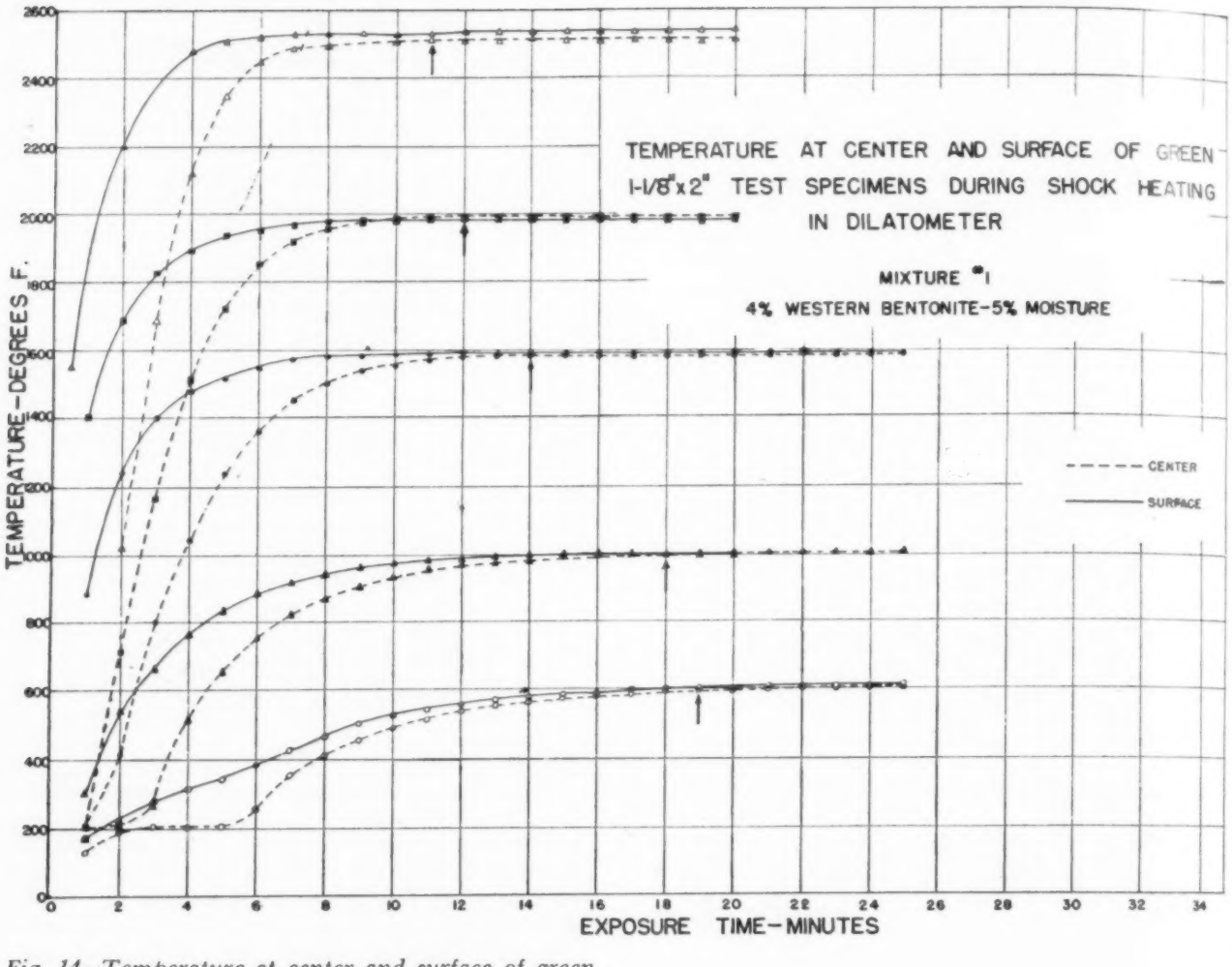
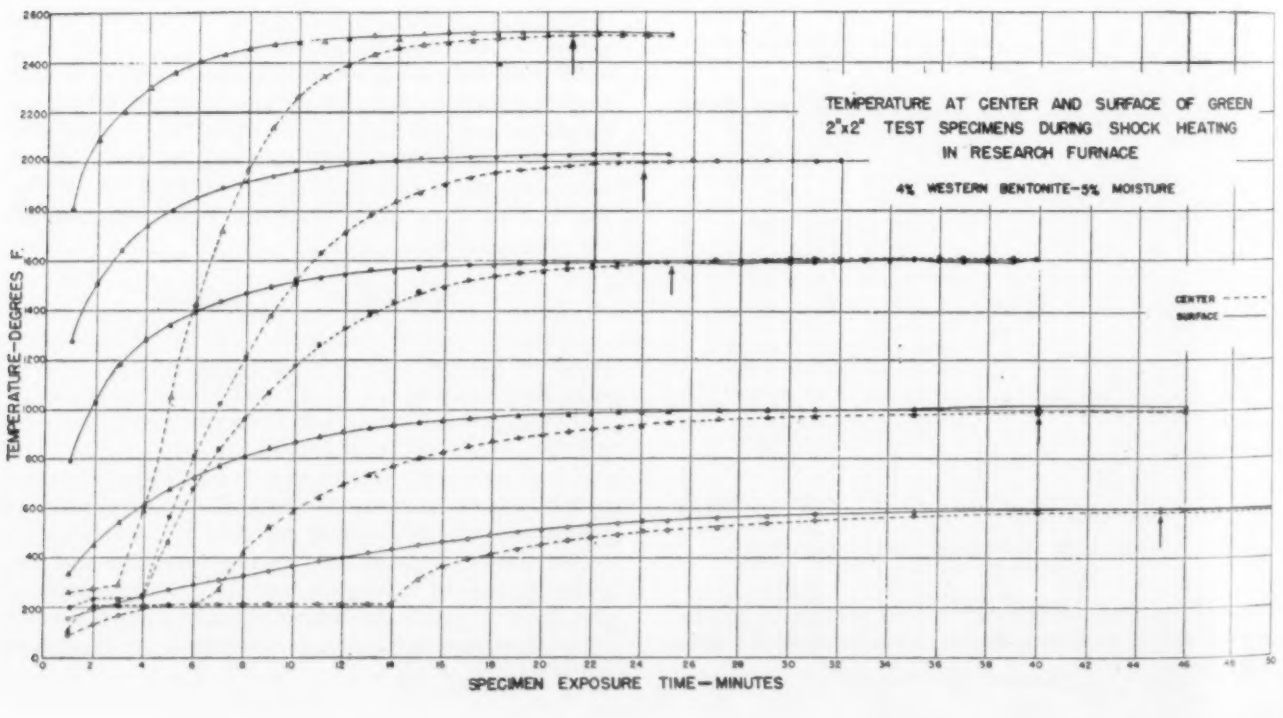


Fig. 14—Temperature at center and surface of green 1⅛-in. test specimens during shock heating in dilatometer.

Fig. 16—Temperature at center and surface of green 2-in. test specimens during shock heating in research furnace.



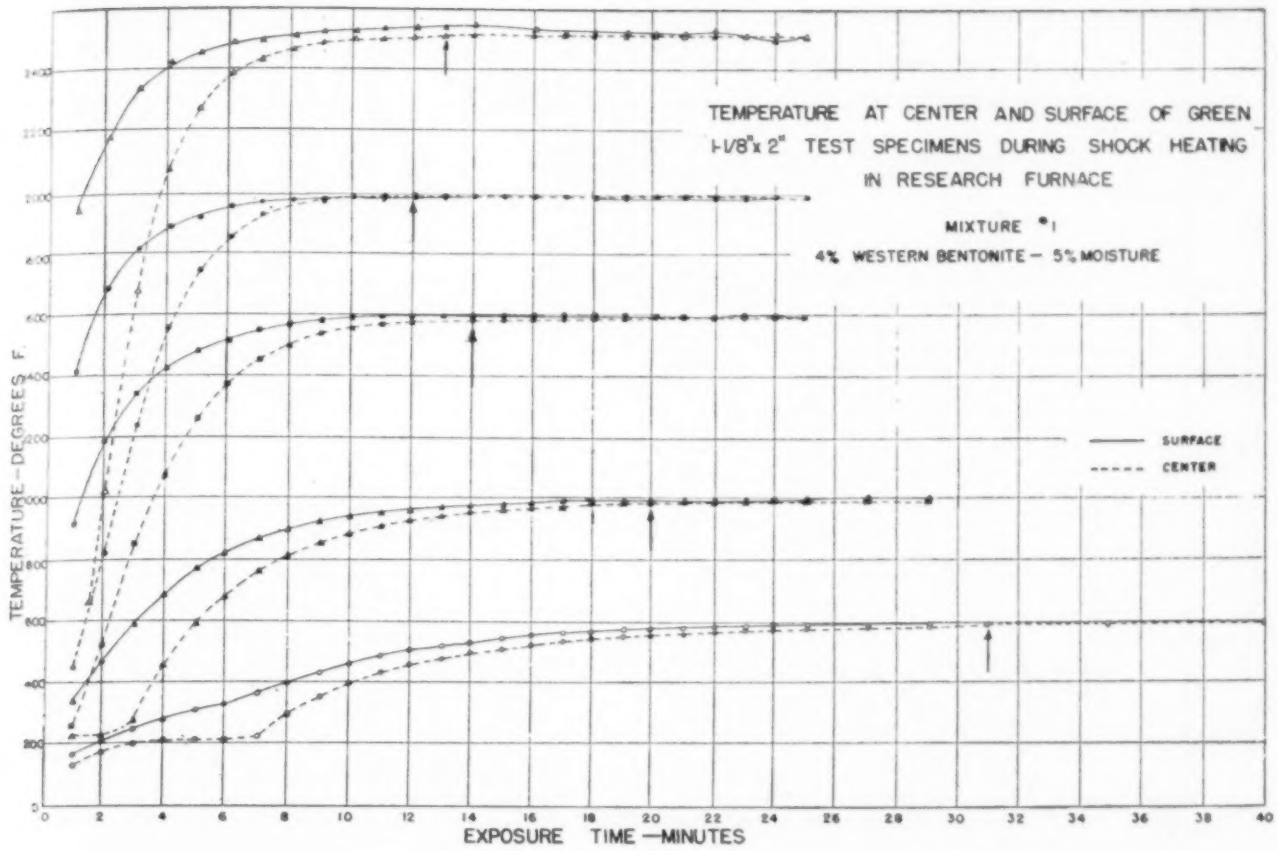


Fig. 15—Temperature at center and surface of green 1⅛-in. test specimens during shock heating in research furnace.

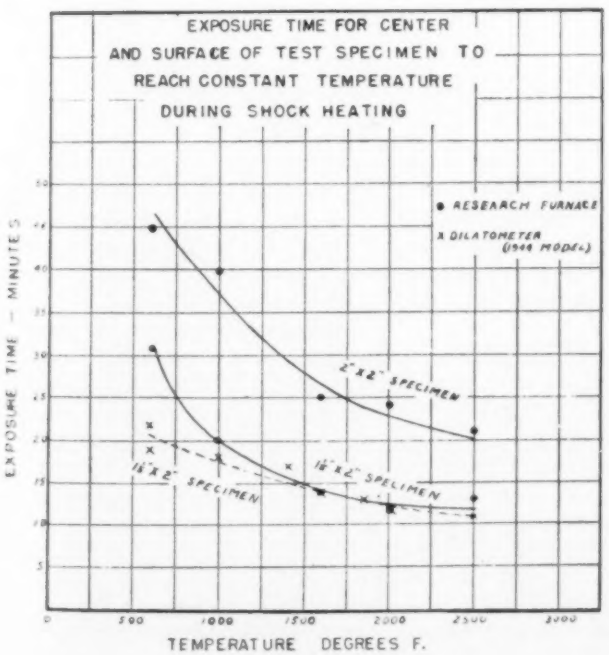


Fig. 17—Exposure time for center and surface of test specimen to reach constant temperature during shock heating.

TABLE 3—EXPOSURE TIME TO REACH CONSTANT TEMPERATURE
4% Western Bentonite—5% Moisture

Furnace Temperature °F	1⅛-in. x 2-in. Specimens		2-in. x 2-in. Specimens
	In Dilatometer, Min.	In Research Furnace, Min.	In Research Furnace, Min.
600	19		
	22	31	45
1000	18	20	40
1600	14	14	25
2000	12	12	24
2500	11	13	21

Table 3 and Fig. 17 summarize the data in this part of the report and clearly show the longer time required to heat the larger specimen. Two tests were made at 600°F in the dilatometer to verify the results which indicate that a somewhat shorter time is required for heating than in the research furnace.

PART 4

Hot Compressive Strength vs. Test Temperature

In order to obtain complete test data on hot compressive strengths of molding sand mixes, it was felt desirable that tests be conducted over a wide temperature range which would include all maximum temperatures to which sand is subjected in various parts of a mold during pouring. A series of such tests was planned and Fig. 18, 19, and 20 represent the first three sets of results.

Although it is possible to evaluate in a general way the effect of corn flour by comparing Fig. 18 and 19 and the effect of moisture by comparing Fig. 18 and 20,

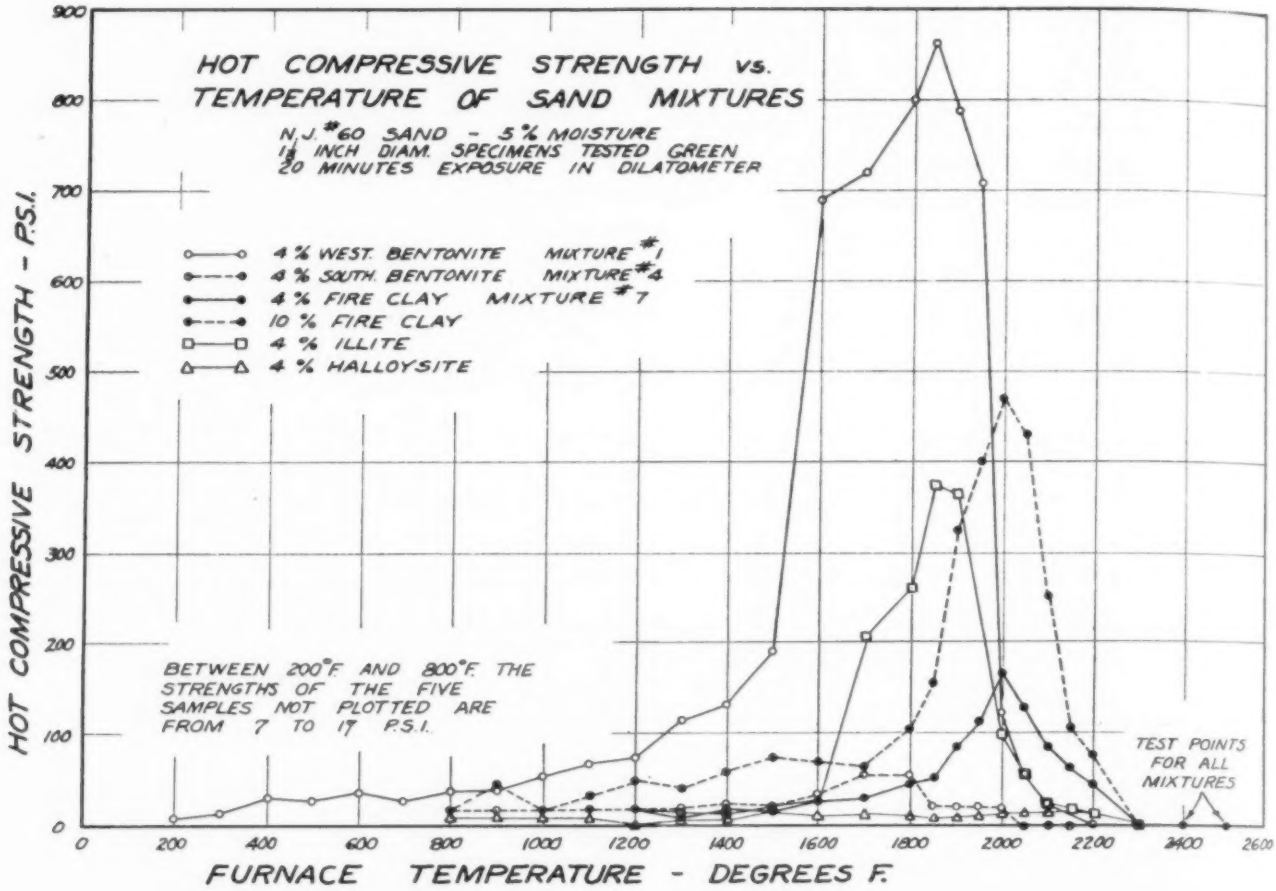


Fig. 18—Hot compressive strength vs. temperature of sand mixtures at 5 per cent moisture.

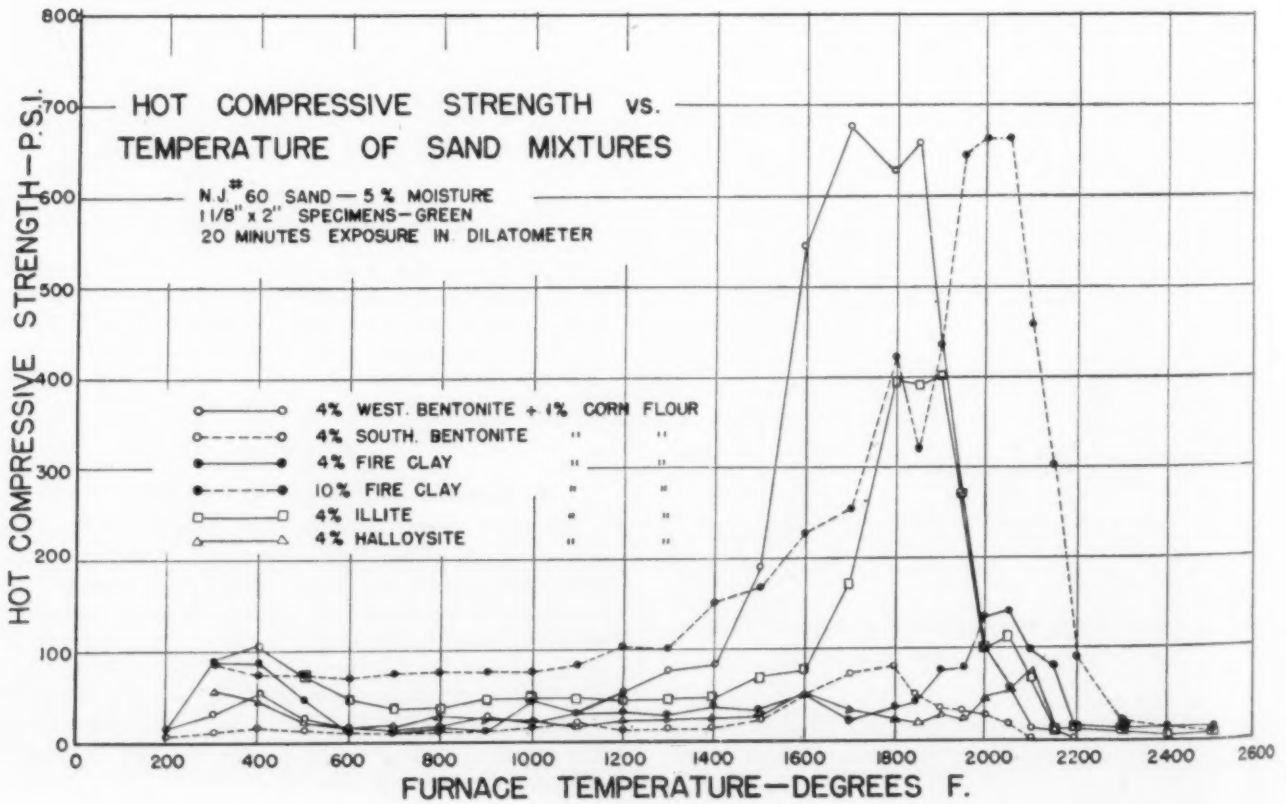


Fig. 19—Hot compressive strength vs. temperature of sand mixtures at 5 percent moisture with 1 percent corn flour.

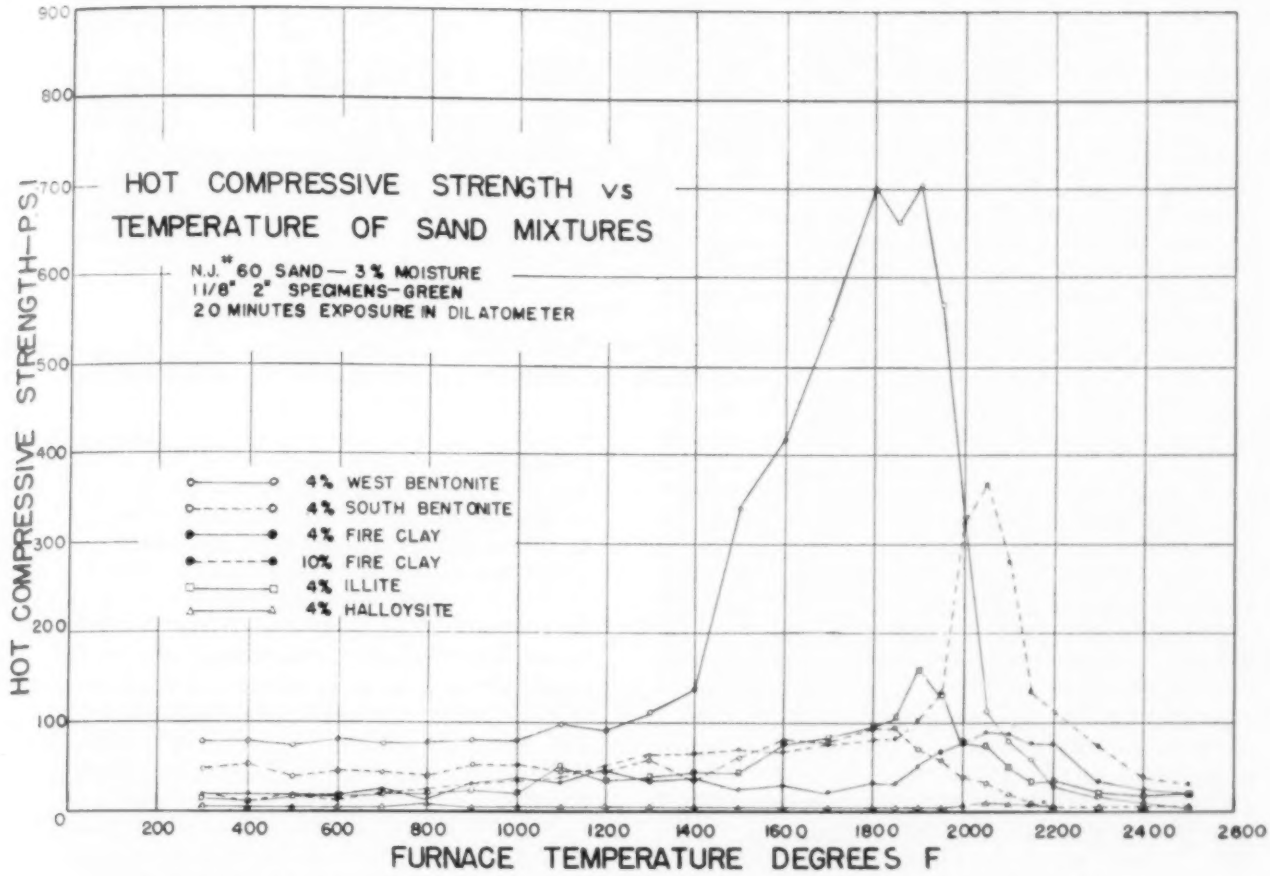


Fig. 20—Hot compressive strength vs. temperature of sand mixtures at 3 percent moisture.

any real conclusions as to the effects of these and other variables cannot be made until further data are available. These additional data are now being obtained and will be reported in subsequent reports.

Summary and Conclusions

The furnace which has been designed and constructed as a part of this project is working satisfactorily. Specimens can be heated uniformly and can be seen throughout the test at elevated temperatures. The heating chamber is large enough to permit carrying out tests on transversely loaded specimens and other special specimens as the program requires. The maximum temperature which can be used in this furnace for extended test periods is 2850°F.

Based on many tests of several sand mixes at elevated temperatures, statistical studies indicate that with present test methods, it is reasonable to expect that hot compressive strength values will vary within approximately ± 10 percent of the arithmetic mean. The factors causing such variations are discussed in detail, but most evidence points to the preparation of the specimen as the most critical. Until better techniques can be developed, it seems advisable to exercise great care in testing, heat until the specimen is heated to a constant test temperature, and test a sufficient number of specimens to obtain a good average.

Tests were conducted to determine the time necessary to heat the 1⅛-in. diameter and 2-in. diameter test specimens to a constant temperature throughout. In the dilatometer the time for heating the 1⅛ in. specimens varies from about 21 min. at 600°F to 11 min. at 2500°F while in the research furnace the times are 31 min. and 13 min. respectively. A smooth curve averaging the data shows that the heating times for both furnaces are essentially the same except at low temperatures. The 2-in. diameter specimens could be studied only in the research furnace and the time required was found to be 45 min. at 600°F and 21 min. at 2500°F. It appears, therefore, that the heating time of 20 min. being used on the hot compressive strength (H.C.S.) tests on the project is satisfactory.

Due to the fact that the peak strengths of various sand mixtures do not occur at the same temperature, it is necessary to obtain complete H.C.S. data over a wide temperature range in order to compare their strengths. It was decided, therefore, to obtain H.C.S. values for several mixtures over as wide a range of temperatures as possible. Such data from 200°F to 2500°F are presented in the report for 4 percent western bentonite, 4 percent southern bentonite, 4 percent fire clay, 10 percent fire clay, 4 percent illite, and 4 percent halloysite mixtures. One set of results is on all mixtures at 5 percent moisture, and another at 3 percent moisture. A third set of data is for all mixtures at 5 percent moisture with an addition of 1 percent corn flour to each. Further tests are in progress and conclusions can best be made when these results are obtainable. The 4 percent western bentonite and the 10 percent fire clay mixtures show superior strength properties at elevated temperatures, the former reaching its maximum value at about 1850°F and the latter at about 2000°F.

EFFECT OF COKE QUALITY ON CUPOLA MELTING

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DEFINITION OF THE TERM "coke quality" depends to a considerable extent on what is demanded of the coke. Coke considered of suitable quality for one operation may not be at all satisfactory for another. Furthermore, the cupola operator may have so adjusted his cupola operation that he gets satisfactory operation with what might be described as a mediocre coke. Under such conditions, it is entirely possible that the substitution of a coke of higher quality might even result in less satisfactory operation for this man. The quality of coke may vary in several ways. Some of these are: (1) coke size, (2) uniformity in size, (3) strength or resistance to breakage, (4) combustion characteristics, and (5) sulphur content. The last-named factor has seldom caused much difficulty.

Although any variations in the properties enumerated in the foregoing may be considered to be a change in coke quality, probably the most-used criterion of coke quality is the effect on the temperature of the iron. Whenever a decrease in iron temperature is experienced, particularly when a new car of coke is received, the coke often receives the blame. Other noticeable effects of a variation in coke quality, or rather a decrease in coke quality, are increased blast pressures and difficulties with bridging in the tuyere zone.

It is sometimes possible to offset a decrease in coke quality, as judged by a drop in iron temperature, by increasing the size of the coke charges. But if more coke is used, more air with corresponding higher blast pressures is needed to maintain the same melting rate. Many blowers lack the reserve capacity to produce the higher pressures and, in such cases, iron temperature and melting rate may both drop.

Coke Properties

The objective of this discussion is not to set up standards for coke quality or to consider the various factors that may affect the quality of coke. Such a consideration is the subject of a previous paper, "Foundry Coke Characteristics and Quality Factors," published in July, 1947 issue of AMERICAN FOUNDRYMAN. The prime purpose of this discussion is to indicate to the cupola operator what he might expect in his cupola operation with a change in coke quality and, also, what he might do in order to get the best out of the coke available.

Although there is no intention of discussing the various properties of coke in detail, it is, nevertheless, desirable to enumerate some of the properties of coke and how they affect its performance in the cupola. The size of the coke is quite important, not only from the standpoint of maximum size but, also, uniformity of size. Since coke must withstand fairly rough handling and also must support the weight of charges in the cupola, the strength of the coke is an important property.

A smaller coke of good strength may give more satisfactory operation than a large coke which is weak. With a small strong coke, cupola conditions can be set up to give optimum results for coke of that size. In the case of the weak coke, a change in coke size will occur in handling and it will be difficult to set up proper conditions. Another property of coke is its combustibility or reactivity. Although these are not the same thing, they are closely related. In one sense, combustibility is the rate of burning for a given rate of oxygen supply. Reactivity is the rate of reaction between the coke and CO_2. One factor related to combustibility, the ignition temperature, affects the combustion of the coke.

Handling of Coke

A coke with a low ignition temperature is likely to give high "solution" losses in the preheating zone of the cupola. As to the ignition temperature of coke, this is fairly closely related to the volatile matter content. Since this is the case, coke producers usually keep quite close control over the volatile matter content of the coke. The fixed carbon content of the coke also has some effect on its combustibility as well as its rate of solution in the iron. In general, a high fixed-carbon coke will tend to produce hotter and higher carbon irons than a low fixed-carbon coke. This is not a hard-and-fast rule since other factors must be considered. Although comparatively little is said about the fusion point and other properties of the coke ash, it is conceivable that a change in coke source may affect cupola operation through a difference in ash properties.

As a result of general shortage of coke, classification of coke by screening to given sizes has, in many cases, been dispensed with in order to increase the yield. Although the screening of coke at the coke plant is not

This discussion was presented at a Gray Iron Shop Course Session of the 51st Annual Meeting, American Foundrymen's Association, at Detroit, April 29, 1947.

an exceptionally rough treatment, it subjects the coke to sufficient tumbling action to stabilize its size. Too often, the handling of the coke by the foundrymen is considerably more drastic than that employed by the coke producer and, as a result, a coke of satisfactory size at the time it left the coke plant may have been so badly treated in subsequent handling that it is considered unsatisfactory at the time it reaches the cupola. Coke is an inherently brittle material and cannot be made stronger without affecting some of its other properties.

Coal Quality Lower

Although the coke producer exercises considerable care in the production of the coke and the running of his ovens, he is, nevertheless, dependent on the quality of the coal supplied to him. Since it is well known that stock piles of coal are at a very low point and that the quality of coal has decreased generally, the coke producer is somewhat limited as to what he can do with regard to coke strength. The cupola operator often can improve his operations considerably through better handling of coke.

There are three places in the handling of coke at the foundry where breakage can be reduced. These are in the unloading of cars, in the handling of the coke from the stock pile to the point at which it is weighed, and in the charging operation. As to unloading coke, the use of a clamshell bucket is highly unsatisfactory. Similarly, the dropping of coke from a self-dumping bucket or a box from a substantial height onto either a concrete platform or onto the bottom of a steel bin subjects the coke to unnecessary breakage. All unloading and handling operations should be conducted in such a manner that the coke is subjected to the least amount of shattering or crushing.

Considerable damage to coke often occurs through improper charging methods. If coke is unnecessarily broken up in unloading, the coke pile will contain rather small coke and large coke. If coke from this pile is randomly selected, it is quite likely that successive charges of coke will be nonuniform as to size. One charge may be made up of quite large coke, whereas the ensuing charge might be made of the small under-sized coke. In mechanical charging methods, it has been found generally that better results are obtained if the coke is charged on top of the bucket rather than on the bottom.

Some types of charging buckets are easier on coke than others. In charging with a drop-bottom bucket, breakage of coke can be minimized considerably by keeping the cupola full. If the cupola is operating with the top of the charges six feet or more below the bottom of the bucket, an undue amount of coke breakage will result. A decrease in coke quality due to lower strength may be offset by more careful handling in the plant.

Coke Quality—Relation to Cupola Melting

Although coke quality is certainly one factor in cupola operation, every case of unsatisfactory operation cannot be always attributed to poor coke quality. Since the results obtained with a given coke depend a great deal on the way in which it is used, some time will be devoted to a discussion of cupola operations and how variations in operation will affect the results obtained from the coke. The results reported and conclusions reached during discussions of this type would be of considerably greater value to the cupola operator if cupola operations were under much better control than they are at present.

Too often, the cupola is the most neglected piece of equipment in the foundry. This is a highly regrettable state of affairs since the quality of the casting leaving the foundry is very much dependent on the quality of the metal leaving the cupola spout. Foundrymen often spend comparatively large sums of money on molding equipment, but object strenuously to spending even a small amount on proper maintenance of a rather remarkable melting unit such as the cupola. It is only because of the remarkable nature of the cupola that satisfactory or reasonably passable operation is obtained with such gross neglect.

It might be a surprise to many foundrymen to learn what they could get out of a cupola if it were operated as carefully as an electric furnace. The cupola has been proven to be one of the most economical types of melting furnaces available. Not only is it remarkable with reference to operating cost, but with proper control, it can be depended upon to supply iron of guaranteed physical properties.

Another factor which is often neglected or lost sight of is the fact that the coke not only supplies heat to melt and superheat the iron but the coke also acts as a refractory material to support the charges in the proper position for correct melting. Better results from a given quality of coke are invariably obtained by better control over the cupola operation.

Controlled Charging

A cupola and its operation are usually neglected in one or two ways; (1) as to maintenance of the cupola as a mechanical unit and (2) as to the proper weighing or measuring of the materials going into the cupola operation. Such materials not only include the iron charge but also the coke, stone, and the air charges.

If consistently good results are desired, it is highly important that all materials going into the operation be carefully controlled. Not only is the weight of the materials important but, also, the size of the materials, particularly with reference to scrap. Uniformly good operation cannot be obtained by using small pieces of material on some charges and heavy pieces on others. The rate at which the pieces melt depends on their melting point and their mass or weight, with other factors constant.

The extent to which the various factors in cupola operation, cupola construction, and cupola equipment affect the results obtained from a given quality of coke will be discussed. For the sake of simplicity, a cupola can be divided into five zones; namely, the preheating zone, the melting zone, the superheating zone, the tuyere zone, and the well. The effect of changes in coke quality on its behavior in the various zones of the cupola varies to some extent since some properties are more important in some zones than in others.

One of the materials entering into cupola operation not always fully appreciated by the foundryman is the air. When it is considered that as much air by weight

is being used as iron is melted, the importance of this material should be apparent. Unless the foundryman has some control over the rate at which air is supplied to the cupola, he is at a disadvantage in getting the best results from the other materials in the charge.

Cupola and Equipment

There is no cupola operation too small not to warrant the installation of a blast-pressure gauge and a blast-rate indicating device. For larger operations, only a controlling air-weight device should be considered, preferably a recording type. The larger operation should also use a recording pressure gauge. Neither the installation of an air-weight control nor a recording blast-pressure gauge will guarantee good cupola performance, but they will help in enabling the operator to reproduce the conditions found to give most satisfactory practice.

The various air-weight control devices now sold for use on the cupola automatically compensate for changes in the density of the air and, therefore, supply a predetermined weight of air, whether it is indicated as cubic feet or pounds. A blast-pressure gauge is a useful instrument for indicating the conditions within the cupola. Fluctuations in windbox pressure with a constant rate of air supply are an indication that melting conditions are varying within the cupola. To allow such irregularities to persist day after day is an indication that the advantages of a pressure gauge are not appreciated by the cupola operator.

A change in pressure may indicate a change in bed height or such other irregularities as bridging and incorrect charging practice. Quite often, full advantage of air-control devices is not obtained because of incorrect installation or numerous leaks in the blast pipe and windbox system. Every effort should be made to keep the blast pipe and windbox system free of air leaks. Quite often an allowance of 5 to 10 per cent is made for air leakage. Such a condition should not be tolerated as it is entirely unnecessary.

With comparatively little effort, a blast pipe and windbox can be kept entirely free of leaks. Tuyere covers should be removed at regular intervals and resurfaced to prevent leakage at this point. However, it is not enough merely to stop air leakage in the blast pipe and windbox system. It is important that tuyeres be made to fit tightly against the cupola shell to prevent air from passing between the tuyere and the shell. As a result of various A.F.A. activities, there is a gratifying trend on the part of foundrymen toward greater appreciation of the importance of air measurement.

Air Distribution Important

With regard to distributing the air within the cupola, some operators attach too much significance to the design of the tuyeres and the tuyere ratio. Quite often, an otherwise good cupola installation will not give optimum results because of poor distribution of air around the cupola. In order to obtain maximum iron temperature and uniform results in the cupola, it is essential that air be equally distributed among the tuyeres. A cupola in which one side is supplied with more air than the other will melt unevenly and produce iron of unpredictable properties. Not only will the iron properties suffer, but it will be necessary to use more coke in order to keep the lowest part of the bed above the minimum desired level. This means too high a bed on the other side of the cupola.

As to tuyere design, some operators prefer continuous tuyeres, others, intermittent tuyeres. Either type works under proper conditions. Tuyere ratios will vary from 2:1 to 8:1 and even as high as 12:1. The distribution of the air in the cupola and the path followed by the ascending gases depend to a great extent on the distribution of materials and size of the voids in the stack of the cupola rather than on such factors as tuyere area or tuyere design. Air and gases in the cupola follow the path of least resistance, and comparatively little can be done about this path by adjustments or alterations of tuyere design.

One factor in cupola design that has a profound effect on the results obtained from the coke is the distance from the top of the tuyeres to the top of the charges or stock height. This distance may vary from as little as 9 ft to an extreme of 22 ft. Cupolas having stock heights of less than 12 ft require very careful supervision of the charging operation in order to get reasonably satisfactory results.

Cupolas with short stacks can seldom be operated at melting rates as high as those obtained with cupolas of the same diameter but with greater stock height. Not only is the height of the charges important but, also, that they be kept at a uniform height throughout the operation. An often-observed practice of allowing the top of the charges to drop five or six feet in the cupola and then bringing the charges up to normal level results in irregularities in iron temperatures as well as melting rate.

For normal operation, stack heights or rather stock heights in excess of 18 ft seem unnecessary since a higher blast pressure is required to force the air through the cupola. With unnecessarily high stacks, solution losses with coke of high reactivity may become rather high. The higher stacks are required when high melting rates are desired or if special types of cupolas such as the balanced-blast cupola are used.

Charging Equipment and Practice

Frequently, poor operation has been blamed on the coke, whereas it should have been placed on poor charging practice. Charging equipment and practice which tends to give unequal distribution of materials in the cupola cannot help but result in uneconomical operation. In coke of very high quality, it is sometimes possible to obtain acceptable results under these conditions.

However, with a decrease in the coke quality, the effect of poor charging practice is quite evident. This not only refers to lopsided charging, that is, charges being higher on one side of the cupola than the other, but also to segregation of the charge material as to size. A system which consistently charges fine materials in one part of the cupola and coarse materials in another contributes to nonuniform gas flow in the preheating zone and uneven bed height.

Some charging systems have a tendency to create high charge density in the center of the cupola and leave the charges at the periphery rather open, thereby allowing most of the ascending gases to travel up along the lining.

Such a condition is conducive to poor coke economy, low melting rate, and cold iron. Some types of charging buckets are harder on the coke, thereby decreasing the effectiveness of the coke.

Improper selection of charge materials as to size will result in nonuniform flow of gases in the preheating zone, with the result that some of the metal pieces will be insufficiently preheated when they reach the melting zone, whereas others will be melted above the zone.

Unless some skill is exercised in their manipulation, the use of wheelbarrows for charging cupolas is not always satisfactory. Similarly, other charging devices which introduce the materials by means of a chute from one side of the cupola are apt to cause considerable segregration of the materials in the cupola. In small cupolas, however, it is possible to obtain reasonably good results with such equipment if ample precautionary means are taken.

Lining in the Combustion Zone

Unsatisfactory results attributed to coke can often be traced to unsatisfactory patching practice in the cupola. When it is realized that more heat is required to form a pound of slag than to melt and superheat a pound of iron, the importance of keeping melting-zone erosion or burn-out to a minimum can be appreciated.

Not only is heat required to melt and flux the refractory burned out, but the increase in volume in the melting zone must be compensated for by adding additional coke. If the melting zone could be maintained to the desired dimensions throughout the heat, considerable savings in coke could be realized in addition to less trouble with bridging and freezing up of tuyeres.

Not only must the materials and the technique used for patching the melting zone be good, it is also necessary that it be properly dried out and preheated before melting actually takes place. Flash heating of the surface of the patch when it is still backed up by wet mud should not be considered satisfactory practice.

Poor patching materials or patching techniques also result in a comparatively large amount of sticky material dropping past the tuyeres and often freezing in front of them. This freezing of material in front of the tuyeres materially affects the distribution of air in the tuyere zone. This material in front of the tuyere often builds up forming a bridge in the tuyere zone. Not only does the quality of the material used in the patching of the melting zone have an effect on the operation but also the dimensions to which the zone is patched.

Lighting-Up Practice

Under some conditions, an improvement in temperature can be realized by decreasing the cross-sectional area of the cupola in the melting zone without changing it above the melting zone. Such a practice will increase the blast concentration, i.e., pounds of air per square foot of cupola area, and thereby extend the melting and superheating zones, providing a greater height of hot coke through which the iron can be superheated.

A poor job of lighting the bed in the cupola will often completely obscure any effect of a change in coke quality. It is highly important that the bed-lighting operation be conducted in such a manner that the well of the cupola is thoroughly preheated before melting starts. Very little combustion takes place in the well zone, and once the coke has dropped into this area, most of the heating of the refractories in the well is through absorption of sensible heat from the coke in that portion.

The method followed in lighting the bed has a profound effect on the drying out and preheating of the refractories in the combustion zone. Sufficient time should be allowed for lighting the bed to ensure establishing equilibrium between the temperature of the surface of the refractories and the rate of heat flow to the shell. It is impossible to establish such a relationship in less than several hours. Although it is desirable to put the blast on the bed for a short time before charging is started to clean it of ash and, also, to cause it to settle down so that the height of the bed can be properly measured, the use of the blast should not be relied upon to offset an otherwise poor light-up operation.

In burning-in the bed, some care must be taken to regulate the air so as not to burn the coke to too small a size. If burning of the bed is prolonged too far or permitted to progress too vigorously, the bed will be filled with small coke rather than with the more desirable larger size which was charged. This small coke will burn rapidly at the beginning of the heat, thereby causing a rather drastic drop in bed height unless precautionary measures are taken. The quality of the bed-lighting operation can be judged by the temperature of the first iron out of the cupola.

Effect of Coke Quality on Operating Conditions in the Five Zones of the Cupola

As pointed out previously, a change in coke quality may affect the operation of the cupola differently in the various zones existing in the cupola. Some properties of coke which might make it very desirable with reference to the performance in the tuyere zone might make it unsuitable to fulfill its proper function in the preheating zone or in one of the other zones.

With reference to the function of coke in the preheating zone, the primary purpose is to support the metal charges and, therefore, resist crushing due to the movement of the stock down the cupola and to the impact resulting from the addition of new charges. A decrease in the size of the coke or in the strength of the coke, thereby resulting in greater crushing and reduction in its size, will result in greater resistance to gas flow through the cupola stack. Higher blast pressures will, therefore, be encountered, with a greater tendency for the gases to pass up along the lining.

Another property of coke which will affect its performance in the preheating zone of the cupola is its ignition temperature and reactivity. A coke with too low an ignition temperature will start to burn through the reaction of carbon dioxide, which produces carbon monoxide in this zone. Should this occur, considerable loss of coke takes place before it reaches the melting zone where it is needed. Since the ignition temperature is related to the volatile matter content, it is desirable to keep the volatile matter contents about one per cent.

The primary function of coke in the melting zone is to supply sufficient heat to melt the iron charges above it. Although many diagrams showing the position of the coke and iron charges in the cupola stack show well-

defined layers of coke and iron, this is seldom the case in actual practice.

Charges become somewhat scrambled in their descent through the cupola so that by the time the melting zone is reached, melting may occur through an appreciable depth. In a well-balanced operation, this melting is restricted to a fairly narrow well-defined zone. Operations not so well under control may have an appreciable extended melting zone. This is not desirable from the standpoint of chemical composition and metallurgical control of the properties of the iron.

If the decrease in coke quality is merely due to a decrease in size, the small coke in the melting zone will result in increased blast pressures and a narrower melting zone. Due to the comparatively large amount of surface exposed when the coke is small, coke combustion may take place at a more rapid rate than desired, thereby necessitating the use of more coke to keep the bed at a safe height. The question of volatile matter content of the coke is very likely wiped out before the coke reaches the melting zone, since the time required to pass through the preheating zone is usually sufficient to drive off the excess volatile matter.

Effect of Ash Content

The ash content of the coke as well as the fusibility of the ash would be expected to have some effect on the performance of the coke in the melting zone. If the ash cannot be fluxed off rapidly, it is possible that the coke in the melting zone will become rather gummed up, causing sudden rises in pressure and a decrease in iron temperature.

Some of the remarks made relative to the performance of coke in the melting zone also apply to the coke in the superheating zone. The superheating zone is that part of the coke bed at the highest temperature. It also corresponds to a zone of complete combustion. In this zone, all oxygen has been eliminated and carbon dioxide content of the gas is at a maximum. This region corresponds to the area of maximum burn-out in the cupola, ordinarily 15 to 18 in. above the tuyeres.

If coke becomes small, this superheating zone will become shallower due to more rapid reaction between the oxygen in the air and the carbon in the coke. Although it is difficult to set up a test designed to indicate the temperatures reached by the combustion of the coke, it is felt that some changes in combustion characteristics of the coke may be reflected in the temperature reached by the coke in this part of the cupola. Cokes of low carbon content are not likely to reach as high temperatures as those of higher carbon contents.

If a coke is of small size in the superheating zone either because of initial small size or due to crushing as a result of low strength, the superheating zone can only be extended by increasing the blast rate. In order to keep coke at maximum temperature in the superheating zone, it is desirable to keep the coke clean by means of sufficient slag, high in lime, to wash off the ash produced in the combustion of coke in this zone. This scavenging action of the high-lime slag in the superheating zone is highly important if maximum temperatures are desired. Conditions in the superheating zone also influence the carbon picked up by the iron.

A difference in coke behavior has often been observed at the tuyere level. Some types of coke seem to turn dark more rapidly than others after the blast is turned on. In this regard, blast temperature has a pronounced effect on the temperature of the coke immediately in front of the tuyeres. Even a mild increase of temperature from 70 to 250 F has a noticeable effect.

Coke Reactivity Factors

From the standpoint of maintaining high temperatures immediately in front of the tuyeres, a highly reactive coke would be desirable. However, as pointed out previously, such a highly reactive coke would be destroyed at a fairly rapid rate in the preheating zone. It would be a nice thing if a highly reactive coke could be safeguarded against solution losses in the preheating zone until it reached the tuyere zone. The temperature of the coke near the tuyeres, in addition to being affected by coke quality and blast temperature, is also affected by melting rate and the amount of slag dropping to the well.

Since combustion of the coke is not complete until the superheating zone is reached, the temperature of the coke at the tuyere level is of necessity lower than that in the superheating zone. The temperature of the coke in the tuyere zone is appreciably increased by the descent of the hot iron and slag from the superheating zone above. It was frequently observed that a cupola exhibiting a considerable amount of iron dropping past the tuyeres will have cleaner tuyeres than one in which the melting takes place farther toward the center of the cupola.

The matter of ash content of the coke is probably important in the tuyere zone. The low-carbon coke is more likely to result in a colder tuyere-zone area than a high-carbon coke. Small coke at the tuyere zone will result in poor penetration and distribution of the air blast. Too small a coke in the tuyere zone is also likely to result in bridging in front of the tuyeres.

The effect of coke quality in the well zone is probably not nearly so pronounced as it is in a tuyere melting and preheating zone. Small coke in the well may cause some difficulty with the slag hole becoming plugged with pieces of coke. A coke of very low reactivity, that is of high ash content, may result in a colder operating slag hole and give some difficulty with free slagging.

Low-carbon cokes are apt to give less carbon absorption in the well than high-carbon cokes. However, since a high-carbon coke is also likely to develop more heat in the superheating zone, it is difficult to determine whether greater carbon pickup resulted from the contact with the coke in the well or with the hotter coke in the superheating zone. A well filled with very small coke will, of course, have less metal capacity than one filled with fairly large coke.

Factors Affecting Iron Temperature

If the various factors which affect the temperature of the iron as it reaches the well in the cupola are classified, it will be found that there are only three primary factors responsible for metal temperature. These are (1) the amount of steel in the charge, (2) the depth of the superheating zone, and (3) the temperature of the superheating zone. Of course, the temperature of the

iron tapped also depends on how long it is held in the well of the cupola and some of the other conditions existing in the cupola well.

Iron temperature depends on the amount of steel in the charge because steel melts at a considerably higher temperature than pig iron or scrap iron. The drops of carburized molten steel start out at an initial temperature approximately 500 degrees higher than drops of molten iron. A drop of molten steel, therefore, does not require as much superheating as a drop of molten iron in order to reach a desired temperature of, say, 2800 F. All other things being equal, hotter iron should be expected when the amount of steel in the charge is increased.

Superheating Zone Depth

The depth of the superheating zone, of course, affects iron temperature, since too short a zone will not allow the drops of iron descending through this zone to absorb the heat available. Coke size, reactivity, coke ratio, and blast rate affect the depth of this zone. The temperature of the superheating zone in turn is affected by melting rate, coke quality, blast rate, and blast temperature. It is also quite likely that the amount of slag falling through the superheating zone affects its temperature, since it cleanses the coke and exposes fresh carbon to the oxygen entering the combustion zone.

Although the three primary causes enumerated above can be held responsible for iron temperature, there are a number of factors which contribute to the three causes and make the relations more complicated. The remarks above also indicate that certain changes in coke quality or cupola operation affect not only the depth of the superheating zone, but the temperature as well. Most often, the two factors cannot be varied separately.

In analyzing operations of cupolas in a number of foundries and comparing the results obtained, it was found that there was a fairly good relationship between the rate of combustion of coke per square foot of cupola area and the temperature of the iron obtained. The relation was somewhat independent of coke ratio.

Analysis of the operations of cupolas that produce hot iron indicates that the air to coke ratio is on the order of approximately 8 to 9 lb of air per lb of coke. With such an operation, the carbon dioxide content of the stack gas falls within the range of 12 to 14 per cent. Excessive air to coke ratios indicate that an attempt is made to drive the cupola at a rate faster than iron can be melted.

Carbon dioxide contents of the stack gas of less than 12 per cent indicate that too much coke or too little air is being used. On the other hand, a trend in carbon dioxide content in excess of 14 per cent often indicates that the bed is burning too low as a result of insufficient coke or too much air. With reference to cupola efficiency, it is desirable to use a minimum amount of air to a given weight of iron melted.

Coke Quality Variation—Metallurgical Effects

Since coke quality affects cupola operation in several ways, it often becomes rather difficult to determine whether a change in the metallurgical properties of the iron is a result of differences in coke quality or of a change in cupola melting conditions resulting from a change in coke. The matter of carbon pickup is, of course, dependent not only on coke quality but also on other melting conditions within the cupola.

It has been definitely proven, however, that higher carbon cokes, pitch coke being an example, will result in a greater carbon pickup in the cupola. Similarly, low-carbon cokes will produce lower carbon irons. It is also entirely probable that the amount of inoculation needed to correct undesirable structures in iron may be related to some property of coke. It is known that iron held in the well of a cupola will often show a decrease in chill, particularly if it is hot and the coke is of good quality. It is a fairly well-known fact that if the iron runs cold because of a decrease in coke quality or a change in melting conditions in the cupola, which in turn were caused by a change in coke quality, the iron will show an increase in the amount of undesirable graphite structure.

It may also be possible to attribute an increase in chilling tendency of the iron to lower coke quality, the composition being the same. Some cupola operators have observed or rather attributed unsoundness of iron and defects such as interdendritic shrinkage to high volatile matter in the coke. This is somewhat open to criticism since it is likely that the volatile matter will be driven out before it reaches the melting zone.

Summary

A considerable improvement in coke economy may be effected by paying careful attention to the construction and maintenance of the cupola and the operation of the auxiliary devices. Cupolas having stack heights of less than 12 ft should be rebuilt or altered in order to increase the height to at least 14 ft, and preferably more. All air leaks should be eliminated and air-weight and air-pressure control instruments installed if they are not on the cupola at the present time. Improved operation will result if more attention is given to supervision of the charging operation. Undesirable melting conditions in the cupola caused by poor charging will result in cold iron at the spout. However, by the time the cold iron is observed at the spout, it is too late to correct the poor charging responsible for the condition.

Proper lighting of the bed is important for short heats as well as long heats. It has been found difficult to correct conditions caused by poor bed-lighting practice later in the heat.

If melting conditions in the cupola are correct as to bed height, air to coke ratio, and coke to iron ratio, it is entirely possible that a good quality of iron can be produced at temperatures below 2750 F. In such an event, it is highly important that the loss in temperature of the iron after it leaves the cupola is kept to a minimum.

Although the fluidity of the iron or its ability to fill a mold depends on the composition, superheat is one of the most important factors. Therefore, with lower spout temperatures, it is highly important to decrease the temperature losses during handling so that the iron reaches the mold at a safe temperature. There is hardly any cupola operation that cannot be improved in some respect with reference to obtaining more uniform iron temperature, higher iron temperatures, and the use of less coke.

VARIABLES AFFECTING CARBON CONTROL IN CUPOLA OPERATION

By
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IT IS THE PURPOSE OF THIS PAPER to discuss some of the variables which affect carbon control when melting iron in the cupola, and to present an equation, based on a knowledge of these variables, for calculating the percentage of carbon to be expected in the iron at the cupola spout. This procedure has been used successfully over a period of several years in the foundries of the company with which the author is associated and extensive laboratory records are on file.

The ultimate objective of cupola operation covers many items. Among these is the delivery of good, hot iron to the pouring stations at a rate equal to the demands of the molding department. Another is that the iron must be of the proper composition and of the highest quality commensurate with the type of castings being produced. Good "carbon control" is one of the prerequisites for "quality control" and, in the opinion of the writer, these terms are practically synonymous, although the importance of the balance of the chemical composition must not be overlooked.

The amount of carbon in our cast irons has more influence on the physical and mechanical properties of the castings than any of the other ordinary elements entering into the chemistry of the iron. (Alloying elements and inoculants bring about certain changes due in part to their influence on the amount of graphite or combined carbon in the iron and, of course, in part to the effect resulting from being taken into solution by the matrix.)

Carbon Equivalent Calculation

One of the accepted equations for the calculation of carbon equivalent (C.E. $=$ %T.C. $+ 0.3$ (%Si $+$ %P) demonstrates clearly the importance attached to the percentage of total carbon in the iron. It means that the percentage of total carbon exerts about three times as much effect on the properties of the iron as do either silicon or phosphorus. In other words, the foregoing equation "indicates that a change in carbon necessitates an inverse change in silicon and/or phosphorus of approximately three times the magnitude" if we are to maintain the same structure and hardness in castings having equal cooling rates.

Furthermore, it is well known that the silicon content of our irons can be increased when necessary by the addition of ferro-silicon after the iron has been melted. Conversely, for some applications an excess of silicon (as determined by chill-test) can be compensated for by the addition of some sort of stabilizer such as ferro-chromium.

Sulphur can be increased or decreased after the metal has been melted. Manganese can be increased and likewise phosphorus if desired. In contrast with the elements just mentioned, it is practically impossible to increase or decrease the carbon content of the iron after it has been tapped from the cupola. This applies particularly to the composition range covering the bulk of our commercial gray iron castings. Furthermore, in certain applications, such as permanent mold irons, a deficiency in total carbon cannot be satisfactorily compensated for by an increase in silicon.

An excess of total carbon in these applications cannot be taken care of quickly as we have no way of reducing the carbon content; the silicon cannot be reduced and, in some of the irons in question, the addition of stabilizing alloys or inoculants is out of the question due to the detrimental effects on the finished product.

Controlling Total Carbon

In view of the foregoing it is obvious that we must make every effort to control the total carbon in the iron at the cupola spout so that it will be in the range most suitable for the castings being produced. Some of the factors which affect the percentage of carbon in the melted iron are listed in the following paragraphs.

1. Type of Coke Used in the Cupola Bed—Some types of coke lend themselves to greater carbon absorption by the molten metal than others. Bed height also has an effect on carbon pickup, especially in the first iron melted at the beginning of a heat, although it is assumed that the bed height has been properly and carefully adjusted in accordance with good practice.

2. Amount and Type of Coke Used Between Charges—As mentioned in the preceding paragraph, some types of coke lend themselves to greater carbon pickup than others. The company with which the writer is connected uses three different types of coke, either singly or in combination, to help maintain carbon control. These are by-product, beehive and pitch cokes. The by-product coke is used alone when low to medium carbon irons are being produced, and the beehive coke is used when higher carbon irons are melted. The pitch coke, all other things being equal, produces the hottest iron and results in a greater carbon pickup than is realized when using either of the other cokes. It is always

NOTE: This paper was presented at a Gray Iron Shop Course Session of the 51st Annual Meeting, American Foundrymen's Association, April 28-May 1, 1947, at Detroit.

used in combination with one of the other types, and combinations of the other cokes (without pitch) are frequently used.

3. Carbon Content of Ingoing Metal Charge—If we are to control the amount of carbon in the iron at the cupola spout we must have an accurate knowledge of the composition of the ingoing charge, including the percentage of carbon in the charge. This is easily determined provided we know the carbon content of the various components. We can and should make determinations for total carbon on each carload of pig iron received with the same care and accuracy used in determining silicon, sulphur, manganese, and phosphorus.

Likewise, we have records of total carbon in our own return scrap and gates. The use of large percentages of purchased scrap should present no particular problem as the composition, including total carbon, of the various types is quite definitely known. It is granted that carloads of mixed scrap present a problem and contents might have to be sorted according to classification. The best and simplest way to avoid these difficulties is to instruct the party responsible for the purchasing of scrap to insist that any one carload contain only one type and not a mixture of several kinds.

In comparing notes on cupola charges, foundrymen and metallurgists frequently refer to a mixture as containing a certain amount of steel, 50 per cent for example. While the percentage of steel is certainly of interest and importance, the writer feels that this figure in itself tells only part of the story because it gives no idea of the amount of carbon in the charge, and it is this figure which (as will be shown later) determines the amount of carbon in the iron at the cupola spout. To illustrate, three cupola mixtures are tabulated:

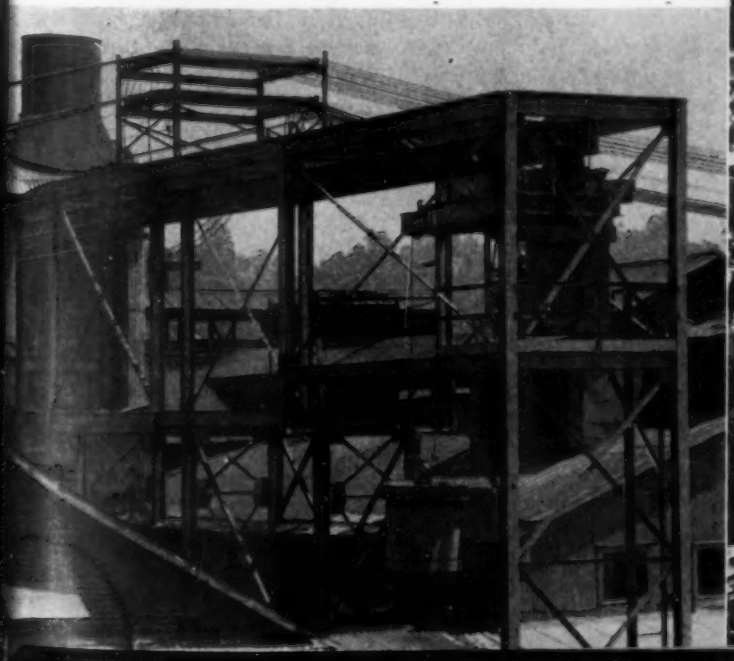
(A)	50% steel containing	0.30% T.C.
	50% pig iron containing	4.20% T.C.
	Carbon in charge	2.25%
(B)	50% steel containing	0.30% T.C.
	50% cast scrap containing	2.90% T.C.
	Carbon in charge	1.60%
(C)	25% steel containing	0.30% T.C.
	75% cast scrap containing	2.90% T.C.
	Carbon in charge	2.25%

From this table it is evident that mixtures (A) and (B) will not produce irons of the same carbon content, when melted under like conditions, in spite of the fact that each contains 50 per cent of steel. Likewise, mixtures (A) and (C) will produce irons of about the same carbon content even though mixture (A) contains twice as much steel as mixture (C).

4. Accurate Weighing of Materials in Cupola Charges—If the calculation of the composition of the ingoing charge is to be of any value, we must insist that all components of the charge be carefully weighed. Weighing equipment must be checked frequently by qualified scale repair men and kept in good order. Too frequently, when the analysis "gets off," investigation reveals that the iron yard or some other scale is out of order and the charge has been estimated.

The weighing equipment must be rugged and accurate, sensitive over the range in which it is to be used, and intelligently chosen for the job it is intended to do. Generally speaking, one set of scales will not be satisfactory for weighing all of the materials entering into the charge. For example, 50 lb of 50 per cent lump ferro-silicon ordinarily cannot be weighed accurately on a set of scales designed for heavy loads and where

Views showing cupola charging method; accurate weighing (below) of cupola charge components and (left) method of conveying charge to cupola.





the smallest division on the dial represents 25 lb and the tare weight of the charging car and bucket may be 3,000 or 4,000 lb.

Assuming that the scaleman works to an accuracy of plus or minus one division on the scale dial, his error could then be plus or minus 25 lb. This represents 12.5 lb of silicon, which in turn means a possible variation of 0.50 per cent silicon in a 2,000-lb charge. This is not a hypothetical case as these figures represent a set of conditions which actually exist at one of this company's plants. For weighing materials used in quantities up to about 50 lb an ordinary spring scale has been found to be quite satisfactory.

Silicon Variations

Reference has been made here to the possible variation of percentage of silicon in the iron resulting from inaccurate weighing of ferro-silicon, and the reader may well ask what bearing this has on carbon control. It is well known that, all other things being equal, the carbon pickup during melting will be inversely proportional to the percentage of silicon present in the iron at the cupola spout. This will be discussed in more detail later in the paper.

For weighing 100 or 150 lb of silvery pig iron a portable platform scale of 500-lb capacity is used. Coke is weighed on the same type of scales and the equipment need not be expensive or elaborate. Containers used for coke may be scrap steel drums cut in two with handles welded to each half. The gross weight is marked on each container and, for convenience and ease of handling, the coke is weighed in batches of 100 lb or less. Plant layout and conditions will determine, at least to some extent, the type of weighing equipment to be used. However, this subject should be given considerable thought and attention as there is no substitute for accuracy in making up cupola charges.

5. Tapping Technique—All of the cupolas operated by this company are of the front-slagging type which, of course, means that iron flows from the taphole continuously from start to finish of the heat. At no time is there any appreciable accumulation of molten iron in the well of the cupola, but the small amount which does collect remains at a constant level because it flows out as fast as it is melted. Front-slagging cupolas possess many advantages over those tapped intermittently, including uniform carbon pickup, which is one of the essentials of carbon control.

Front-Slagging Cupolas

Other advantages include hotter iron at the cupola spout; no need for a slag hole with the usual loss of blast and occasional shutdowns for making repairs, flow of metal from the cupola may be stopped by shutting off the blower, thus eliminating the need for "bots" in the breast hole. This not only makes a safer operation, but also it is practically impossible to run cupola slag into the pouring ladles, as frequently happens when the man tapping the cupola leaves the breast hole open a little too long or a bot accidentally comes out.

In cupolas which are tapped intermittently the amount of molten iron in the well may range from several tons (in the larger cupolas) to practically none in the few minutes required to make a tap. This means that the depth of iron within the cupola is constantly changing, and under these conditions the carbon pickup is apt to be less uniform than when the metal is removed as fast as it is melted.

It is not inferred that uniform carbon pickup in the cupola cannot be obtained with intermittent tapping, but it requires a little closer supervision. An exact tapping cycle must be established, and the time between taps must then be maintained by means of a stop watch.

Frequently, with intermittent tapping, a cupola tapper can see no harm in allowing the time between taps to vary 10, 20 or 30 seconds, although actually this may be a high percentage of the established time cycle (between taps), and variations of the magnitude just mentioned may materially affect the percentage of total carbon in the iron tapped, especially in mixtures containing high percentages of steel.

6. Other Factors—Generally speaking, any change made in the operation of the cupola which results in higher metal temperatures at the cupola spout will result in somewhat higher total carbon in the iron. These changes include an increase in coke between charges (reducing coke ratio), increasing blast pressure with attendant increase in melting rate and temperature, preheating the cupola blast, and others.

Weighing Blast Air

Accurate weighing of the air used for the blast is another essential to good carbon control. It is well known that for every pound of iron melted, practically one pound of air is required. It therefore seems logical that a raw material used in quantities nearly equal in weight to the tonnage of iron melted be metered or weighed with the same care as the metallic components of the charge. Variations in the weight of air blown into the cupola will affect melting rate and melting temperature, and these variables will in turn affect carbon pickup, as discussed in the preceding paragraph.

Conditions within the cupola must be kept as uniform as possible from day to day as well as throughout the duration of a single heat. Moisture content of the blast has its effect on carbon in the iron at the cupola spout, a low moisture content resulting in higher carbon than a high moisture content, all other things being equal. It is well known that the moisture content of the blast may vary from 2 grains per cu ft to 12 grains per cu ft, depending upon conditions as well as the season of the year. Furthermore, the moisture may vary within the limits referred to during the course of a single day's operation.

There are 7,000 grains in one pound. Now let us consider the possible variation in the amount of water carried into a cupola with the air blast when melting 200 tons per day, as is often the case at one of the foundries being discussed. This operation will require about 5,200,000 cu ft of air, so that the water blown into the cupola will be between 1,500 lb (180 gal) and 9,000 lb (1,080 gal).

From these figures it is evident that the control or elimination of a variable of such magnitude is well worth consideration. For this reason equipment for controlling moisture in the cupola blast was installed at one of the plants of the company.

The installation is designed to remove moisture from

13,000 cu ft of air per minute down to 4 grains per cu ft in the summer season, and also is capable, in dry weather, of introducing as much as 7 grains per cu ft. In the winter season it is necessary to add moisture to the cupola blast in order to maintain a year 'round constant control.

While it so happens that this company has installed a moisture control system for maintaining a year 'round uniform moisture content of 4 grains per cu ft of cupola air blast, this figure in itself is not particularly significant. Whether the system be designed for 2 grains or 5 grains or any other figure, the significant feature is that variations in moisture content of the cupola blast have been eliminated.

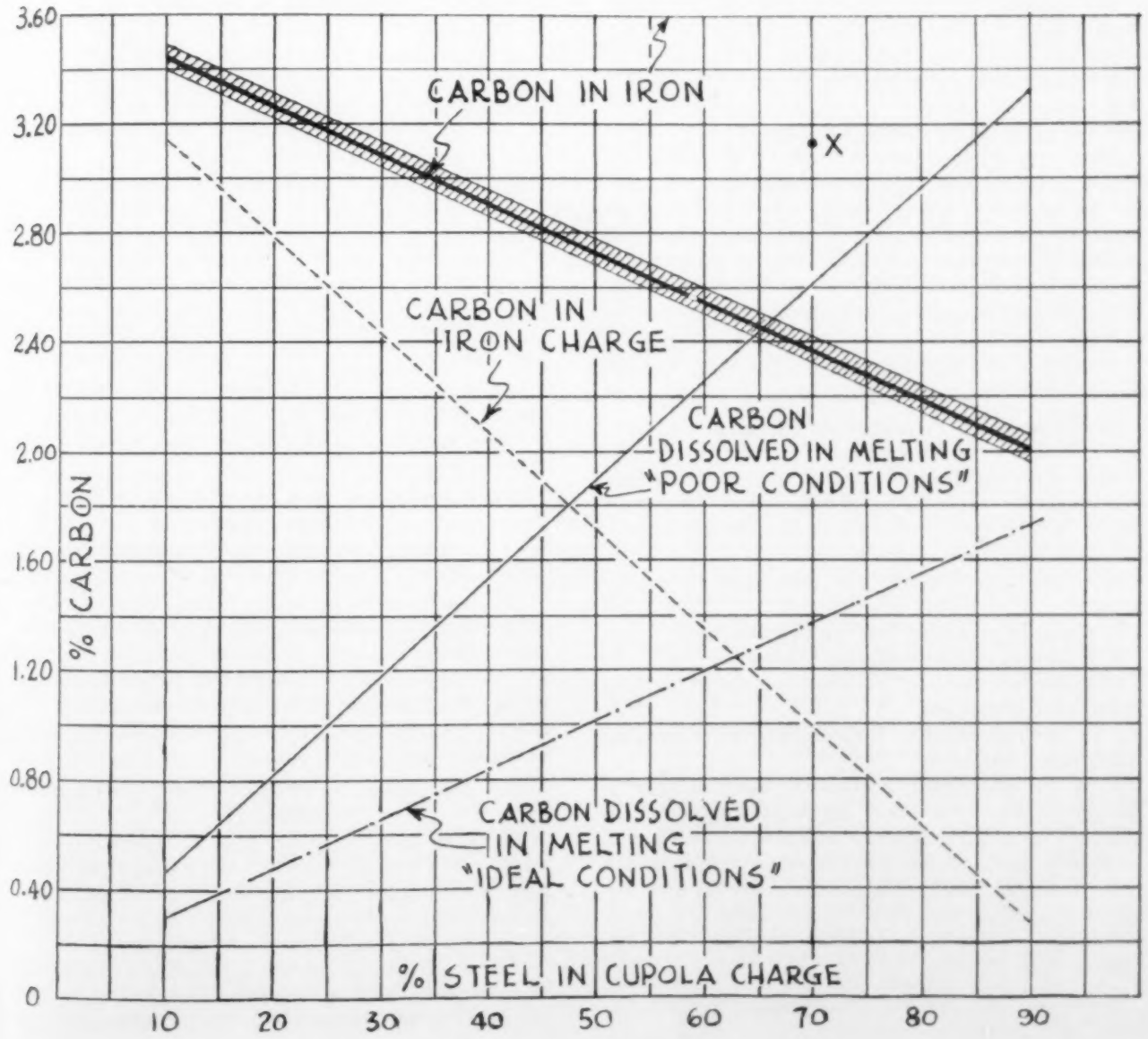
The balanced blast type of cupola helps to maintain good uniform conditions within the cupola from start to finish of the heat, and is particularly helpful when the heat is of long duration. Five of the seven cupolas operated by the company are of the balanced-blast type, each equipped with one row of main tuyeres and two rows of auxiliary tuyeres. The main tuyeres are provided with slide valves so that they may be opened or closed at will during the course of the heat.

Two of the main tuyeres diametrically opposite are closed at the same time so that the area directly in front of them is heated to a temperature sufficiently high to melt off all overhanging slag. After 8 or 10 min these are opened and two others are closed. This procedure is continued throughout the heat, and results in the prevention of bridging and blocking-up of the main tuyere openings.

The openings of the auxiliary tuyeres are adjustable, but the settings cannot be changed without shutting off the cupola blast. The upper, or auxiliary tuyeres, bring about more complete combustion of the coke and thereby make possible a more efficient cupola operation, and in conjunction with the controllable main tuyeres the balanced-blast cupolas melt somewhat faster and at higher temperatures with the same coke ratios than the cupolas provided with conventional tuyeres.

Auxiliary cupola equipment plays an important part in control and uniformity of the molten iron. All cu-

Fig. 1—Graph showing method for determination of percentage of carbon in iron.



polas in this company's foundries are charged mechanically. Mechanical charging usually improves the operation because it is easier to keep the cupola filled to the charging door level at all times during the melting operation, and the types of charging equipment used distribute both the metal and coke in the cupolas in a much more satisfactory manner than is accomplished by hand.

This is especially true with cupolas of larger inside diameters, where charging crews often are prone to place the heavier components of the charge near the charging door opening. In hot weather more of the coke charge than should be is placed near the charging door in order to protect the "chargers" from heat inside the cupola.

As previously stated, all of the cupolas operated by the company are of the front-slagging type. Molten iron from the cupolas runs directly into forehearths or teapot ladles, where an amount equivalent to several cupola charges is stored and mixed, resulting in greater uniformity of metal composition than would be obtained when tapping directly from the cupola into the pouring ladles.

This is especially true when the capacity of the pouring ladles is small (1,000 lb or less). The size of the forehearth ladle is such that it will hold as much molten iron as the cupola melts in 10 to 15 min. From this it is obvious that different sizes of forehearth ladles are used with cupolas of different capacities.

After having taken into account all of the factors involved and eliminating as many variables as is possible the laboratory should keep proper records of all cupola mixtures used. They will in time become invaluable to the operator in making up mixtures to produce irons of predetermined carbon content and, furthermore, will enable him to make a graph or series of graphs showing the total carbon level to be expected in the iron at the cupola spout when the carbon in the charge is known.

Computations for Percentage of Carbon in Iron at Cupola Spout

Some years ago, Donald J. Reese* prepared an excellent graph for determining percentage of "carbon in iron." The graph (private communication) is presented as Fig. 1 and includes:

1. A line representing "per cent steel in cupola charge."
2. A line representing "carbon in iron charge."
3. A banded line representing "carbon in iron."
4. A line representing "carbon dissolved in melting under ideal conditions."
5. A line representing "carbon dissolved in melting under poor conditions."
6. In the area above the banded line representing "carbon in iron" Mr. Reese has located a point "X" which, of course, represents an unknown percentage of "carbon in iron." In other words, when melting under poor conditions it will be impossible to predict with any degree of accuracy the carbon to be expected in the iron at the cupola spout, as it may fall at any point "X" within the area in question.

* Development and Research Div., International Nickel Co., Bayonne, N.J.

Mr. Reese's graph presents valuable information regarding the relationship between carbon content of the molten iron and carbon content and steel content (percentage) of the charge.

Records have been kept in the laboratories of the Lynchburg Foundry Co. over a period of more than 10 years in which the effects of changes in percentage of ingoing carbon, various percentages of silicon and phosphorus, as calculated in the iron at the cupola spout, and the effects of various types of coke and/or combinations have all been noted. From the data gathered it has been possible to derive an equation and draw a graph (Fig. 2) from which "per cent carbon in iron at cupola spout" can be predicted when "per cent carbon in cupola charge" is known regardless of the relative amounts of steel, cast scrap, and pig iron used in the charge. Certain conditions must exist and certain practices must be followed in order that data taken from the graph be reliable. They are as follows:

1. Use of by-product coke.
2. Continuous flow of metal from cupola (front slagging), or
3. Maximum tapping cycle of 90 sec if tapping intermittently. This means 90 sec from the time any one "bot" is removed from the breast until the next one is removed. It does *not* mean that the cupola is left closed up for 90 sec after the "bot" is put into place. Obviously, this means that study must be given to calibration of the tap hole as iron must be removed from the cupola just about as fast as it is melted.
4. Silicon at cupola spout must be about 2 per cent.
5. Phosphorus in iron at cupola spout must be about 0.2 per cent.
6. The graph has been used successfully for "calculating" carbon in iron at the cupola spout within the range of 2.40-3.60 per cent T.C. This, of course, means that carbon in cupola charge was in the range of 1.10-3.50 per cent.

In Fig. 2, the carbon content of the cupola charge is plotted against carbon content of the molten iron at the cupola spout. The equation for this relationship is as follows:

$$(1)\quad \text{T.C. (spout)} = K + \tfrac{1}{2}\,(\%\ \text{C in charge} - 2.00\%),\ \text{where}$$

"T.C. (spout)" means "per cent carbon in iron at cupola spout."

"K" is a constant.

"Per cent C in charge" means "per cent carbon in cupola charge."

For the graph drawn a value of 2.85 per cent has been assigned to K, and this value applies when the six conditions described exist in the foregoing. The equation then becomes:

$$(2)\quad \text{T.C. (spout)} = 2.85\% + \tfrac{1}{2}\,(\%\ \text{C in charge} - 2.00\%).$$

The portion of the graph (Fig. 2) representing the range over which it has been used successfully is drawn as a band indicating that the carbon in the iron at the cupola spout can be expected to fall within a range of approximately plus or minus 0.05 per cent.

The graph is used as follows: Suppose the carbon in the charge has been calculated and found to be 2.00 per cent. A horizontal line is drawn intersecting the

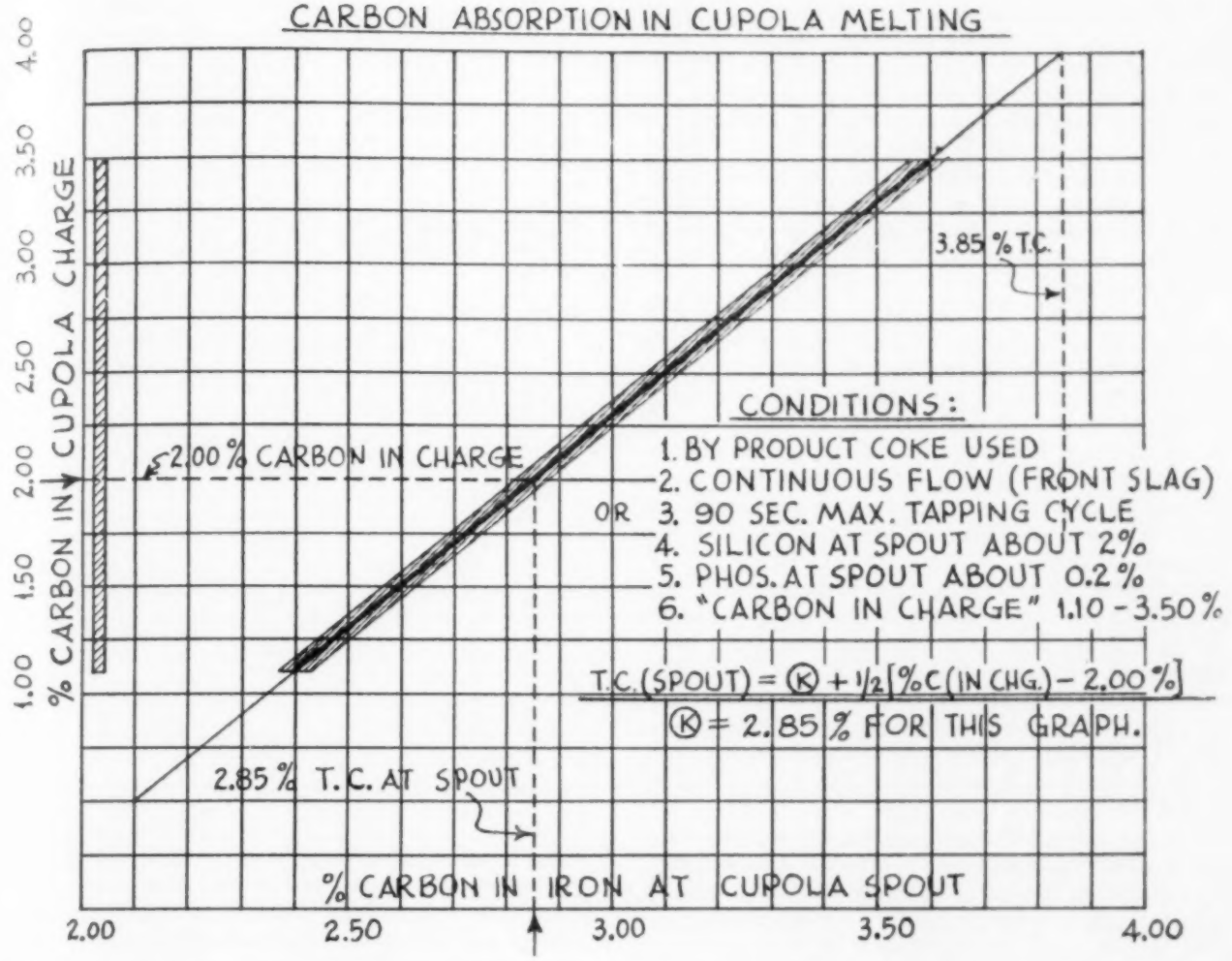


Fig. 2—Graph showing percentage of carbon in iron at cupola spout when percentage of carbon in charge is known and certain conditions exist.

graph at the level representing "2.00 per cent carbon in cupola charge." From the intersection with the graph a vertical line is drawn so as to intersect the line representing "per cent carbon in iron at cupola spout." This point is found to be 2 .85 per cent, or the percentage of carbon in the iron at the cupola spout.

Examination of the graph and/or Equation (2) reveals the following:

1. When the "per cent carbon in the charge" equals 2.00 per cent, the "per cent carbon in iron at cupola spout" will equal 2.85 per cent, or K.

2. Any change made in "per cent carbon in the charge" increases or decreases the "per cent carbon in iron at cupola spout" by an amount equal to one-half of the change.

3. Within the range of the graph, it is noted that there is always an increase in carbon during melting.

4. Beyond the range of the graph (where we have no data at present) it seems probable that when the "per cent carbon in the charge" is 3.70, the "per cent carbon in iron at cupola spout" will likewise be 3.70, indicating that there is no carbon pick-up at this point. When the "per cent carbon in the charge" exceeds 3.70, it seems probable that there will be a reduction or loss in carbon during the melting operation. For example, when the ingoing carbon is 4.00 per cent, it appears that the carbon in iron at cupola spout" will be 3.85 per cent.

Attention is again called to the practices and conditions under which Equation (2) holds. If there is a change in these conditions a modification is necessary. For example, if silicon in iron at cupola spout is more or less than 2 per cent, calculation for the effect on total carbon is made as follows: Subtract silicon in iron at cupola spout from 2.00 per cent. Multiply this difference by 0.25 and add the product to the percentage of carbon expected in iron at the cupola spout.

Example 1—Suppose silicon in iron at cupola spout is 1.60 per cent. Then 2.00 per cent — 1.60 per cent = 0.40 per cent. The product of $0.40 \times 0.25 = 0.10$ per cent, which is the amount to be added to the percentage of carbon expected in iron at the cupola spout.

Example 2—Suppose silicon in iron at cupola spout is 2.40 per cent. Then 2.00 — 2.40 = minus 0.40 per cent. The product of minus 0.40×0.25 = minus 0.10 per cent, which is the amount to be added, but adding a minus quantity, of course, means that its numerical value is subtracted. This relationship has been found to be quite accurate when silicon in iron at cupola spout is within the range of 1.50-2.50 per cent. The effect of phosphorus is handled the same way in quantities up to 0.60 per cent phosphorus in iron at the spout of the cupola.

Table 1 is presented as an example of the effect of changing "per cent carbon in cupola charge." In this particular case the change was from 2.36 to 2.62 per cent and, as previously pointed out, the increase in "per cent carbon in iron at cupola spout" was equal to approximately one-half of the change.

Table 2 shows the effect of different types of coke on carbon pickup. It also shows the uniformity of percentage of total carbon in the iron in consecutive ladles attainable when all phases of the operation are carefully controlled.

The author has deliberately avoided making any reference to coke charges or coke ratios as any such recommendations might lead to difficulties in the cupola operation. However, let us assume that a cupola is melting good, hot iron and at the same time the carbon in the charge is 3.00 per cent. Let us assume further that the operator is at some other time called upon to produce a grade of iron calling for a charge containing only 2.00 per cent carbon and that the weight of the charge is 4,000 lb.

The carbon picked up by the first charge mentioned will be 0.35 per cent, while that picked up by the other will be 0.85 per cent, or a difference of 0.50 per cent. This means that a 4,000-lb charge with ingoing carbon of 2.00 per cent will pick up 20 lb more of carbon than the same size charge with ingoing carbon of 3.00 per cent. Twenty lb of carbon is equivalent to about 22 lb of coke, so it would be wise to increase the coke charge by this amount.

In other words, when using a charge of such a nature that a considerable amount of carbon is absorbed during the melting operation, care must be taken to supply sufficient coke to properly melt and superheat the iron, after having made due allowance for the equivalent amount absorbed by the charge.

Under certain conditions the cupola practice may be deliberately manipulated to bring about some desired result. The use of beehive coke alone or in combination with pitch coke is a deliberate manipulation intended to produce irons higher in total carbon content than could be obtained when melting the same charge with by-product coke alone. In other instances, a combination of by-product and pitch coke is used to melt charges having a lower percentage of carbon in the charge than would normally be used when melting with by-product coke alone.

It may be of interest to note that in the author's experience, when using "good" coke, any manipulations of the sort referred to have always resulted in an increase in the value of "K" in the equation. On the occasions when there has been a decrease in the value of "K," it has almost always been traceable to the use of coke not intended primarily for cupola use. This we have referred to as "bad" coke. In conclusion, the author wishes to point out that the equation

$$\text{T.C. (spout)} = 2.85\% + \tfrac{1}{2}\,(\%\ \text{C in charge} - 2.00\%)$$

is a good yardstick for the measurement of coke quality, although a discussion of this subject is beyond the scope of this paper.

It is realized that Equation (2) as presented herein is not in its simplest form. It could be written as follows:

$$(3)\quad \text{T.C. (spout)} = 1.85\% + (0.5)\ .\ (\%\ \text{C in charge}).$$

Furthermore, it would probably be well to introduce a "coke" factor "f" and assign to "f" a value of one (1) for what we consider "good" coke (or a combination of cokes), a value greater than one (1) for "extra good" coke, and a value less than one (1) for "bad" coke. The equation would then be written:

$$(4)\quad \text{T.C. (spout)} = 1.85\% + (f)\ .\ (0.5)\ .\ (\%\ \text{C in charge}).$$

TABLE 1—CARBON ABSORPTION IN CUPOLA MIXTURES—EFFECT OF CHANGING PERCENTAGE OF CARBON IN CHARGE

(by-product coke used for melting)

Materials	Charge No. 1		Charge No. 2	
	lb	%	lb	%
Malleable pig	300	15.0	300	15.0
Automotive cast	700	35.0	900	45.0
Cast iron scrap (domestic)	250	12.5	250	12.5
Steel scrap	530	26.5	375	18.8
Silvery pig	100	5.0	75	3.7
Manganese steel	120	6.0	100	5.0
Totals	2000	100.0	2000	100.0
Carbon in charge		2.36		2.62
Coke per charge (by-product)	300		300	
Coke ratio		6.7:1		6.7:1
Carbon in iron		3.05-3.20		3.20-3.35
Carbon at cupola spout		Avg. 3.13		Avg. 3.28

(A) Difference between carbon in charges No. 2 and No. 1 equals 2.62 minus 2.36 or 0.26 per cent.

(B) Difference between avg. carbon in iron at cupola spout in charges No. 2 and No. 1 is 3.28 minus 3.13 or 0.15 per cent, approx. one-half of difference in (A) above.

NOTE: Silicon in both charges was adjusted to 1.70 per cent at cupola spout by means of 50 per cent ferro-silicon.

TABLE 2—CARBON ABSORPTION IN CUPOLA MIXTURES—COMPARISON BETWEEN BEEHIVE AND BY-PRODUCT COKES

Materials	Charge A		Charge B	
	lb	%	lb	%
Silvery pig (8% Si)	100	5	100	5
Steel No. 2	1000	50	800	40
Motor blocks	900	45	1100	55
Totals	2000	100	2000	100
Carbon in charge		1.72		2.01
Coke per charge	250	(Beehive)	250	(By-prod.)
Coke ratio		8:1		8:1

TOTAL CARBON IN MELTED IRON SAMPLES TAKEN FROM CONSECUTIVE LADLES OF 2 TONS

Ladle No.	T.C., %	T.C., %
1	3.04	3.00
2	3.06	3.04
3	3.02	3.04
4	3.02	3.04
5	3.05	3.08
6	3.04	3.08
7	3.04	3.02
8	3.00	3.02
9	3.04	3.08
10	3.06	3.08
11	3.06	3.06
12	3.06	—
Average	3.04	3.05
Carbon pickup per ton	26.2 lb (1.31%)	20.0 lb (1.0%)
Coke melting ratio	9.0:1	8.7:1

NOTE: Silicon in both charges was adjusted to 1.50 per cent at the cupola spout.

SOME CAUSES OF PINHOLES
IN MAGNESIUM ALLOY CASTINGS*

By
H. H. Fairfield and A. E. Murton **

ABSTRACT

One of the most troublesome defects encountered in the sand casting of magnesium alloys is the phenomenon called pinhole blows. These blows show up in a radiograph as round cavities. They are usually found just beneath the surface of the casting, and sometimes they have a small outlet to the surface about the size of a pin.

This paper deals with some experiments carried out in order to try to arrive at some of the causes of pinholes. In these experiments different types of sand, metal and pouring methods were used.

A flat plate was chosen as a test casting, because foundry operators had reported that the pinhole defect is most prevalent on large flat surfaces. The plates cast were 10 in. long, 5 in. wide and ½ in. thick. They were gated as shown in Fig. 1. About 700 castings were made.

When two different conditions were to be compared, the two types of molds were rammed up in alternate order and also poured off in alternate order. It is hoped that this procedure minimized such variables as metal condition, drying-out of molds, temperature change during pouring, etc.

The cast plates were radiographed and the number of circular defects determined by count. If a defect was over 1/16 in. square, the number of 1/16 x 1/16 in. squares covered was entered as the pinhole count.

PINHOLE DEFECTS occur in epidemics in magnesium foundries, appearing in large quantities for a few days and disappearing as mysteriously as they come. At the request of Canadian magnesium foundry operators, a study was made of factors influencing the formation of pinholes in magnesium castings, and the following is a report of the progress made in this investigation.

Type of Pattern

It was decided to investigate the pinhole phenomenon by using a test casting in which pinholes would be most likely to occur. At the suggestion of magnesium foundrymen a flat plate (Fig. 1) was chosen, as this type of casting is said to produce the most pinholes. It is 10 in. long, 5 in. wide, and ½ in. thick, and each of the two gates is 1 x 7/16 in. in cross-section. No precautions were taken to hold back flux and dross in the gating system.

Pinholes Defined — Counting Method

For the purpose of this paper, a pinhole is defined as a casting defect in the form of a round hole. These defects are usually found just beneath the surface of the castings. All castings were radiographed and the number of pinholes was determined from a count of the negative. Any round cavity with an area less than a 1/16 in. square (1/256 sq. in.) was counted as a pinhole. If the area was over 1/256 sq. in., each 1/16-in. square was counted as one pinhole.

Although the term pinhole is used in this paper, all other round cavities have been included. It was not possible to identify every defect individually. Sectioning of a number of test castings showed that the majority of defects had the characteristics of pinholes, that is, they were found underneath the surface of the casting and they had a small opening to the surface. They are smooth-surfaced, globular cavities.

Melting Procedure

For the first series of experiments, no special precautions were taken in melting the metal. It was thought that such a procedure would increase the formation of pinholes.

The first melting method was as follows:

1. Fifty per cent scrap and 50 per cent new ingots were charged in a 60-lb. capacity steel crucible. A gas-fired furnace was used.
2. The ingots were preheated to 150 C (300 F), to drive off moisture. The scrap charged was sand-blasted only.
3. Domal flux was sprinkled on the bottom and sides of the crucible and on the charge to prevent undue oxidation. This flux is similar to Dow 310 flux.
4. The charge was melted down in 20 to 50 min., depending on furnace temperature at the start of melting operation.
5. At 730 C (1350 F) the original flux cover was cleaned off and fresh flux was sprinkled on, dried, then stirred in until the metal had a silvery luster.

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TABLE 1.—VARIABLES INVESTIGATED

Moisture content of sand.
Drying-out of molds.
The use of mold wash.
The presence of clayballs on mold surface.
Permeability of sand.
Sulphur dusting of metal stream while filling mold.
Additions of boric acid, sulphur, di-ethylene glycol in the molding sand.
Presence of loose sand in mold.
Interrupted pouring (breaking of metal stream while filling mold).
Flux inclusion entering the mold.
New sand vs. old sand.
Sprue size.
Sand mixing time.
The use of organic binders in green sand.
Variation in pouring temperature.
The use of pouring basin.
Green strength of sand.
Method of fluxing the metal.
Method of grain refinement used in melting.
Preheating of scrap used in melting.

6. In order to refine the grain, the metal was heated to 900 C (1650 F) and held at that temperature for 15 min.

7. All test castings were poured at 1350 F except where noted.

Other melting procedures, used in the last series of experiments, are described in the body of this paper.

Molding procedure is described in Appendix 1.

Sand properties are described in Appendices 2 and 3.

Analysis and Properties of Metal

ASTM Alloy AZ80, commonly used in Canada, was chosen for this test work. Its nominal composition is 8.5 per cent aluminum, 0.5 per cent zinc. Appendix 4 lists the chemical composition of the heats prepared in this series of experiments. Very pure ingot metal was used. The rather high iron and silicon contents shown in some melts are due to contamination from the use of unselected scrap metal.

TABLE 2.—EFFECT OF MOLD GAS PRESSURE ON PINHOLES

Experiment No.	Melting Method: Flux	Grain Refinement	Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
5	Solid	Superheat	Ingots only	Normal	22.3	19.3	6	5.1	Significant
				Mold wash, skin dry	3.0		6	0.8	
40	Cl_2	CCl_4	Ingots only	Normal	14.3	5.6	6	3.5	Significant
				Mold wash, skin dry	8.7		6	2.1	
2	Solid	Superheat	Ingots only	Normal	51.8	27.3	6	20.6	Significant
				Dried mold	24.5		6	3.7	
32	Cl_2	CCl_4	Ingots only	Normal	16.0	2.7	6	4.7	Doubtful
				Dried mold	13.3		6	6.0	
33	Cl_2	CCl_4	Ingots only	3.5% moisture	11.8	2.4	6	2.8	Doubtful
				2.5% "	14.2		6	4.0	
9	Solid	Superheat	Ingots only	2.4% moisture	19.7	4.8	6	10.5	Inconclusive
				2.8% "	24.5		6	15.0	
3	Solid	Superheat	Ingots only	Mold hardness 70	33.8	7.5	6	13.2	Doubtful
				Mold hardness 50	26.3		6	11.5	
4	Solid	Superheat	Ingots only	167 permeability	20.0	371	6	6.0	Significant
				12 permeability	391.0		6	557.0	
17	Solid	Superheat	Ingots only	126 permeability	10.2	35.4	6	4.6	Significant
				14.7 permeability	45.6		6	33.0	

TABLE 3.—EFFECT OF FACTORS WHICH INHIBIT OXIDATION

Experiment No.	Melting Method: Flux	Grain Refinement	Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
1	Solid	Superheat	Ingots only	Normal	11.2	4.0	6	2.2	Doubtful
				No sulphur dusting	15.2		6	6.2	
12	Solid	Superheat	Ingots only	Normal	22.2	53.5	6	7.1	Doubtful
				No boric acid in sand	75.7		6	118.0	
13	Solid	Superheat	Ingots only	Normal	18.3	122.2	6	8.7	Doubtful
				Without glycol	140.5		6	175.0	
48	Solid	Lampblack	Ingots only	10% sulphur	13.7	0.3	6	7.0	Inconclusive
				5% sulphur	14.0		6	7.8	
6	Solid	Superheat	Ingots only	3% sulphur	162.0	144.0	6	254.0	Doubtful
				7% sulphur	18.0		6	5.2	
41	Cl_2	CCl_4	Ingots and scrap	5% sulphur	14.3	6.5	6	2.3	Significant
				10% sulphur	7.8		6	2.7	

Appendix 5 shows the results of tensile tests on bars cast during the series of experiments.

Experimental Procedure

For most of the experiments, 12 molds were made. Six molds represented normal conditions, and the other six represented the variable under test. The two types of molds were rammed up alternately and were also poured alternately. Tensile test bars were poured in normal green sand at the same time. It was thought that this procedure would eliminate as much as possible foundry variables such as metal temperature, drying-out of sand in molds, etc. Since all 12 molds were poured from the same ladle, the effect of any variations of the metal should be the same in both series.

In spite of these precautions, perfect experimental conditions were not attained. The tendency of magnesium to burn in the molten state is erratic. This may account in part for the variations in pinholes.

TABLE 4.—EFFECT OF LOOSE SAND

Experiment No.	Melting Method: Flux	Grain Refinement	Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
19	Solid	Superheat	Ingots only	Standard	21		3		
				Interrupted pouring	273		3		
				Flux entered mold	30		3		
				Loose sand entered mold	509		3		
24	Solid	Superheat	Ingots only	Clean sprue	45	32	6	22	Inconclusive
				Dirty sprue	71		6	33	
36	Cl_2	CCl_4	Ingots only	Good molding	11.3	4.5	6	6.6	Doubtful
				Poor molding	15.8		6	5.6	

TABLE 5.—COMPARATIVE EFFECT OF NEW VS. OLD SAND ON PINHOLES

Experiment No.	Melting Method: Flux	Grain Refinement	Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
7	Solid	Superheat	Ingots only	New facing sand	21.0	343	6	25.4	Doubtful
				Old sand	364.0		6	640.0	
16	Solid	Superheat	Ingots only	New facing sand	6.0	18.7	6	1.7	Significant
				Old sand	24.7		6	26.7	
8	Solid	Superheat	Ingots only	Well riddled old sand	18.6	95.2	6	6.4	Significant
				Old sand with clayballs	113.8		6	65.0	
14	Solid	Superheat	Ingots only	New facing sand	9.8	132.7	6	6.6	Inconclusive
				New facing sand with 1.25% epsom salts	142.5		6	294.0	

TABLE 6.—EFFECT OF POURING SPEED ON PINHOLES

Experiment No.	Melting Method: Flux	Grain Refinement	Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
22	Solid	Superheat	Ingots only	½-in. sprue	983		3		Significant
				¾-in. "	143		3		
				1-in. "	124		3		
				1½-in. "	34		3		
47	Solid	Lampblack	Ingots only	½-in. sprue	267	253.7	6	551	Significant
				1½-in. "	13.3		6	4	
46	Cl_2	Lampblack	Ingots only	½-in. sprue	47.8	38.3	6	31	Significant
				1½-in. "	9.5		6	5	
42	Powder, stir ½ min. only	None	None	½-in. sprue	70.0	38.0	6	272	Inconclusive
			Damp scrap	1½-in. "	32.0		6	185	
43	Solid	Superheat	Scrap and ingot	½-in. sprue	26.3	14.1	6	16.8	Significant
				1½-in. "	12.2		6	3.2	
45	Cl_2	Superheat	Ingots only	½-in. sprue	20.8	10.0	6	15.2	Significant
				1½-in. "	10.8		6	5.8	
35	Cl_2	CCl_4	Scrap and ingots	½-in. sprue	17.7		3		
				¾-in. "	12.7		3		
				1-in. "	14.3		3		
				1½-in. "	10.7		3		
44	Cl_2	CCl_4	None	½-in. sprue	12.7	0.7	6	4.5	Doubtful
				1½-in. "	12.0		6	3.8	

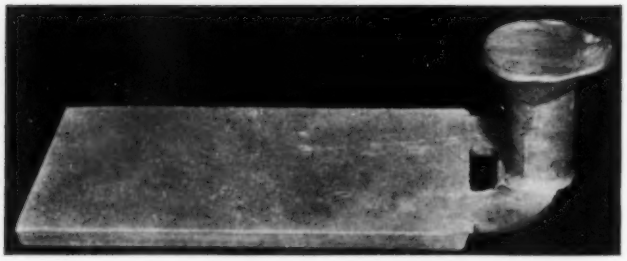


Fig. 1—Pinhole test casting.

Also, the manual skill of the foundry workers is an uncontrolled variable. Occasionally a casting was made with some variations, such as loose sand in the mold, a small difference in the manner of pouring, etc. In carrying out experiments in the foundry, these variations in manual operations are always present. However, if a large number of experiments are carried out, useful results may be attained. Over 700 castings were made in this series.

A list of probable causes for pinholes was drawn up. It was changed and added to as the work proceeded. Table 1 is the final list of variables investigated.

Results of Experiments

The experimental conditions and the number of cavities counted are recorded in Tables 2 to 8.

Discussion

Dietert, Doelman and Bennett[1] have shown how the important phenomenon of mold gas pressure can be measured. It can be readily demonstrated that green molding sand generates a large quantity of gas when heated. One gram of molding sand for magnesium may generate over 200 cc of gas when heated to 680 C (1250 F). The sudden generation of gas from the mold as the metal enters it is called mold gas pressure. In Table 5 the experiments in which mold gas pressure is considered are grouped together.

It is evident that each variable which raises mold gas pressure tends to increase pinholes, e.g.;

Higher moisture content of the molding sand.
Lower permeability of the molding sand.
Harder ramming of the molding sand.
More clayballs on the mold surface.

Skin-dried molds coated with mold spray showed results markedly superior to those obtained with green sand molds. While part of this improvement was attributed to absence of moisture, that could not account for the fact that molds which were skin-dried only did not give so marked an improvement. The superior results obtained with a mold wash may be due either to improved inhibiting properties of the mold wash, or to the hard mold surface which prevents loose grains of sand from being entrapped.

Following is the mold spray mixture used in this investigation:

Liquids:
Water, 2 parts by volume.
Di-ethylene glycol, 1 part by volume.
Solids:
"Micawash," 88 per cent by weight.
Sulphur, 10 per cent by weight.
"Goulac," 2 per cent by weight.
Mix to 45°–50° Baume specific gravity, spray on, and dry at temperature low enough to retain the sulphur.

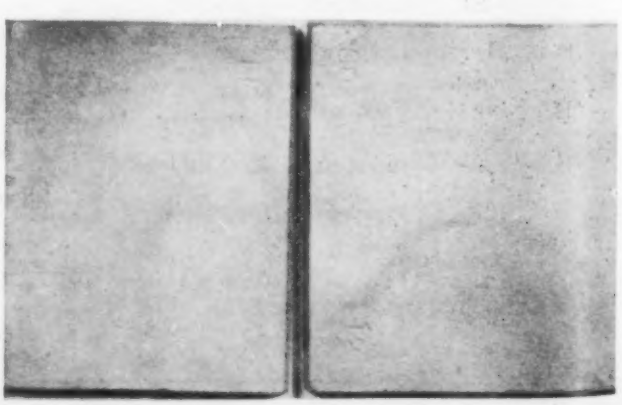
If a magnesium casting is made without sufficient inhibitors in the sand, it will have a black surface due to oxidation of the metal. During the course of the experiments described herein it was noted that pinholes were associated with black surfaces on the castings. It seems reasonable to conclude, therefore, that surface burning of the metal is connected with pinholes. It is possible that the surface burning may raise the temperature and in so doing may raise the gas pressure at that point.

There is sufficient evidence, from the experiments listed in Table 6, to indicate that certain inhibitors which will retard the oxidation of the metal, will also retard the formation of pinholes.

TABLE 7.—EFFECT OF MELTING METHODS ON PINHOLES

Experiment No.	Melting Method: Flux	Melting Method: Grain Refinement	Melting Method: Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
38	Cl_2	CCl_4	Ingots and scrap	Slow melt	10.8	1.2	12	5.1	Inconclusive
39	Cl_2	CCl_4	Ingots and scrap	Fast melt	9.6	1.2	12	2.8	Inconclusive
Average of 16 control heats	Solid	Superheat	Ingots only	Normal conditions	15.0		96		
30	Solid	Superheat	Ingots and scrap	Fast melt	7.0	2.0	12	3.74	Doubtful
31	Solid	Superheat	Ingots and scrap	Slow melt	5.0	2.0	12	2.24	Doubtful
27	Cl_2	Lampblack	Ingots and scrap	Normal	6.0		12	3.87	
28	Cl_2	CCl_4	Ingots and scrap	Normal	6.5		12	3.22	
29	Cl_2	Lampblack	Ingots and scrap	Normal	3.8		12	1.92	

Fig. 2—Two plates from experimental Heat No. 42, showing sound surface at left, and pinholes at right. Damp scrap was used in melting this metal.



When loose sand was dropped down the sprue hole, a large number of pinholes resulted. Test molds were made up with sprues left untrimmed. This would be expected to introduce loose sand into the mold cavity.

Although results were not conclusive, it seems reasonable to believe that loose particles of sand may cause pinholes.

New sand, prepared with washed, screened and dried core sand, gives fewer pinholes than old molding sand. The reason for this is not clear. Old sand does have a greater tendency to form clayballs. When all clayballs were carefully removed (Experiment 8), old sand gave results comparable to new sand. It is also possible that there are organic compounds formed in old sand as well as magnesium sulphates. These compounds may have some effect in producing pinholes. It has been suggested that old sand may have a higher mold gas pressure due to the accumulated fines.

Flat surfaces are more prone to have pinholes than

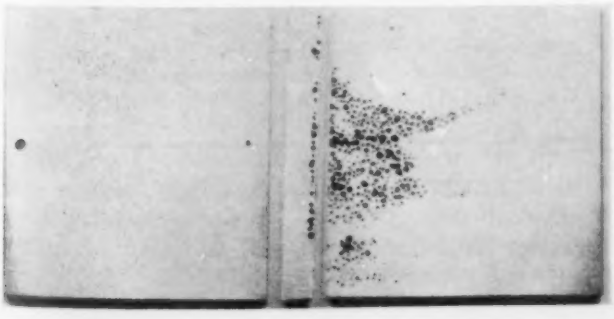


Fig. 4—Battelle Memorial Institute test casting.

TABLE 8.—EFFECT OF FACTORS OF MINOR IMPORTANCE

Experiment No.	Melting Method: Flux	Melting Method: Grain Refinement	Melting Method: Preheat	Test Conditions	Average No. of Pinholes	Difference between Averages	No. of Castings	Standard Deviation	Significance of Experiment
10	Solid	Superheat	Ingots only	15 min. sand mixing	148.3	33.3	6	240	Inconclusive
				3 min. sand mixing	181.6		6	319	
18	Solid	Superheat	Ingots only	Normal	8.7	2.1	6	3.6	Doubtful
				Organic binder	10.8		6	3.2	
20	Solid	Superheat	Ingots only	Pouring temp.: 1450 F	31		1		
					14		1		
				1400 F	23		1		
					59		1		
				1350 F	28		1		
					20		1		Inconclusive
				1300 F	37		1		
					11		1		
				1250 F	11		1		
					23		1		
				1200 F	30		1		
					38				
37	Cl_2	CCl_4	Ingots only	Normal	9.0	1.2	6	3.1	Inconclusive
				3-in. pouring basin	7.8		6	2.45	
34	Cl_2	CCl_4	Ingots only	4.4 Green Bond	15.0	1.5	6	4.8	Inconclusive
				7.4 Green Bond	16.5		6	5.2	

Fig. 3—Machined surface of specimens shown in Fig. 2. About $\frac{1}{16}$ in. of metal was removed from the surface of the castings. The center specimen is a section of the right-hand specimen. Note that in two castings poured from the same ladle, a great difference in the number of pinholes can occur.



are rounded surfaces. Surface area of metal exposed is greater in flat plates or surfaces. This means a greater chance for oxidation to take place and also a faster cooling rate in the liquid metal. It is possible that gas is present in the liquid metal in the mold as: (a) mechanically entrapped gas; (b) gas coming out of solution in the metal; (c) gas forced into the metal from the mold; or (d) gas produced as a result of burning of the metal. The extra heat produces a higher gas pressure.

The nature of the rapidly changing phenomena inside a mold during and after pouring can only be surmised. In the opinion of the writers the following reactions take place: The first inrush of metal heats the mold surface. The mold surface generates gas which has a tendency to enter into the molten metal. The metal in the mold cools rapidly and loses its fluidity.

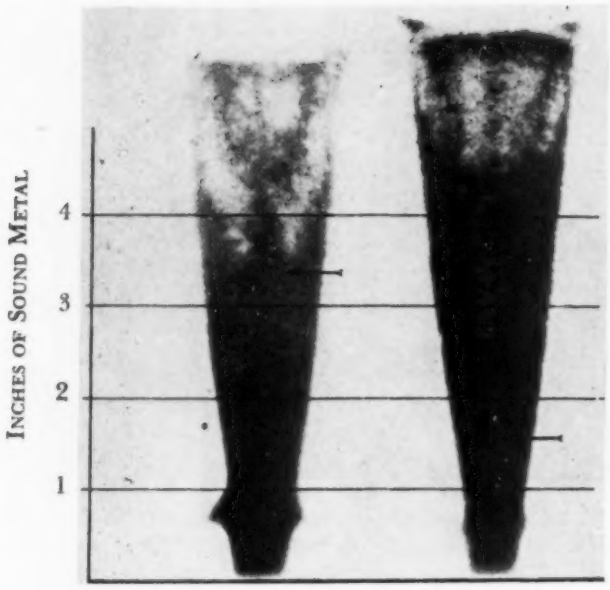


Fig. 5—Radiographs of sections of wedge castings.

There is a balance reached between (1) the gas pressure generated in the sand, (2) the pressure exerted by the metal, and (3) the decrease in fluidity of the metal. The soundness of the casting is dependent upon this final balance. If pinholes occur, they can be reduced by changing the balance in one or more of the following ways: (a) reduce the gas generated by the sand, (b) increase the pressure exerted by the metal, and (c) effect a slower rate of decrease in the fluidity of the metal.

In order to illustrate the effect of physical phenomena of pouring, molds were made with various sprue sizes, ½-in., ¾-in., 1-in. and 1½-in. diameter As is shown in the eight experiments recorded in Table 6, castings poured using the 1½-in. sprue always showed fewer pinholes.

It is tentatively assumed that the reasons for better results with larger sprues are:

1. Faster pouring allowed less time for surface burning.
2. Higher metal pressure was developed in the mold cavity.
3. Solidification was delayed long enough for the metal to force gas out of the mold cavity.

It can be visualized, therefore, that each casting must have an optimum pouring speed. Pouring too slowly can be one cause of pinholes. Pouring too fast may cause other types of defects.

Effect of Moisture

Water vapor or hydrogen entrapped in the metal during melting has been shown to be a major cause of metal porosity.[2, 3]

When heats of metal were made with wet or unheated scrap, the number of pinholes was much higher than normal. These plates showed a black burnt surface. It appears that hydrogen acts somewhat as a catalyst in promoting surface burning of the metal in the mold.

TABLE 9.—SOME MAJOR CAUSES OF PINHOLES

Condition	Remarks
High mold gas pressure	Any change in the molding sand which reduces the gas pressure in the mold will cause a reduction in the frequency of pinholes. These changes include: Raising the permeability, lowering the moisture, softer ramming, and drying the mold.
Pouring Speed	For the mold under test, faster pouring resulted in markedly fewer pinholes. It would seem that there is an optimum pouring rate for each casting.
Melting technique	Pinholes can result from careless melting practice. Solid flux used in magnesium melting is hygroscopic and, if carelessly handled, introduces moisture into the melt. All melting tools, and all ingots and scrap charged in the furnace, should be cleaned and heated prior to melting operations. Failure to take these precautions will cause formation of pinholes in the castings.
Hardness and dryness of mold surface	Dried molds coated with a mold wash had fewer pinholes than green sand molds.
Old molding sand	Castings molded in old sand had more pinholes than castings made in new sand.
Clayballs	A typical batch of used molding sand was screened through a ¼-in. mesh screen. The lumps of sand remaining on the screen were placed on the pattern and rammed up in the mold so that they were at the mold face. The castings so made had a much greater number of pinholes than castings made with carefully screened sand.

TABLE 10.—SOME MINOR CAUSES OF PINHOLES

Condition	Remarks
Sulphur content of sand	For the particular casting investigated, no difference was observed with sulphur ranging from 5 per cent to 10 per cent. Below 5 per cent sulphur, however, pinholes occurred in greater numbers.
Moisture content of sand	Moisture variation within plus and minus 0.4 per cent of the optimum temper had no appreciable effect upon pinhole formation.
Green deformation of sand	Below 0.013 in. deformation, more pinholes are found.
Sulphur dusting	Dusting the stream of metal while pouring inhibits the formation of pinholes.
Di-ethylene glycol	There is evidence to indicate that glycol additions in the molding sand inhibits the formation of pinholes.

Heat No. 42 (Table 6 and Figs. 2 and 3) which was made from damp metal that was not preheated before charging, illustrates the effect of moisture in the formation of pinholes. When the metal was poured through a ½-in. sprue the plates were covered with

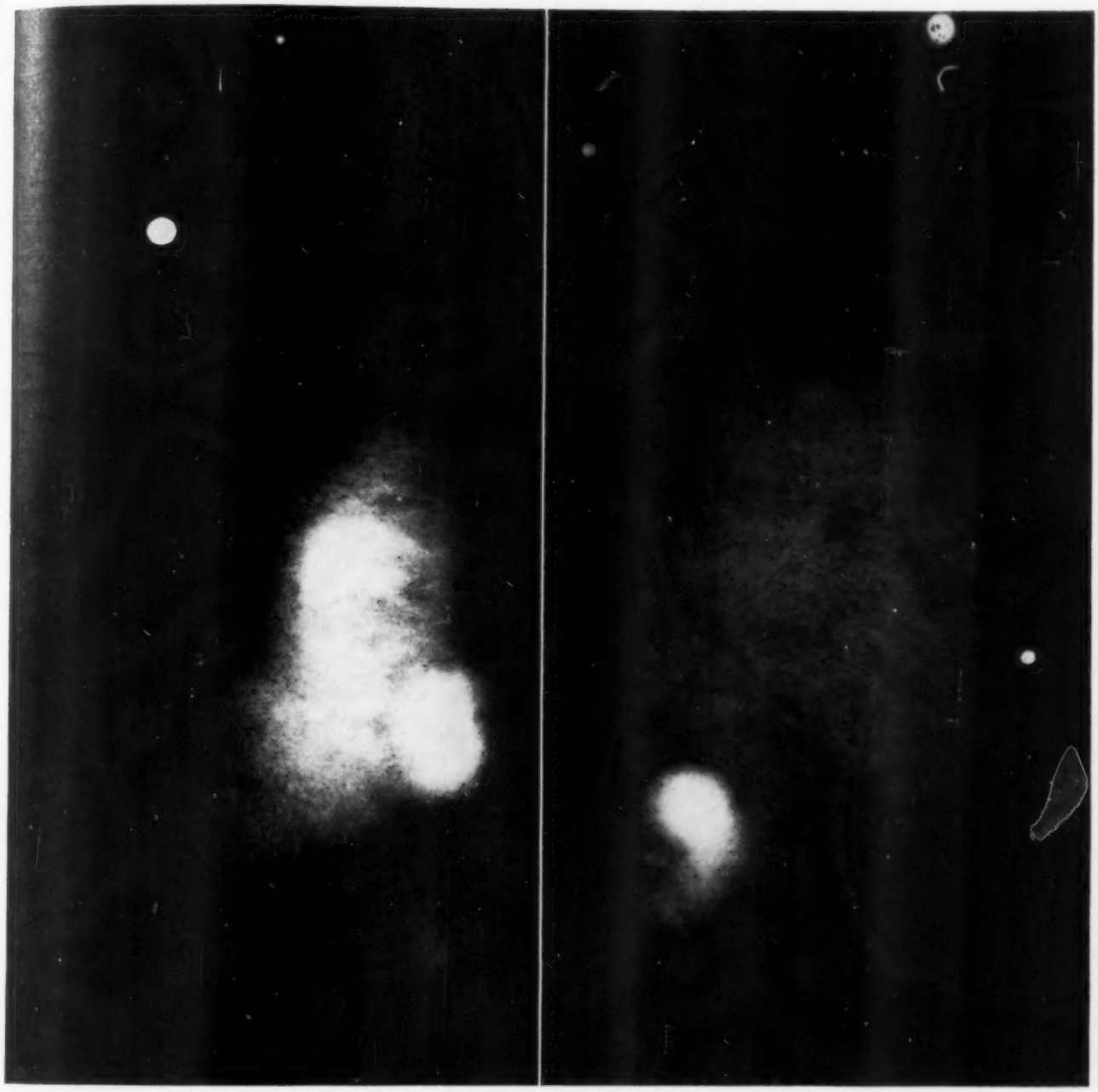


Fig. 6 (left)—Radiograph of a typical test casting fluxed with a solid flux; (right)—Radiograph of a typical test casting fluxed with chlorine.

pinholes, but the same metal poured through a 1½-in. sprue gave results little worse than average. It is assumed that the plates which were poured slowly had more time to burn before the mold was filled. Metal which had been properly degassed (such as Heat No. 44) did not burn when poured through a ½-in. sprue, and showed the normal number of pinholes.

Even if the dissolved hydrogen does not produce pinholes in large numbers, test results indicate that it will increase the number of pinholes. It is reasonable to expect this result, as dissolved gases produce pinholes in other cast metals.

It is possible to keep hydrogen out of the melt, by preheating all the metal charge and by exercising care that all tools and flux used are thoroughly dry. Fluxing with chlorine gas will remove hydrogen from the melt, and for this reason chlorine fluxing is preferable to the use of solid fluxes which tend to by hygroscopic.

In order to find out if melting method* had an effect upon pinholes, several methods were tried out:

Fluxing was accomplished either by stirring in a solid or by bubbling a gas through the metal. The solid flux was similar in composition to Dow 310 flux. The gas used was chlorine.

* Results of investigations on melting procedures of magnesium alloys carried out in these laboratories have been published.

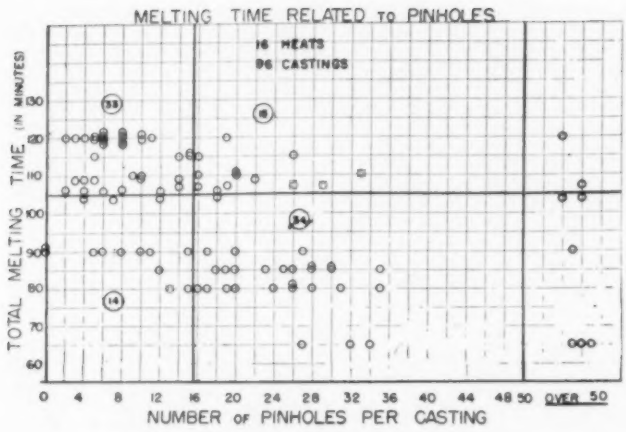


Fig. 7—Melting time related to pinholes.

The gas fluxing results in sounder metal (Figs. 4, 5 and 6). Gas fluxing also gives stronger metal (Appendix 5). There is not enough evidence to prove conclusively that gas fluxing reduces pinholes.

Three methods of grain refinement were employed in these investigations:[4]

1. Superheating at 1650 F (900 C) for 15 min.
2. Lamp black additions.
3. Bubbling carbon tetrachloride through the metal.

The superheating method is considered undesirable due to the rapid absorption of iron that occurs at temperatures above 1400 F.

An analysis of the effect of melting time upon pin-

TABLE 11.—FACTORS INVESTIGATED WITH INCONCLUSIVE RESULTS

1. Green strength of molding sand.
2. The use of a pouring basin.
3. Addition of organic material in the sand.
4. Mixing time for molding sand.
5. Addition of boric acid in the sand.
6. Addition of epsom salt. It was thought that old molding sand contained a product similar to magnesium sulphate.
7. Hardness of green sand molds.
8. Variation in chemical composition of metal (within the limits indicated in the appendix hereto).

APPENDIX 1.—METHOD OF MOLDING

1. Facing sand was mixed, aerated, and placed in covered cans.
2. Backing sand was mixed, aerated, and piled beside the molding machine.
3. The pattern was on a matchplate. A jolt-squeeze molding machine was used with metal molding boxes.
4. Mold hardness averaged 65.
5. When two facings were to be compared, the molds were made up alternately.
6. The casting was designed to produce the pinhole phenomena.
7. No strainers were used in the gating system.

APPENDIX 3.—PROPERTIES OF MOLDING SAND

No.	Experiment Identification	Moisture, per cent	Permeability	Green compressive str., psi	Deformation, in./in.	Flowability, per cent
4	High permeability	1.4	167	7.8	0.014	78
	Fine sand	5.0	12	9.6	0.011	77
17	Normal	2.4	126	5.5	0.016	85
	Fine sand	6.0	14.7	8.1	0.020	74
18	With cereal flour	2.9	108	3.7	0.025	83
	Normal	2.2	128	5.3	0.019	83
33	Low moisture	2.0	163	6.7	0.0155	83.0
	High moisture	3.2	134	4.6	0.017	85.5
34	Low bond	2.1	156	4.4	0.0135	83.5
	High bond	2.3	163	7.4	0.0165	80.5
41	10 per cent sulphur	2.4	105	6.5	0.015	79
	5 per cent sulphur	2.3	122	5.2	0.015	82
48	10 per cent sulphur	2.4	96	6.1	0.014	81.5
	5 per cent sulphur	2.3	138	6.1	0.0125	82
Remainder						
	Facing sand mixtures	2.1–2.6	115–167	4.0–6.5	0.011–0.017	82–87
	Backing "Old Sand"	2.2–2.6	120–185	5.5–9.7	0.009–0.020	73–83

APPENDIX 4.—CHEMICAL COMPOSITION

Element	Range, per cent
Aluminum	7.50–8.45
Zinc	0.32–0.71
Manganese	0.25–0.45
Iron	0.001–0.026
Silicon	0.012–0.027

APPENDIX 2.—COMPOSITION OF MOLDING SAND

Experiment No.	Sand Mixture	Core Sand, Lb. A.F.A. No. 57	Core Sand, Lb. Fineness No. No. 179	Western Bentonite, lb.	Southern Bentonite, lb.	Sulphur, lb.	Boric Acid, lb.	Diethylene Glycol, lb.	Others, lb.
	Normal	100		1¾	1¾	4.0	0.5	1.0	
4, 16	Low permeability		100	0.5		4.5	0.5	1.0	
6	High sulphur	100		1¾	1¾	7.0	0.5	1.0	
6	Low sulphur	100		1¾	1¾	3.0	0.5	1.0	
12	No boric acid (H_3BO_3)	100		1¾	1¾	4.0	None	1.0	
13	No di-ethylene glycol	100		1¾	1¾	4.0	0.5	None	
14	Epsom salts (magnesium sulphate) added	100		1¾	1¾	4.0	0.5	1.0	Epsom salts, 1.25 lb.
18	Organic binders	100		1.0	1.0	4.0	0.5	1.0	Goulac, 1.0 lb.; Casco, 1.0 lb.
26, 34	High bond	100		2.0	2.0	4.0	0.5	1.0	
26, 34	Low bond	100		1.5	1.5	4.0	0.5	1.0	
41, 48	High sulphur	100		1¾	1¾	10.0	0.5	1.0	
41, 48	Low sulphur	100		1¾	1¾	5.0	0.5	1.0	

APPENDIX 5.—MECHANICAL PROPERTIES OF MAGNESIUM ALLOY AZ80 AS-CAST TEST BARS

	Solid Flux Method	Chlorine Flux Method
Number of test bars	108	64
Average tensile strength, psi	28,080	29,560
Normal range of tensile results (range for average of four bars), psi	±2,140	±2,120
Elongation, per cent in 2 in.	5.0 to 9.0	5.0 to 9.0

Metal fluxed with chlorine gas had a tensile strength superior to that of metal fluxed with solid flux.

APPENDIX 6.—SIGNIFICANCE OF VARIABLES IN CONTROL HEATS*

Variable	Range Studied	Best Conditions	Significance
Sand moisture	1.4–3.0	2.0–2.3	90
Permeability	112–134	—	None observed
Green bond, psi	3.0–7.6	Below 5.3	99.9
Deformation, in./in.	0.006–0.019	Above 0.013	98.0
Flowability, per cent	83–86	—	None observed
Aluminum, " "	7.53–8.69	—	" "
Zinc, " "	0.34–0.43	—	" "
Manganese, " "	0.23–0.50	—	" "
Iron, " "	0.005–0.019	—	" "
Silicon, " "	0.012–0.027	—	" "
Melt-down time, min.	19–50	19–42	93
Heating time from melt-down to 1350 F	5–20	Over 10 min.	98
Heating time from 1350 F to 1650 F	10–41	Over 24 min.	99.9
Total time, in min.	55–105	Over 90 min.	99.0

* Only heats which were fluxed with a solid-type flux are included. The scrap was not preheated for the above heats.

APPENDIX 7.—CORRELATION OF DATA ON CONTROL CASTINGS

In the course of the experiments described in this paper, a considerable number of control, or normal, castings were made. A study of the data collected on these control heats was made and some interesting correlations were observed. The method of plotting these data for study is shown in Fig. 7.

The relationship between total melting time and number of pinholes is quite definite (Appendix 6). It is not proved that melting time is a causative agent in forming pinholes. Its connection with the pinhole phenomenon must be determined by further experimental work (or by some logical process of reasoning).

The other correlations in Appendix 6 were obtained from scatterplots similar to Fig. 7. The evidence obtained should serve as possible clues for further investigation.

holes in 16 heats of normal castings is shown in Fig. 7. In these heats, longer melting time is definitely associated with increased pinholes. It will be noted, however, that the experiments reported in Table 7 do not show a marked difference between the long and short melting times.

The effect of melting time can only be determined by collecting a large number of observations in a production foundry.

An examination of Table 6 indicates that when scrap was not preheated in the cases in which the solid fluxing method was used, a large number of pinholes occurred in the test castings. However, when preheating and other precautions were used, solid fluxed metal was comparable (so far as the occurrence of pinholes was concerned) with metal produced by other melting techniques.

When chlorine fluxing was used, damp scrap in the charge did not affect the number of pinholes. It is proven elsewhere that chlorine removes hydrogen from the melt.

In Table 10 a number of melting techniques are compared to the average of 148 test plates made with the solid fluxing technique. Further work will be carried out in order to get sufficient data for an accurate comparison between these methods. It is apparent, however, that diligent attention to detail is more important than the choice of melting method.

Factor of Minor Importance

Some factors appear to be of minor importance in producing pinholes. These are:

Moisture in Molding Sand: If the sand is near temper, minor fluctuations as much as ±0.4 per cent moisture content appears to have little effect.

Green Bond: In Experiment No. 34, six plates were cast in sand with a green bond of 4.4 psi compression strength and six in sand with 7.4 psi. No significant difference was observed between these two groups.

Magnesium Sulphate in the Sand: No significant difference was observed between the use of sand with 1.25 per cent epsom salts and the normal mixture. This may indicate that the presence of some magnesium sulphate is not the reason for the inferior qualities of old sand.

Pouring Temperature: Varying the pouring temperature between 650 C (1200 F) and 760 C (1400 F) produced no significant difference in the number of pinholes formed.

Conclusions

As a result of the experimental work, certain conclusions were arrived at concerning the cause of pinholes in magnesium castings. The factors investigated are grouped in Tables 9, 10, 11.

Blows and pinholes in magnesium castings are caused by a number of conditions in melting, molding, pouring and gating.

Moisture or hydrogen absorbed in melting is a cause of pinholes.

Chlorine fluxing reduces the tendency to form pinholes.

Sulphur, boric acid and di-ethylene glycol additions to the molding sand inhibit pinhole formation.

All factors which tend to increase mold gas pressure also accelerate pinhole formation. Pinholes are increased by: (a) lowering permeability, (b) raising moisture, (c) increasing mold hardness. Dried molds give markedly fewer pinholes than do green sand molds.

Clayballs have been shown to be a cause of pinholes.

Pouring speed has a pronounced effect upon pinhole formation.

Acknowledgments

The writers an indebted to Mr. J. W. Meier* for his advice and assistance in this project. Messrs. Hugh Livingstone and H. J. Kozlowski** supervised all the melting operations described herein.

* Senior metallurgist, Non-Ferrous Section, Physical Metallurgy Research Laboratories, Bureau of Mines, Ottawa.

** Metallurgists, Physical Metallurgy Research Laboratories, Bureau of Mines, Ottawa.

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DISCUSSION

Chairman: W. E. SICHA, Aluminum Co. of America, Cleveland

Co-Chairman: J. C. DE HAVEN, Battelle Memorial Institute, Columbus, Ohio

DR. L. W EASTWOOD[1] *(written discussion):* The authors are to be commended for having carefully and meticulously carried out their experimental work and preparing a well-written paper.

The following comment is not intended to be a criticism but is, instead, an attempt at clarification. While there are many types of unsoundness in magnesium castings, there are two defects which I should like to describe in a little detail. The first defect occurs as isolated, small, rounded holes generally just under the surface and connected to it by a small neck. These rounded holes are 1/16 in., more or less, in diameter and usually extend an average of 3/16 in. deep, more or less. They are not readily visible on the as-cast surface but appear after sandblasting. These defects are isolated, usually only one to a casting when they do occur, and in a very serious epidemic, one casting out of ten may have at least one of these surface holes. This defect is sometimes referred to as "green sand blows" because they occur mostly on green sand surfaces.

The cause of this defect is relatively unknown, and for this reason, it is a very serious plague to the magnesium foundryman because he does not always know how to reduce their number. Furthermore, such small, rounded cavities, which open up on the casting surface, are frequently cause for rejection of aircraft-quality castings. Some foundries weld these defects, others hot peen them so they are not visible.

The second defect occurs as myriads of gas holes of various size and which are produced as a result of the evolution of gas during solidification. In aluminum alloys, this defect is referred to as "pinhole porosity," and it is very common when casting aluminum. When very gassy magnesium melts solidify, a similar defect occurs, but the resulting cavities are usually larger. However, they may approach pinhole size when the section solidifies fairly rapidly. This defect, in contrast to the preceding one, occurs as a great number of small, rounded or angular cavities. Good illustrations of this defect are shown by Fig. 4b and 5 in the authors' paper.

Of these two types of unsoundness in magnesium castings, the first is difficult for the foundryman because its origin is not definitely known. The second is not difficult for the foundryman to handle because it is a direct result of a high gas content of the melt or of local gas absorption by the melt in the mold cavity with subsequent evolution of absorbed gas during solidification. Since the proper methods of degassing magnesium melts are now fairly well understood, this type of unsoundness need no longer be a real problem in magnesium foundries.

While the authors may have considered both types of defects, they apparently have lumped the two together. Certainly, the defects shown by Fig. 4b and 5 are the second type. The authors' paper would, therefore, have much more practical value if they could separate these two defects or indicate the actual number of cavities observed. Where one or two cavities occurred, it probably is a "green sand blow"; where many occur in clusters, as shown by Fig. 4b and 5, the defect is probably a result of the evolution of gas which was once in solution in the melt.

Mr. Nelson has implied that the defects such as those illustrated by Fig. 4b and 5 in the authors' paper can be eliminated by turning the casting on edge and placing a top riser on it. The inference is that such unsoundness is a result of mechanical entrapment of mold gases.

Such a conclusion would be unfortunate because unsoundness of the type illustrated by Fig. 4b and 5 in the authors' paper could hardly be accounted for in this way. It is true that hydrogen in solution in the melt will precipitate and then rise in the casting. Therefore, if the casting were placed on edge with the riser on top, the hydrogen bubbles tend to rise and escape into the riser, producing a sounder casting than could be obtained if the same piece were cast in a horizontal position.

This is not proof that the unsoundness is caused by mechanical entrapment of mold gases. It merely indicates that the hydrogen precipitated from solution in the melt tends to rise and may escape into the riser if conditions permit.

DR. R. F THOMSON[2] *(written discussion):* The authors are to be complimented for having recognized and studied the effect of some 20 variables on the causes of pinholes.

I should like to ask what is the height of the downsprue and was the pattern for the sprue given draft to produce a larger opening at the parting line or at the top of the cope?

The data of Table 6 are of particular importance at this time to the A.F.A. Aluminum and Magnesium Division Research Committee who are interested in the effect of gating system geometry. Our interest centers in the relation of downsprue cross section to gate area of the casting shown in Fig. 1. Let us examine the data of Table 6 from the standpoint of location of constriction or choking in the gating system. From the geometry of the casting we see the following ratios exist:

	Dimensions, in in.	Area, in sq. in.	Ratio Sprue to Gate Area
Gate (2)	1 x 7/16 each	0.875	
Sprue	1/2 Dia.	0.196	0.224
	3/4 Dia.	0.442	0.505
	1 Dia.	0.785	0.897
	1 1/2 Dia.	1.767	2.02

You will note the downsprue area is less than the ingate area in all except the 1½-in. dia. downsprue. Heats 22 and 35 would indicate choking in the ingates to be the preferred method of gating.

Unfortunately this conclusion may not be correct because the variation in number of pinholes between Heats 22 and 43 is greater than the difference between ½-in. and 1½-in. sprues in Heat 22. As far as I could see, Heats 22 and 43 were handled similarly except the charge in Heat 22 consisted of ingots only and in Heat 43 it consisted of scrap and ingots. It does not appear likely this scrap addition would be responsible for reduction in pinhole intensity. I do not believe the authors will make any such claim for scrap additions.

I hope the authors will find it possible to continue their investigations on this very important problem as their initial work has certainly shown the direction for further fundamentally sound research, and is the type of research which pays off in decreased price and higher quality light metal castings.

MR. FAIRFIELD: All these melts were half ingots and half scrap, and in Heat No. 22, only the ingots were preheated and not the scrap. Heat No. 35 was fluxed with chlorine, which removed a lot of the hydrogen and we found that whenever we used chlorine, the metal was less sensitive to any agent causing pinholes.

Comparing Heat No. 43 to Heat No. 22, in Heat No. 22 we preheated the ingots and not the scrap and in 43 we preheated both scrap and ingots so there is a difference in melting technique.

DR. THOMSON: Perhaps it is an oversight on my part for not finding out this difference but the important thing is this trend in the first set of experiments and I hope the authors find it possible to continue this investigation on this important problem.

C. E. NELSON[3]: Is there any special place in the panels where the porosity shows up?

MR. FAIRFIELD: We considered trying to differentiate between types of porosity, as Dr. Eastwood suggested, but we thought we would run into a bias on our own part due to our inexperience or inability to distinguish between them. As far as aircraft inspection is concerned, a hole is a hole. Therefore we tried

[1] Battelle Memorial Institute, Columbus, Ohio

[2] International Nickel Co., Detroit

[3] Dow Chemical Co., Midland, Mich.

to find out the conditions which produce soundest castings. Figure 6 shows the two types of porosity. That is the one type of isolated cavity Dr. Eastwood referred to. That has 1/16th machined off the surface of the plate. There are a large number of defects, and they occur just under the cope surface. Now we do not know what causes that. We have no idea what the difference is between those two. All we are able to do is to find when the condition is not present.

In Fig. 2 there are two castings poured together in the molds of identical properties. In one mold there was definitely a burning reaction. The other casting was fairly sound. You see, what we were up against when we wanted to evaluate the pinholes. As soon as you start saying, "Well, that isn't a pinhole and that is," then you are putting personal bias into it. So we took every cavity that had a circular shape. The castings in Fig. 2 incidentally are quite difficult to cast.

DR. EASTWOOD: Figure 2 shows the type of defect which results from a small blow during a highly localized reaction with mold. But Fig. 6 illustrates very well the type of defect produced by localized gas absorption and reprecipitation.

MR. FAIRFIELD: Figure 1 shows the gates. In Fig. 4, that is a cross section or slice in the middle. Actually the pinholes do not occur close to the gate usually. Usually they are about half way down the casting.

Figure 1 shows the test casting. That is cast with the patterns in the drag. A casting can have 200 pinholes and you can detect it from the surface very rarely. Severe sand blasting will open them up. I do not know what the height of that sprue is.

Dr. Thomson pointed out that different heats did not give the same results. That is right. We could not make all the castings the same. The pattern was so sensitive that a slight variation which escaped our observation would make one heat with more pinholes than another.

Figure 7 refers to 16 heats of controlled castings. We analyzed the data statistically and we mention in the paper optimum sand moisture, green bond, and deformation. That was obtained from scatter plot charts. That is the spread we got in so-called control heat made in our backing sand.

As to the pinholes, the general average was around 16 per plate. The ones definitely without that sand blow would be those under six. That points out that the longer the melting time, the fewer pinholes result. We thought that was due to slow heating in the charge at the start which drove the moisture out before it got entrapped in the liquid metal.

MR. NELSON: I get the impression that most of this porosity is the cope surface type. By cope surface porosity I refer to porosity just under the cope surface which normally cannot be seen until the surface has been thoroughly sand blasted or machined. From our experience, the greatest cause of that is insufficient venting of the gases from the mold at the time of pouring. I believe a little vent at the opposite end of that panel would cut down those pinholes, as you call them, very much. I think by trapping the metal in there, the mold gases have to expand and they will actually penetrate into the surface of the molten metal.

The data in the paper on the effect of permeability of the sand illustrates my point very well; where you have high permeability the number of pits is down and conversely with low permeability there are a very large number of pits. You might possibly explain some of the effect of old molding sand by the same principle. I do not know whether the authors have any measure of the permeability of their old sand or not but it is quite likely that the permeability would have dropped down quite appreciably on the used sand.

We have found the isolated and randomly occurring pits or blows to be caused by impurities that get into the molding sand during use. One of the worst things, of course, is coal dust particles.

There might be a tendency to get over-anxious as a result of seeing this information and I think in actual practice we do not have too much trouble in the magnesium foundries with cope surface porosity. The main reason for that is that foundrymen will vent the molds and also, if they have a flat surface to make, they will almost invariably try to get that flat surface on the vertical walls or at least onto a slanting section, so that they can vent the mold cavity. One of the first things I tried to do in the foundy was to try to make flat plates and that is the hardest thing I know of to make with any metal.

In making the foregoing comments I merely wish to emphasize that by actual experiment one can demonstrate the very important part played by unvented mold gases as well as gases entrapped in the metal while passing through the sprue and runner system if turbulence exists. Unpublished experiments also show that much of this mechanically entrapped gas can be removed to a large extent by passing the metal through a skim gate. (Gas is evolved on upstream side of skim gate.)

The importance of dissolved gases, whether they be in the metal from the pot or absorbed during flow into the mold, will show after adequate venting is applied and good non-turbulent metal flow conditions have been established. In general, porosity due to these sources will show up as micro-porosity well within the casting walls rather than as pinholes (except in cases of gassiness beyond normal occurrence in magnesium foundry operations).

MR. FAIRFIELD: I agree wholeheartedly that venting appears to be part of the answer. Not entirely, though. We cut a wedge riser at the end of that plate and brought it up to the top of the mold so presumably we flushed the gas out, but we still got pinholes. In foundry practice undoubtedly the venting would be of great help. The old molding sand had identical permeability with the new sand. There are several tables of the properties in the paper. But, of course, that casting is a very tough one to make and the radiograph inspection is probably far more severe than any casting would get in normal production. So when we say a casting had 20 pinholes you would have to look pretty close to find those. In ordinary commercial castings it would be quite acceptable, so the situation is not too alarming.

J. W. MEIER[4]: I agree with Mr. Nelson on the problem of better casting technique but the poor casting design was deliberate. In other sand casting investigations we encountered very seldom these defects, so we had to choose a rather difficult shape (flat plate) and to use poor venting to obtain a maximum amount of defects for a more convenient study of the effect of varying melting and casting factors. The use of poor refining techniques and the introduction of scrap without cleaning or preheating were aimed to show the effect of a poor quality of the melt on the amount of defects.

All these experiments were made in 1945, just after the first publication of the interesting investigations on chlorination and grain refinement at the Battelle Memorial Institute. The worst results shown by Messrs. Fairfield and Murton are on melts with high content of gases due to introduction of moisture in scrap or careless fluxing. In the last series of experiments chlorination and grain refining by carbon inoculation was used and the results were considerably better.

I agree entirely with Dr. Eastwood on the problem of improper nomenclature of defects used in this paper. But since this project was undertaken for the practical foundryman, we wanted to study the causes of the defects and were not interested, at this time, in their classification.

We intend to continue this investigation giving special consideration to problems of gating and risering.

J. E. LINDSAY[5]: What is the effect of skim gates on the pouring rate? We regard the use of skim gates as standard practice and it appears the consequent lowering of the pouring rate would tend to give us more pinholes.

In one phase of the investigation molding sand (we presume used molding sand) was introduced through the sprue and it contributed to pinholes. Would the addition of sharp sand containing no bond have the same effect?

Has an attempt been made to differentiate between porosity in the horizontal cope and drag faces? It would seem that the type of porosity caused by absorption of gases from the mold (and subsequent re-precipitation) would tend to occur equally on cope and drag surfaces.

MR. FAIRFIELD: We did some preliminary work with skim gates prior to this investigation and I can only speak from personal opinion, not from data, but I think the gating system can be so designed so that you can get along without skim gates. As I recall, our preliminary work showed no difference in pinhole content with or without the skim gates. I would not speak against skim gates. They are very useful to control the flow of metal. If you have a given gating setup and you want to pour

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[5] Light Alloys, Ltd., Renfrew, Ontario, Canada

it a little slower or faster and change the size of the skim gate and so control the pouring rate, the same as they do in gray iron, that is all right.

DR. EASTWOOD: The defect that we called "the green sand the top and bottom of the casting and then counted the pin used molding sand. I presume it had a coating of clay, water, sulblow" can occur on either the drag side, cope side or on a machined the top and bottom and counted the top and bottom we used the radiograph. In order to do a thorough job, we When we started this investigation, we started to machine off should have taken the casting after we radiographed it and then phur and glycol and made some gas and started the metal burning.

The sand we dropped down the sprue was backing sand or holes that we observed. We thought we would miss some so vertical wall. I believe they occur most readily, however, on the cope side. I think the other type of defect, shown by a myriad of small holes, is a result of gas evolution during solidification. The hydrogen bubbles which formed tend to rise in the melt because of their low density. As a result of that, they are invariably concentrated on the cope side. The distinction between the two types of defects, however, lies in the number formed and it seems to me that the distinction between the two types of defects is important because you have to know first how defects are formed, or you should know how a defect is formed, before you can intelligently eliminate it.

W. J. KLAYER[6]: I presumed that gate geometry was leading to the possibility of the gases coming from the mold. It was mentioned that these melts were purposely gassed. Therefore, it would seem to me that the 34 pinholes per thousand area would be at the rate of solidification. Did you take the time it took to fill the mold? That might indicate that where you had the 34 holes, inasmuch as it was definitely gassy metal, it was still in solution due to the rapid rate of solidification with the 1½-in. sprue.

MR. FAIRFIELD: I believe you are correct. That was our thought in pouring it at different rates.

MR. KLAYER: Do you have the figures?

MR. FAIRFIELD: I do not believe we have the time, but that was the idea behind it. If you made a curve of the rate at which the metal cooled, you would get different cooling rates and different cooling rates would alter the amount of gas that came out of solution. That is, you could take the gassy metal and pour it one way and get a sound casting and pour it another way and all the gas would come out, so you are correct in that statement. That is the only theory that we could think of to explain why that happened because the molds were all the same. The metal was all the same. The only difference was in the speed at which we poured the mold and presumably the amount of pressure on the metal when it was poured.

CHAIRMAN DEHAVEN: There is a tie-up, too, between the amount of gas in the metal and the amount of gas absorbed in the mold. In other words, if you are well below the threshold value of hydrogen content in the metal, then you would not have nearly the chance of adding a little gas through the mold and causing one of these defects.

W. MADER[7]: If you increase the rate of solidification you will reduce the rate of evolved gases and therefore you will not tend to evolve gases and produce pinholes. For istance, in permanent mold castings with proper melting there is very little tendency towards pinhole porosity. Proper application of chills in sand molds will reduce pinhole porosity.

CHAIRMAN DEHAVEN: If you increase the rate of solidification.

MR. MADER: Yes. In this paper we have gone into the theoretical part considerably but we have to apply this theory to foundry practice. For instance, in melting we have to use good melting practice. We must melt in good furnaces with little chance of gas pickup. If we use too permeable molding sand we are going to get a rough casting. We have to strike a balance between great permeability and small permeability. Also, we cannot air condition a foundry so we cannot eliminate gas that way but we can use preheated scrap. We do not necessarily even need to preheat the scrap if we use common sense and not store metal outside but bring it in and let it dry out. We reduce our pinholes. A lot of the points brought out in the paper may be applied to normal foundry practice but we must also use common sense in the application in production of practices determined by experiment.

[6] Aluminum Industries, Inc., Cincinnati

[7] Oberdorfer Foundries, Syracuse, N. Y.

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